



WITAMIR-I, A University of Wisconsin Tandem Mirror Reactor Design

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September 1980

UWFDM-400

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UWFDM - 400

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Acknowledgement

The authors wish to acknowledge the partial support of this project by the Department of Energy, Division of Magnetic Fusion Energy under contract DE-AS02-78ET52048 and the Wisconsin Electric Utilities Research Foundation. The support of both of these organizations and the University of Wisconsin was extremely important to the successful completion of this work in a timely fashion.

The production of this report was greatly facilitated by the excellent work of Mrs. Linda Kraft, Ms. Beth Brown, and Ms. Gail Herrington. We owe a special debt of gratitude to Mr. Dennis Bruggink for his excellent coordination of the graphics, proofreading and general assembly of the chapters of this report.

We wish to acknowledge the very productive technical interactions with scientists at Lawrence Livermore Laboratories. Particular appreciation is extended to Drs. G. Logan, D. Baldwin, G. Carlson, and K. Fowler. Other scientists at the University of Wisconsin also made particularly helpful comments on this work and we wish to thank Professors J. Conrad, W. Wolfer, and R. Conn, who is currently at UCLA. Finally we wish to thank Professor W. Heinz of the Karlsruhe Nuclear Laboratory in the Federal Republic of Germany for allowing Dr. W. Maurer to participate in this study and his continuing interest in this topic.

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I. Design Philosophy

I-A. Introduction

Progress in plasma physics and fusion technology in the 1970's has stimulated a large number of conceptual fusion reactor studies. After the first international fusion reactor conference in Culham (1969), no fewer than 30 different reactor designs were carried out in the U.S. alone. Similar work has been carried out in the UK, FRG, USSR, and Japan. These conceptual reactor designs range from near term facilities such as EPR's, TNS's, TETR, FERF, ETF, etc., to much more advanced power plants for the mid 21st century such as UWMAK-III, Starfire, Hylife, SOLASE, the LLL mirror reactors, etc.

When embarking on any full scale fusion reactor design, it is absolutely essential to have a clear vision of the time frame and level of technology one is willing to embrace so that the level of optimism or pessimism is relatively uniform throughout the design. It is the purpose of this chapter to outline the background for the present study and illuminate the goals we were trying to achieve.

I-B. General Guidelines

Early in the WITAMIR-I study it was decided that this reactor should be *"...a self-consistent conceptual DT Tandem Mirror with thermal barriers that maximizes Q while retaining the economic features of a commercial electricity producing power plant"*.

It is important to emphasize the word "self-consistent" because we have found that it is sometimes possible to find solutions to current reactor problems in one area that completely invalidate the design in another. An example of this phenomenon is designing for extremely high wall loadings to improve the power density (hence lowering the capital cost) but ignoring the increased operating cost and downtime associated with replacing damaged components. There are many other examples that could be given but the point is that only after a given decision has been assessed by all those involved with the reactor design ranging from plasma physicists to stress analysts, neutronics experts, materials engineers, heat transfer specialists, magnet designers, power engineers, safety experts, building designers, economists and environmentalists can we be reasonably sure that it will be compatible with the overall objectives of the device.

We chose a DT fuel cycle for all of the traditional reasons (i.e., highest reaction rate for minimum energy investment, fuel supply, experimental base, etc.) and we rejected the more advanced cycles (e.g., DD, D-He³) because

they appear to be much farther away from presently envisioned confinement schemes. We pushed the tandem mirror with thermal barriers because it was clear to us that such a scheme truly represented the best chance for increasing the energy amplification of the device (Q) while at the same time reducing the level of technology (i.e., neutral beams, RF heating, end plug magnets) required.

We were trying to increase Q, which was in the 3 to 5 range when we started our work, but once values exceeding 10 to 20 were achieved, there was somewhat less to be gained by further increases. Obviously, higher Q's have some attraction but they began to demand more from the other components of the reactor and we eventually settled on a Q of 28.

Finally, we decided to focus on an electricity producing plant in contrast to a fission-fusion hybrid or a synthetic fuel producer. This decision is based on a lack of clear economic incentive for hybrids and the generally unfavorable results on synthetic fuel productions that have been published in the last 5 years.

Once the general theme of the WITAMIR study had been determined we set about defining some more specific guidelines for the reactor. These are listed below:

1. *It should be a "near term" commercial fusion power reactor (approximately 2020).*
2. *The power level should be approximately 1000 MWe.*
3. *The reactor should utilize both RF and neutral beams to create the thermal barriers.*
4. *Steady state reactor operation was assumed to be possible even though a mechanism for removing the helium "ash" is yet to be found.*
5. *There should be an emphasis on a "reactor that works" versus one that has the lowest environmental impact.*

The time frame that we chose, the year 2020, means that the reactor should be designed with technology present in the 2000 to 2020 period. This will be after TMX, TMX-upgrade, MFTF, and MFTF-B operation and also after the MFTNS and Mirror Demo reactor have operated. The WITAMIR reactor design should not be the first of a kind, but rather more in line with the second or third of a kind.

We chose an electrical power of approximately 1000 MWe because previous experience has taught us that only a reactor of that size has much of a chance

to be competitive economically. In fact we found that WITAMIR-I, at approximately 1500 MWe, was competitive with our most recent tokamak reactor, NUWMAK.

Steady state operation is one of the major advantages of the TMR that we wished to exploit in our design. Again, past experience on tokamak reactors has shown us that component lifetimes are often limited by fatigue, not just neutron damage. Removal of that feature from the list of engineering constraints has allowed us much more freedom in first wall and blanket design.

Finally, we decided to emphasize a "reactor design that works" instead of trying to design the ultimate system with the lowest environmental impact. This is not to say that we ignored environmental considerations because we worked hard to reduce the tritium inventory and we chose a coolant ($\text{Pb}_{83}\text{Li}_{17}$) which has the lowest potential for contaminating the environment in the event of an accident of any liquid metals previously considered. What this philosophy does indicate is that when faced with a choice of a much more risky, but more environmentally pleasing, material or design approach we tended to favor less risky and more "conventional" decisions. Later, post-WITAMIR-I studies can try to investigate those environmental features which could be improved by new ideas.

I-C. Plasma Design Philosophy

In comparison to tokamaks, the plasma physics understanding of tandem mirrors is in a much more dynamic state. There exist many possible ways of arranging the various elements of a tandem mirror: electrostatic potential peak MHD anchor, central cell solenoid, and thermal barrier. For example, the thermal barrier can be inboard or outboard of the MHD anchor. The potential can occur in the MHD anchor or can be separated from it. The MHD anchor can be a yin-yang minimum-B coil or it can be one of several possible axisymmetric configurations. All of these configurations have their merits and drawbacks, but none of them have as yet been the subject of sufficient theoretical study to fully ascertain their soundness. Furthermore, none of them have been subjected to the final test of experimental confirmation in a working device. These ideas have all developed too rapidly for that to have occurred.

Given this situation, the philosophy adopted for the plasma physics aspects of the reactor design study was to keep the physics requirements as simple as possible, without unduly sacrificing our objective of maximizing Q and reducing the level of technology required. Clearly a balance must be struck. On the one hand, one can be too conservative and arrive at a design

which gives an overly pessimistic view of the reactor potential of the tandem mirror concept. On the other hand, one can be too speculative and base the design on physics requirements which later prove to be unobtainable. In that case, much of the value of the design study is lost. For this reason we chose the inside barrier configuration with a standard minimum-B yin-yang magnet providing the MHD anchor and potential peak. This configuration is closer to the experimental base provided by TMX, 2X-II, and other devices and seemed less speculative than the other configurations. With the addition of ECRH in the barrier to generate a population of hot, magnetically trapped electrons and increase the barrier potential, a respectable Q was achieved. Neutral beam pumping was adopted as the means for removing trapped ions from the barrier since it is based on the straightforward physics of ionization and charge exchange. Drift orbit pumping was considered, but rejected because of some uncertainties with respect to the effect on electrostatic potential asymmetries in the central cell and interference with hot barrier electrons.

These arguments are not meant to negate reactor designs based on more exotic configurations. On the contrary, these configurations deserve more attention now that the WITAMIR-I study has been completed. This study provides a reference against which to measure the gains and risks of the more advanced end-plug configurations.

I-D. Blanket Design Philosophy

We had three main design objectives in this area:

1. *Good T_2 Breeding/Low T_2 Inventory/Good T_2 Confinement.*
2. *Long Design Life/High Degree of Reliability.*
3. *Safe Operation/Minimum Environmental Impact.*

The emphasis on reasonable T_2 breeding ratio is, of course, necessary for the reactor to continue operations at all. We felt that a TBR of at least 1.05 was necessary to cover uncertainties in breeding cross sections and for reasonable doubling times.

In order to achieve this value, and not "overbreed", we chose a Pb-Li eutectic, $Pb_{83}Li_{17}$. Not only does this alloy provide an adequate breeding ratio but it also resulted in giving the highest blanket multiplication (1.37) of any non-fissile blanket reported to date. Since the solubility of tritium is so low in the Pb-Li we were also able to keep the tritium inventory in the blanket to less than 100 g.

The confinement of tritium to the breeder material was accomplished by operating at a moderate coolant temperature (500°C max.) and the use of double-walled steam generator tubes. The normal T_2 leakage rate was calculated to be <20 curies per day.

The second objective was achieved by a combination of a simple, large flow conductance blanket design operating at low pressure (<100 psi). The materials choice (HT-9) was made because of its high resistance to fission neutron damage in this temperature range as well as its well-established technology in the light water fission reactor industry. An added degree of reliability was put into the system by designing all welds to be out of the high fluence neutron zone. While it has been known for some time that welded regions in all fusion reactor designs were potential problem areas, no previous efforts had been successful in completely removing them from the high damage area.

Finally, the safe operation and reduced environmental impact concerns were met by minimizing the T_2 inventory, reducing the fire and explosion hazard of Li metal by combining it with Pb, and choosing a material ($Pb_{83}Li_{17}$) which has an extremely low water reaction potential. Recent experiments at ANL have shown that even when $Pb_{83}Li_{17}$ at 500°C is dropped into H_2O it does not cause hydrogen explosions or fires.

When all of the factors are considered together, we feel that the WITAMIR-I blanket is probably one of the most reliable and maintainable and produces the most energy amplification of any of the previous seven designs done at Wisconsin.

I-E. Design Philosophy for WITAMIR Magnets

The magnet design problems posed by a tandem mirror are rather different from those faced in the tokamak. Major advantages for WITAMIR-I are the d.c. nature of the magnetic field and the comparatively straightforward demands of the central cell. Most of the magnet problems arise in the end plug where the fields lose axisymmetry, have steep axial gradients, and reach high maximum values. The principal objectives of our design have been to:

1. Make an economical design of central cell magnets that would give good access to the blanket for easy maintenance.
2. Take full advantage of the thermal barrier concept to reduce the peak field requirements on the plug and to shift the high fields from the non-circular coils to the barrier solenoid.

3. To reduce the size and fields on the non-circular coils so as to reduce the forces on these coils to manageable values.

In all three respects we believe that our WITAMIR-I design shows significant gains over our earlier design of a classical tandem without thermal barriers.

The central cell magnets have been designed with Al stabilizers and Al alloy structure to minimize weight and cost. At the penalty of some increase in peak field, the filling factor of the central cell has been reduced to 30% in order to maximize access to the blanket.

In the end plug the peak field is 15T which is found on the barrier solenoid. This coil is highly stressed and we have designed it for cooling with He II using a mixed Nb₃Sn, Nb-Ti-Ta, Nb-Ti conductor. The transition coils and yin-yangs have peak fields of 8T and can be constructed from Nb-Ti. The major radii are 2 m and 1.66 m, respectively, leading to moderate coil forces.

In summary we believe that the majority of the magnets for WITAMIR-I are of straightforward design. The high technology problems are located in the end plug, principally in the barrier solenoid. The tandem with thermal barriers appears to be considerably more favorable than our earlier classical tandem design.

I-F. Balance of Plant

The original scope of this study did not include the design of the building, power switching gear, hot cells, etc. We think that the present study is now ready for such an analysis and it should be conducted at the earliest possible time.

I-G. Conclusions

After looking over our original objectives and goals we find that most of them were attained. We still would like to be able to have a firmer plasma physics base for the end plugs, and we may even wish to design for a somewhat smaller power level. However considering the early stage of TMR physics with barrier cells we are more than satisfied with the present results. We fully ascribe to the philosophy of Rufus Jones who said,

"The true nature of a thing is the highest that it can become rather than the lowest to which it has fallen."

II. Overview

II-A. Introduction

Since the early 1970's the conceptual fusion reactor design field has been dominated by the tokamak concept.⁽¹⁻⁹⁾ While there are several positive features of that concept, there are many undesirable aspects of the tokamak that have emerged. The major areas where improvement is desired are:

1.) The maintainability is inherently difficult because of the toroidal geometry and interlocking coil configurations. Potentially long repair times raise serious questions about reactor relevant availability times.

2.) The pulsed nature of the burn cycle induces severe fatigue problems in the first wall and magnet structures.

3.) It is necessary to keep the plasma 'clean' of impurities by the use of exotic divertor and/or limiter designs, most of which must operate under heat fluxes and erosion rates beyond the ability of known materials to last the lifetime of the reactor.

4.) The engineering power density is rather low ($\sim 1 \text{ MW}_{\text{th}}/\text{m}^3$) and this results in rather high capital costs.

Some of the above problems were addressed by two other major magnetic fusion reactor concepts in the 70's, the theta pinch⁽¹⁰⁾ and the simple minimum B mirror⁽¹¹⁾, but both of these concepts introduced more problems than they solved. For example, the minimum-B mirror is a steady state device and does not require the plasma 'cleansing' configurations of the tokamak, but it has an uneconomically low Q (fusion power out divided by the input power) of only ~ 1.2 .

In 1976 the tandem mirror concept was simultaneously suggested both in the U.S.⁽¹²⁾ and in the USSR⁽¹³⁾ and it had Q's of $\sim 5-10$. However, the TMR designs placed unreasonably large demands on fusion technologies such as magnets (17 T) and the neutral beams (1 MeV). Fortunately, in 1979 Baldwin and Logan⁽¹⁴⁾ introduced the thermal barrier concept into the tandem mirror configuration which allowed much higher Q's (~ 10 to 20) to be considered while at the same time reducing the technology demands on the magnets and beams. The tandem mirror/thermal barrier concept appears to form the basis for an attractive commercial reactor design.

The objective of this paper is to summarize a conceptual commercial tandem mirror reactor design called WITAMIR-I. The details of the design can

be found in the remaining chapters of this report and a complete listing of all the reactor parameters is given in Appendix A. Only the major results will be reported here.

II-B. General Design Features

The basic configuration of WITAMIR-I is a long (165 m) solenoidal central cell terminated by an inboard thermal barrier and a yin-yang minimum-B plug. The mirror confined plasma in the plug provides the confining electrostatic potential and the good magnetic field curvature for MHD stability.

Perspective views of the WITAMIR-I reactor are shown in Figures II-B-1 and II-B-2 while Table II-B-1 summarizes its major parameters. From the figures we can see that in contrast to the tokamak reactor, which is a large torus, WITAMIR-I is essentially linear in nature. While the geometries differ considerably, the total nuclear island volume of each device is comparable. For example a recent tokamak reactor, STARFIRE⁽⁹⁾, which operates at roughly the same total power as WITAMIR-I, is ~ 34 meters in diameter and 25 meters high ($\sim 2.3 \times 10^4 \text{ m}^3$) while WITAMIR-I is ~ 250 m long and an average of ~ 10 m in diameter ($1.9 \times 10^4 \text{ m}^3$).

With a plasma Q of 28, the DT power level of WITAMIR-I is $3000 \text{ MW}_{\text{th}}$. Due to an extremely good blanket multiplication of 1.37 and direct conversion of the charged particles leaking from the central cell along with those injected into the end plugs, the net plant output is 1530 MW_e . The recirculating fraction is only 18%, a relatively low value for mirror reactor concepts.

The magnetic fields in the central cell and yin yang coils are relatively modest with maximum fields of 6.1 T and 8.1 T, respectively. The most difficult problem is in the cylindrical barrier coil which has a maximum field of 15 T, but even that appears to be feasible with a hybrid superconductor design and superfluid helium coolant at 1.8 K.

The blanket is made up of only two materials, HT-9 structural and reflector material and a $\text{Pb}_{83}\text{Li}_{17}$ coolant. The latter gives a comfortable tritium breeding ratio of 1.07. Coupled with its high energy multiplication (1.37) and reasonably high neutron wall loading of 2.4 MW/m^2 , the WITAMIR-I blanket design is one of the more attractive systems that has been designed to date.

The thermal barriers are maintained by neutral beam pumping. A total of 55.3 MW of 190 keV (42.5 MW) hydrogen particles and 9.6 keV (12.8 MW) hydrogen particles strategically placed keeps the ion density lower in the barrier region. The neutral beam power to the end plug is 18.2 MW at 500 keV. Finally,

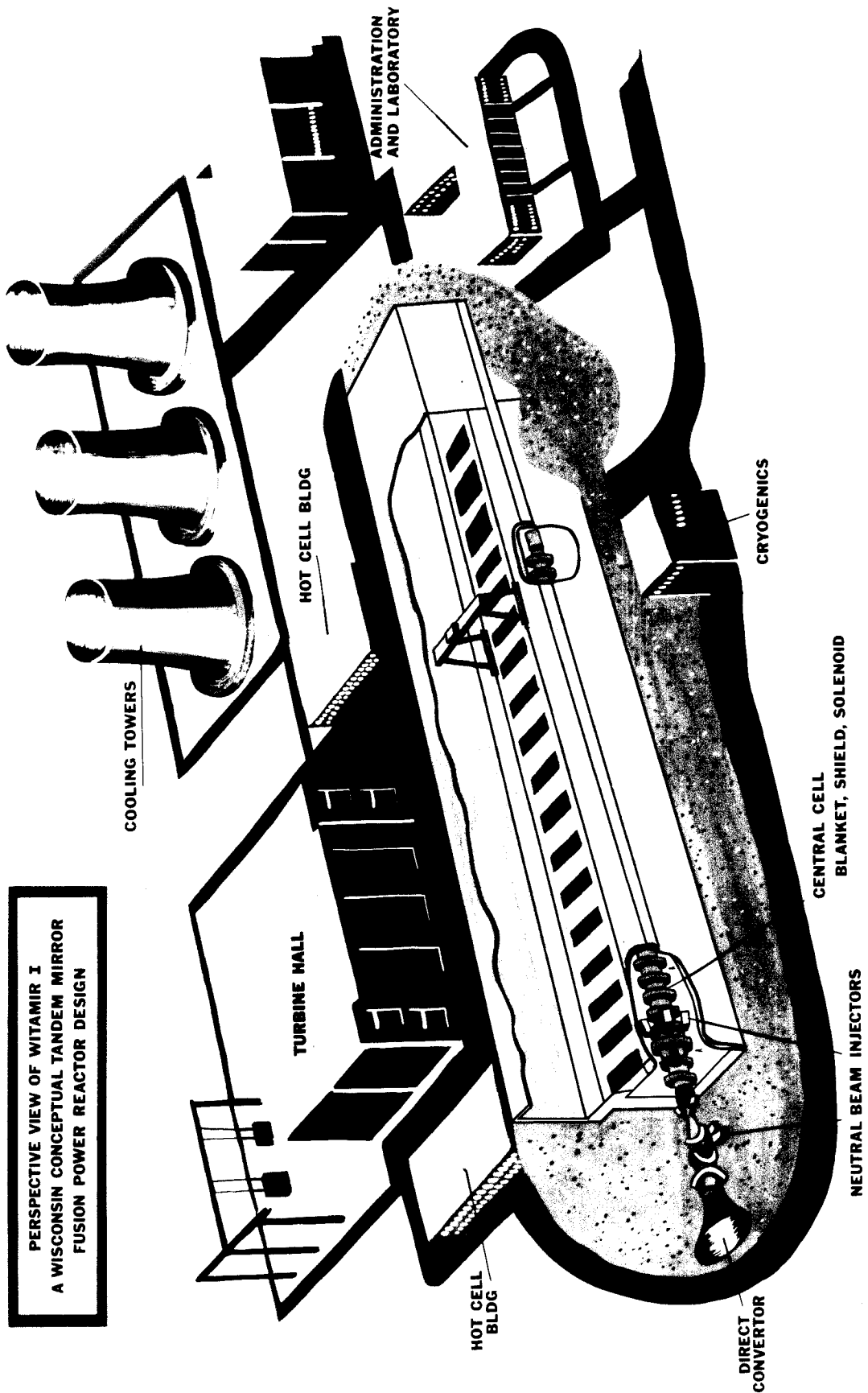


Fig. II-B-1

PERSPECTIVE VIEW OF WITAMIR I A WISCONSIN CONCEPTUAL TANDEM MIRROR FUSION POWER REACTOR DESIGN

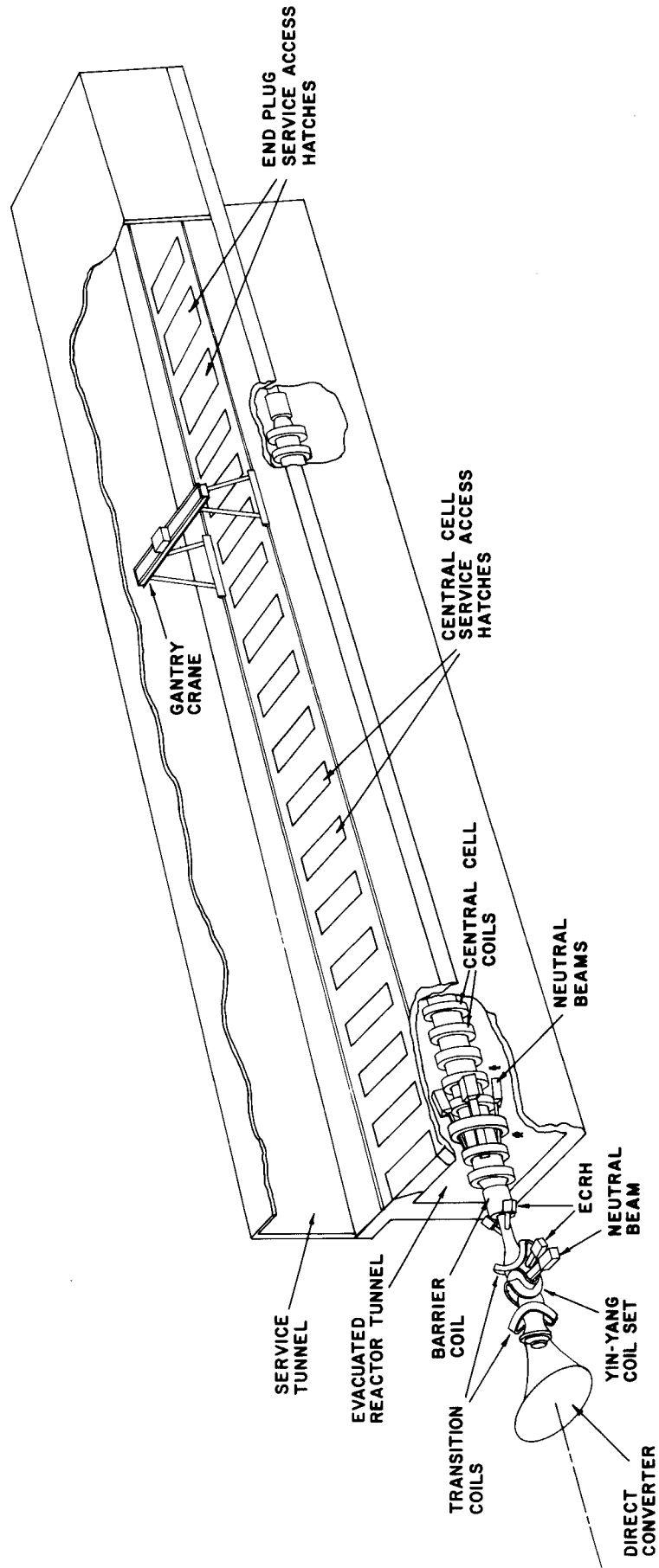


Fig. II-B-2

Table II-B-1
General Parameters of WITAMIR-I

<u>Parameter</u>	<u>Value</u>
Plasma Q	28
DT power	3000 MW _{th}
Net electrical output	1530 MW _e
Recirculating fraction	18%
Central cell length	165 m
Overall reactor length	250 m
Max. magnetic field - central cell	6.1 T
Max. magnetic field - barrier	15.0 T
Max. magnetic field - yin yang	8.1 T
Blanket material	HT-9
Neutron wall loading	2.4 MW/m ²
Blanket multiplication	1.37
Breeder material	Pb ₈₃ Li ₁₇
Breeding ratio	1.07
Barrier pumping method (190 keV and 9.6 keV)	55.3 MW of NB
ECRH power - barrier	33.2 MW (40 GHz)
- plug	16.4 MW (112 GHz)
Plug neutral beam power (500 keV)	18.2 MW

the electrons are heated in the barrier region by 33.2 MW of ECRH power at 40 GHz while electrons in the plug region are heated with 16.4 MW of ECRH power at 112 GHz.

II-C. Plasma Considerations

WITAMIR-I is the first full scale reactor study to consider the consequences of the modified Boltzmann relationship between density, potential and electron temperature proposed by Cohen et al.⁽¹⁵⁾:

$$e\Delta\phi = T_{ep} \ln \left[\frac{n_p}{n_b} \left(\frac{T_{ec}}{T_{ep}} \right)^{\nu_c} \right]$$

where $e\Delta\phi$ = plug potential
 T_{ep}, T_{ec} = electron temperatures in the plug and central cell, respectively
 n_p, n_b = ion densities in the plug and barrier regions, respectively
 ν_c = parameter to account for non-Boltzmann distributions of ions.

The magnetic field, potential, and particle density profiles for one end of WITAMIR-I are shown in Figure II-C-1 and the top/bottom cross sectional view of the end plug region is shown in Figure II-C-2. The potential ϕ_b is created by the density drop from neutral beam charge exchange pumping and flux tube expansion as the magnetic field falls from 14 T to 1.4 T. The potential ϕ_c , which confines central cell ions, is created by RF heating of plug electrons and by neutral beam injection, and ϕ_e is the ambipolar potential occurring because electrons are more collisional than ions and scatter more quickly into the loss cone. The hot electron density, n_{eh} , is created by RF heating at the barrier center. More details on the plasma parameters are listed in Table II-C-1. The pumping parameter, g_b , is the ratio of total barrier ions to passing ion density.

The detailed description of the plasma physics model used to calculate the plasma parameters in Table II-C-1 is contained in Chapter III and much of the present work is based on the models proposed by Baldwin et al.⁽¹⁴⁾ The main terms in the power balance are heating by the alpha particles, neutral

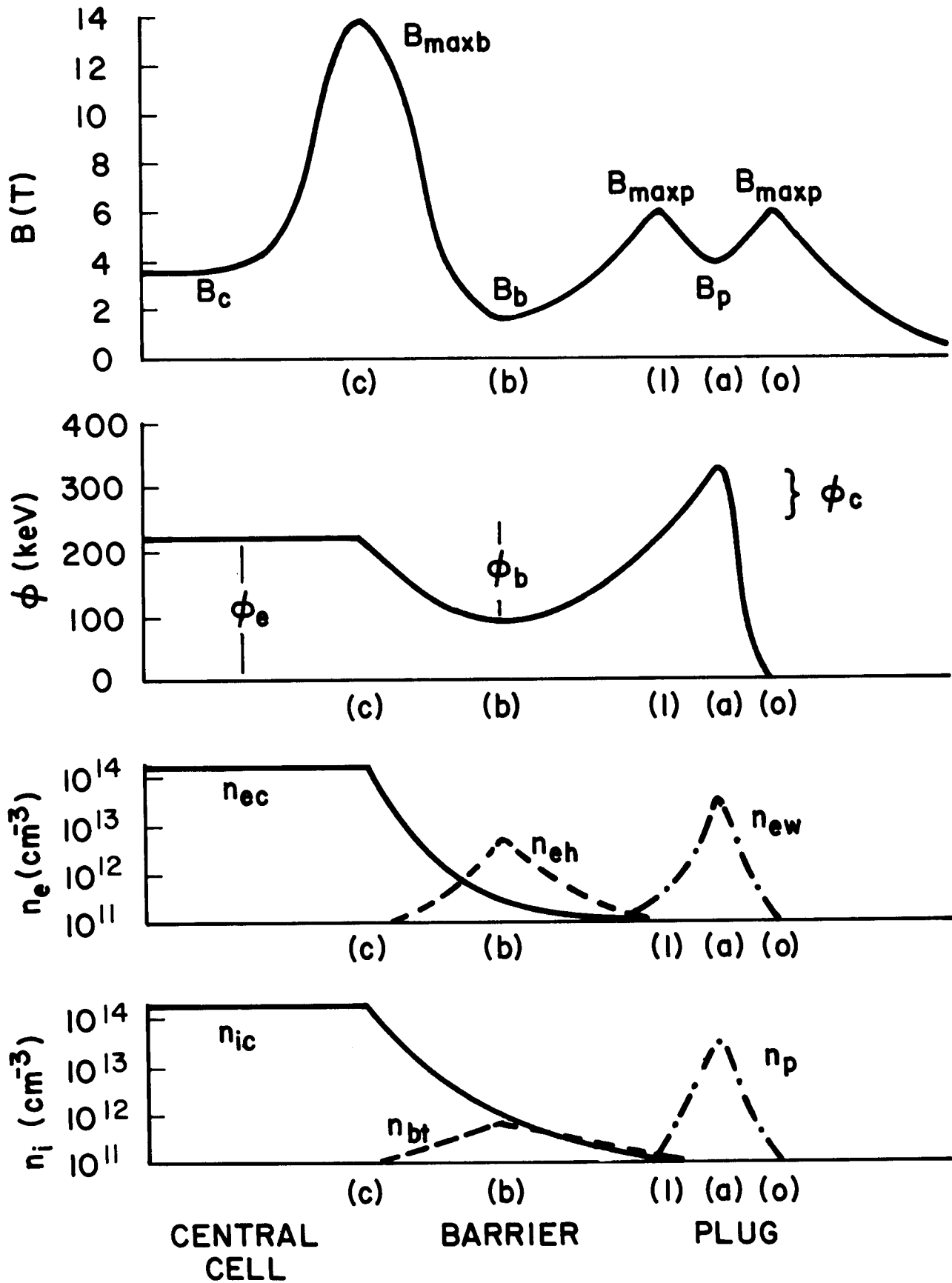


Fig. II-C-1

WITAMIR I A WISCONSIN CONCEPTUAL TANDEM MIRROR FUSION REACTOR DESIGN

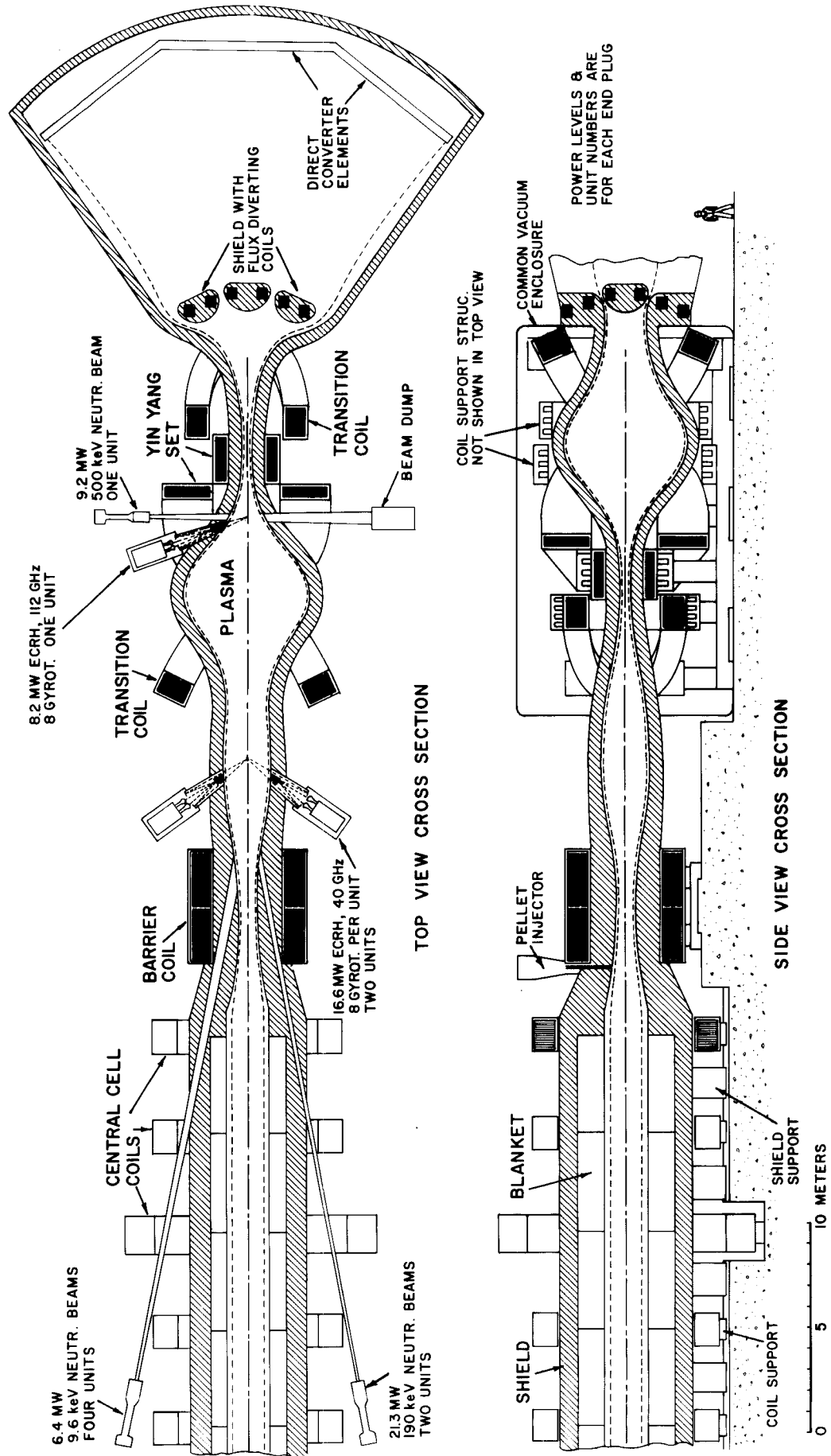


Fig. II-C-2

Table II-C-1
Plasma Conditions for WITAMIR-I

<u>Parameter</u>	<u>Value</u>
<u>Central Cell</u>	
Magnetic field on axis	3.6 T
Density	$1.5 \times 10^{14} \text{ cm}^{-3}$
Ion temperature	32.5 keV
Electron temperature	32.8 keV
Potential, ϕ_c	102. keV
Beta, β_c	0.40
Plasma radius	0.72 m
$(n\tau)_{ic}$	$7.8 \times 10^{14} \text{ sec cm}^{-3}$
<u>Barrier</u>	
Magnetic field on axis	14.0 T
Average density	$6.9 \times 10^{12} \text{ cm}^{-3}$
Ion temperature	32.5 keV
Mean hot electron energy, E_{eh}	270. keV
Passing electron fraction, F_{ec}	0.27
Pumping parameter, g_b	2.0
Pumping fraction at low energy	0.95
Pumping fraction at high energy	0.05
Potential, ϕ_b	141. keV
Beta, β_b	0.235
Plasma radius average	0.59 m
<u>Plug</u>	
Maximum/minimum magnetic field on axis	6.0/4.0 T
Average density	$2.7 \times 10^{13} \text{ cm}^{-3}$
Mean ion energy	905. keV
Electron temperature	123. keV
Potential, $\phi_c + \phi_e$	326. keV
Cohen parameter, ν_c	0.5
Beta, β_p	0.64
Plasma radius	0.77 m
$(n\tau)_{ip}$	$9.8 \times 10^{13} \text{ sec cm}^{-3}$

beams, and ECRH power balanced by end losses, radiative processes and energy carried out by charge exchange neutrals from the pumping process.

Whereas the central cell ions are deuterium and tritium, protons are used in the end plugs because of the desire to reduce neutron production and hence the amount of shielding required for the barrier coil and yin yang coils. Neutronics calculations show us that this approach was successful in reducing the radiation damage in the superconducting magnets to acceptable levels with only 10-20 cm of shielding. Better microstability is also expected with protons because they have smaller gyroradii than deuterium or tritium.

There are MHD stability questions with regard to the central cell beta. Theory now predicts values of roughly 25% although experiments have been consistently exceeding the theoretical limit. The value of 40% chosen here is lower than that used in past preliminary designs, but felt to be reasonable in light of probable future improvements in field design and the fact that MHD theory is somewhat tentative at this point.

The ions that are trapped in the barrier by collisional scattering must be 'pumped' out to maintain a density dip and therefore a potential ϕ_b . This is done in WITAMIR-I by charge exchange reactions with the neutral beams injected at approximately 10° to the axis, through the bore of an oversized central cell coil and a barrier coil (see Figures II-C-2 and II-C-3 for the WITAMIR-I reactor magnet configuration).

The electrons in the outside edge of the barrier coil will be heated by 33.2 MW of ECRH power at 40 GHz (see Figure II-C-4) to maintain the appropriate potential profiles. The ECRH power is provided by gyrotrons and transported to the plasma using a beam waveguide system. While the use of ECRH lowers some technology requirements and allows somewhat higher Q's, a tandem mirror without barrier ECRH is viable but not quite as attractive with respect to Q.

The plug region is essentially a minimum-B mirror machine, for which a great deal of experimental and theoretical base currently exists. Perhaps the most serious technology challenge in the end plug is the requirement of 2.4 MW of steady state 500 keV negative ion beams absorbed in each end plug plasma zone. The plug plasma is also maintained by 8.2 MW of 112 GHz ECRH power at each end.

Finally, the direct convertor is designed to collect all of the ions which escape over ϕ_c . By maintaining ϕ_c slightly higher on one end of the

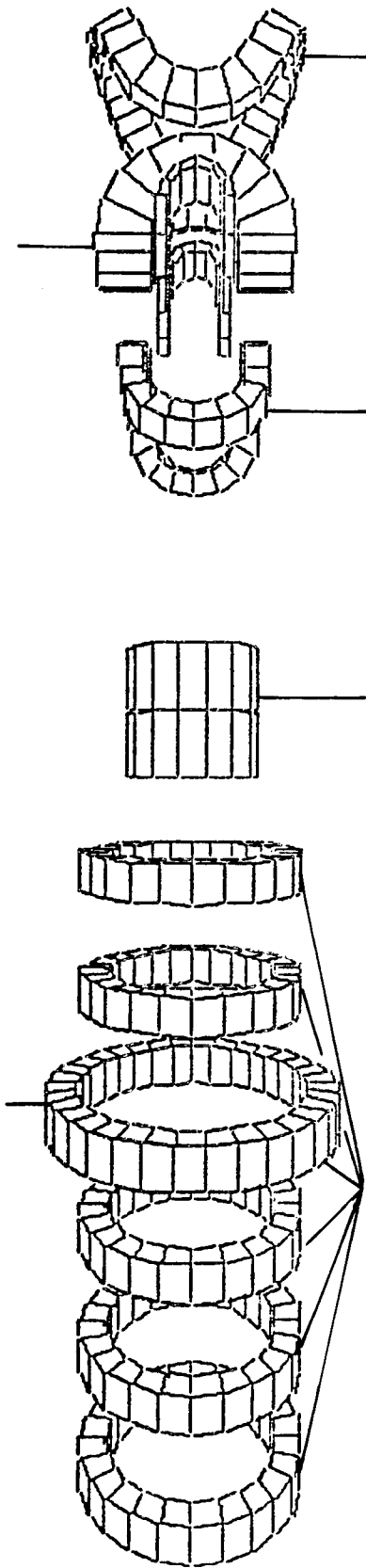
WITAMIR-I MAGNET ARRANGEMENT

Enlarged Coil for
Neutral Beam Access

Central Cell Solenoid
Coils

Plug

Yin-Yang



Central Cell Solenoid
Coils

Barrier
Coil

Transition
Coil

Recircularizing
Coil

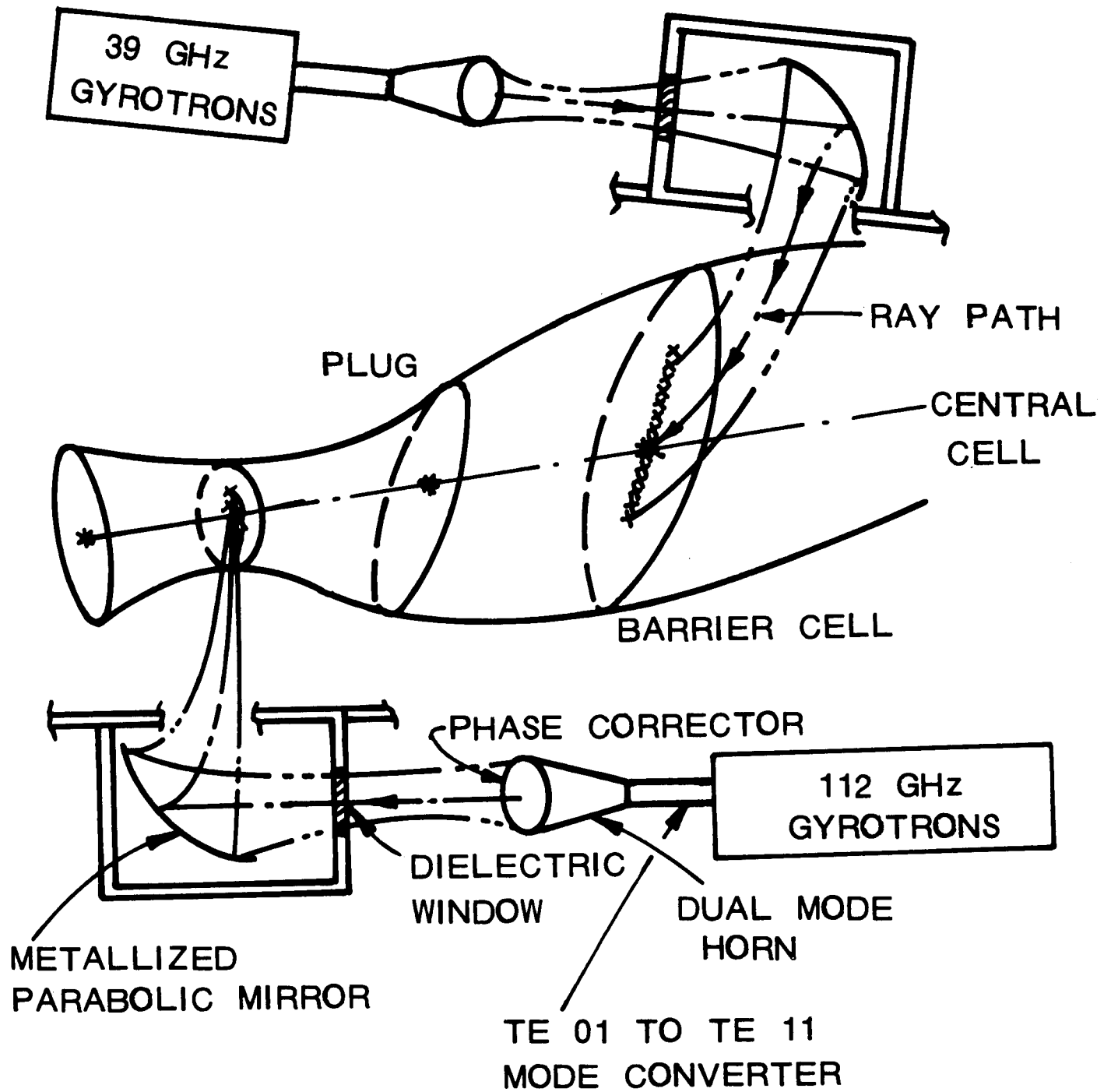
Coils

Coil

Coil

II-11

Fig. II-C-3



ECRH TRANSPORT SYSTEM (Not to scale).

machine, essentially all of the ions will escape out the other end, thus necessitating a direct convertor only on one end of the machine. Electrons are collected at the other end of the machine. The direct convertor handling the ions converts 502 MW of power at an efficiency of 67%.

II-D. Summary of WITAMIR-I Magnet Designs

There are three major superconducting magnet systems in WITAMIR-I; the central cell solenoidal magnets, the barrier coils, and the plug coils consisting of transition/yin yang/ and recircularizing coils. A schematic of the coil configuration at one end is shown in Figures II-C-2 and II-C-3 while Table II-D-1 lists the major characteristics.

There are 34 central cell coils, 2 barrier coils, 2 transition coils, 2 recircularizing coils and 2 yin yang coil sets. The two largest coils (11.4 m outside diameter) are those in the central cell which have to be expanded to accept the barrier pumping beams. The rest of the low field (6.1 T max.) central cell coils are only 8.6 meters in outside diameter and the high field (15 T max.) barrier coils are roughly 5 meters in diameter. The intermediate field (8.1 T max.) plug coils have outside diameters of equal to or less than 4 meters.

The construction of the central cell and plug coils should be straightforward with today's technology using NbTi superconductor and Cu or Al as the stabilizer. These coils will operate at 4.2 K and have modest average current densities of 800 to 1900 A/cm². On the other hand, the barrier coil is a hybrid design consisting of three zones. The high field region is Nb₃Sn, the intermediate zone is NbTiTa and the low field region is NbTi, all operating at 1.8 K. The average current densities are a maximum of 2000 A/cm² in the NbTi, and Cu is used as the stabilizing material for the entire barrier coil design.

Finally, the structural material for the central cell coils are Al alloys while stainless steel is used for the yin yangs, the transition and recircularizing coils. In the barrier coils, CuNb is used for structure. The total weight of all the 44 magnets is 5762 tonnes.

II-E. Blanket and Shield Design

A schematic of the WITAMIR-I blanket and shield design is shown in Figure II-E-1 and Table II-E-1 lists the important operating characteristics. Figure II-E-2 shows a cross sectional view and Figure II-E-3 gives the blanket compositions.

Table II-D-1
Major Superconducting Magnet Characteristics for WITAMIR-I

Parameter	Central Cell	Barrier Coil	Transition/Re-	Yin Yang Coil
			circularizing Coil	
Number of magnets	32(+2)	2	2+2	2+2
Major radius - m	4.3(5.7)*	2.51	2.0	1.66
Wt. of coil - tonnes/coil	88.1(162.5)*	429	283	157
Overall current density-A/cm ²	950(800)*	1000/1500/ 2000	1480	1900
Op. temp.-K	4.2	1.8	4.2	4.2
Max. field cond.-T	6.1	15	8.1	8.1
Max. field on axis-T	3.6	14	N/A	6
Superconductor	NbTi	Nb ₃ Sn/NbTiTa/ NbTi	NbTi	NbTi
Stabilizer	Al	Cu	Cu	Cu
Structural material	Al	CuNb	SS	SS

* 2 oversize coils to permit neutral beam injection.

CROSS SECTION OF WITAMIR I
CENTRAL CELL

II-15

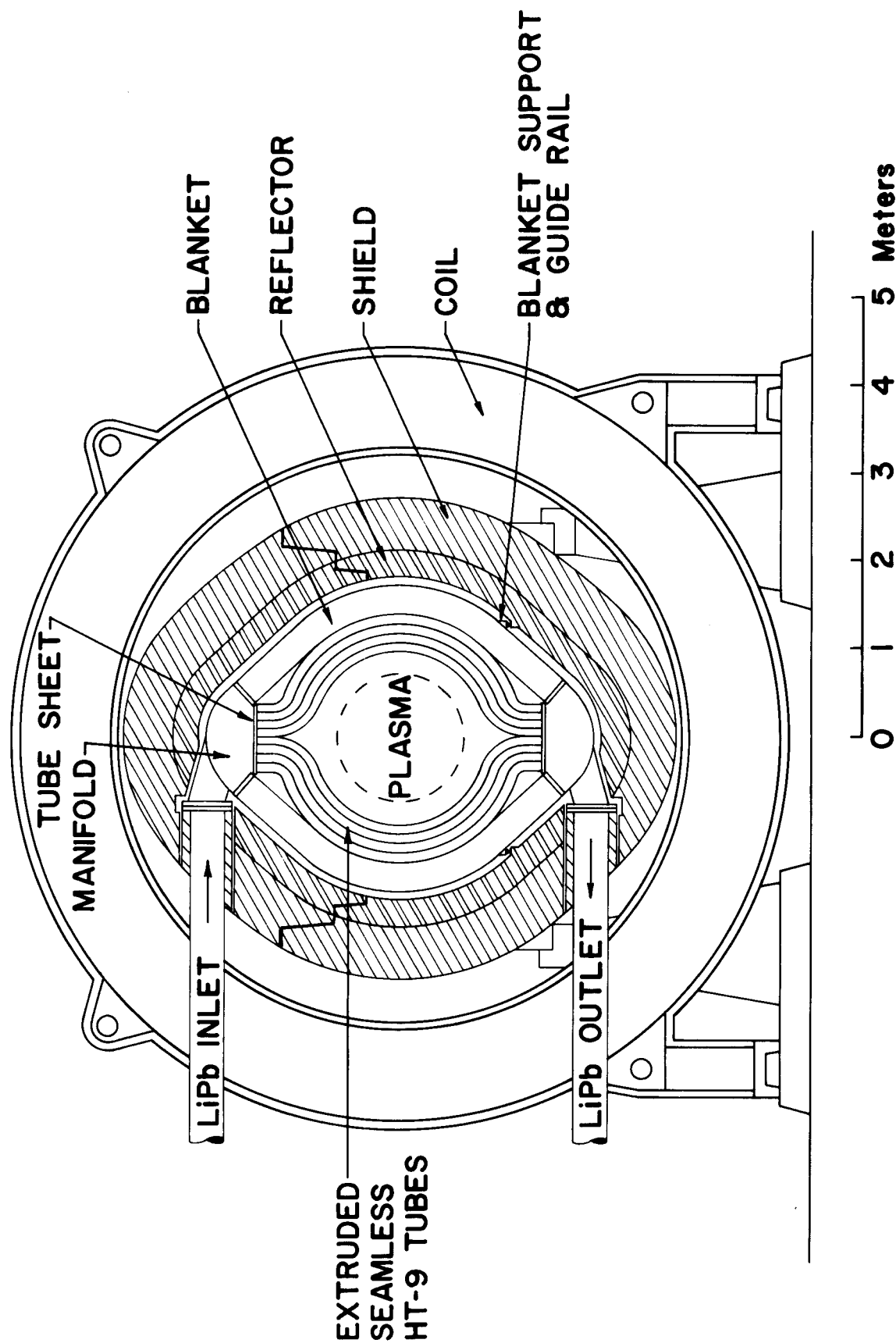
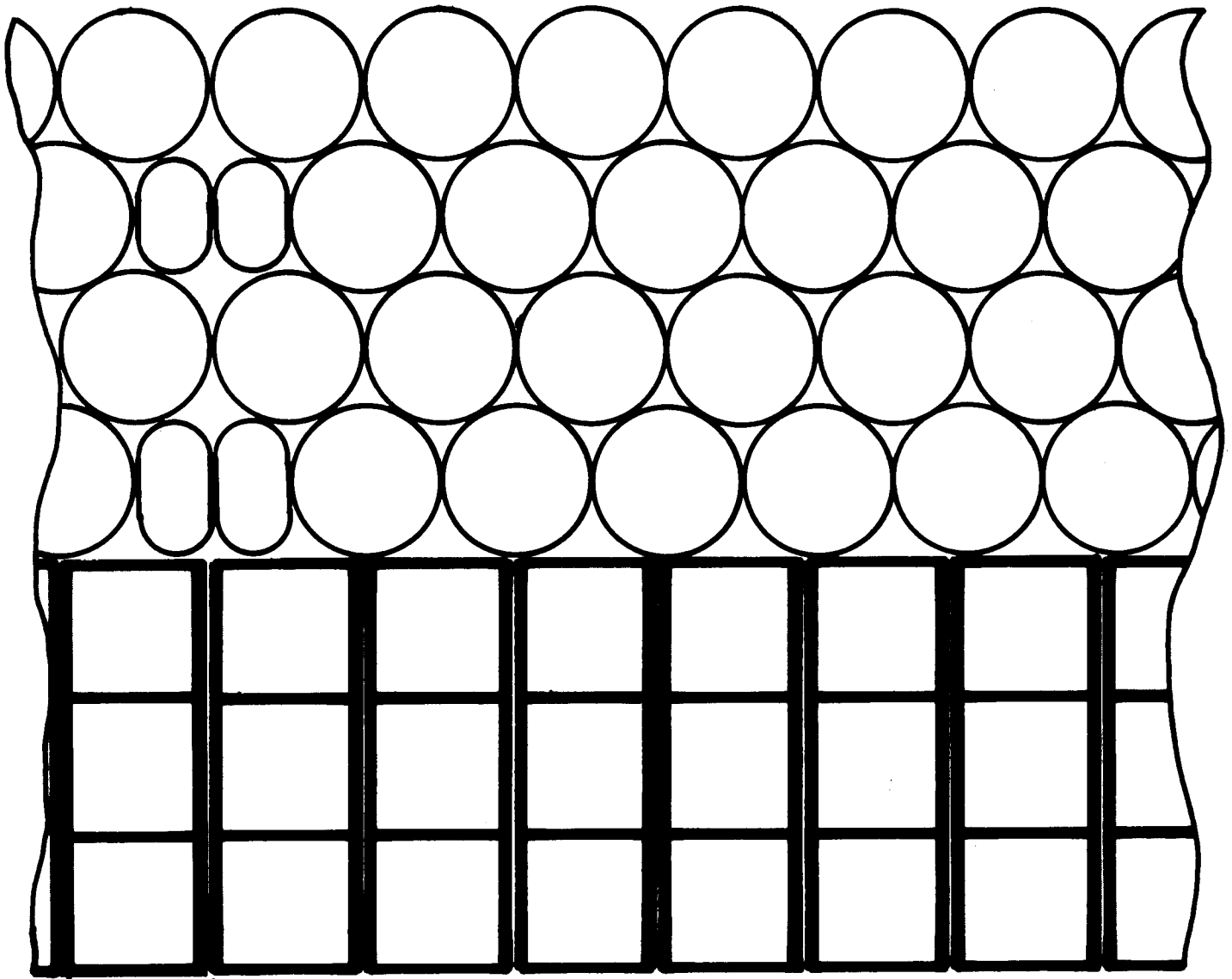


Fig. II-E-1

Table II-E-1
Summary of WITAMIR-I Blanket/Shield Parameters

<u>Parameter</u>	<u>Value</u>
Structural material	HT-9
Breeder and coolant	Pb ₈₃ Li ₁₇
Maximum structure temp.-°C	530
Inlet/outlet coolant temp.-°C	329/500
Inside diameter, central cell - m	1.94
Blanket/reflector/shield thickness - m	0.73/0.28/0.6
Neutron wall load - MW/m ²	2.4
Surface heat load - W/cm ²	2
Blanket multiplication	1.37
Tritium breeding ratio	1.07
Tritium inventory - kg	
Active	0.45
Storage (1 full power day)	2.14
Max. damage rate central cell magnets	
dpa/FPY - stabilizer (Al)	6.6x10 ⁻⁷
rad/FPY - insulator	3.6x10 ⁶
Afterheat at shutdown - MW	24
Radioactivity at shutdown - curies	3x10 ⁹

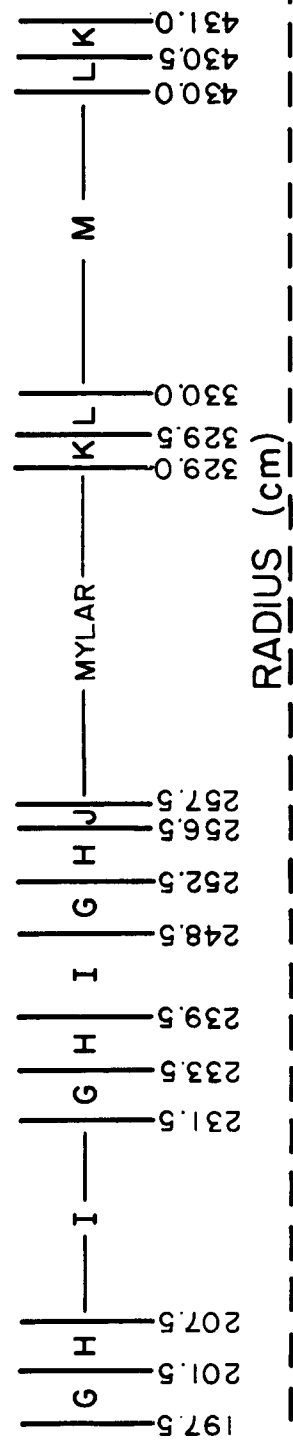
P L A S M A



JUNCTION BETWEEN ADJACENT
BLANKET MODULES

Fig. II-E-2

CROSS SECTION OF WITAMIR - I USED IN RADIOACTIVITY CALCULATIONS



COMPOSITION TABLE

A - 100 % FERRITIC STEEL (HT-9)	H - 87 % B ₄ C
B - 79 % Li ₁₇ Pb ₈₃	I - 95 % F.S., 5 % H ₂ O
C - 84 % Li ₁₇ Pb ₈₃ , 7 % FERRITIC STEEL (F.S.)	J - 100 % F.S.
D - 81 % Li ₁₇ Pb ₈₃ , 9 % F.S.	K - 100 % Al 2219
E - 75 % Li ₁₇ Pb ₈₃ , 25 % F.S.	L - 100 % He
F - 5 % Li ₁₇ Pb ₈₃ , 95 % F.S.	M - 17.8 % Al, 69 % Al 2219, 4 % He, 9.2 % INSULATION
G - 90 % Pb, 5 % H ₂ O, 5 % F.S.	

Fig. II-E-3

The structural material was chosen to be a ferritic steel, HT-9, mainly on the basis of its resistance to fission neutron damage. The maximum operating temperature is 530°C and it is cooled by a $\text{Pb}_{83}\text{Li}_{17}$ eutectic alloy which ranges in temperature from 329 to 500°C. The inside diameter of the central cell is 1.94 meters and the blanket/reflector region is ~ 1 meter thick. A shield of 0.6 m thickness is placed around the blanket to reduce neutron damage in the S/C magnets to a level which could be safely accumulated over 30 years of operation at 70% plant factor (21 full power years, FPY's).

The neutron wall loading of 2.4 MW/m^2 is not considered to be excessive because the surface heat flux is so low ($\sim 2 \text{ W/cm}^2$). This allows the first wall to be cooled by the Pb-Li alloy rather than requiring high pressure water as in the case of most recent tokamak designs which may have heat loads at the 40 Watt/cm^2 level or higher.

The excellent neutronic properties of the Pb-Li alloy allow one of the highest blanket multiplication factors to be attained of any reactor that we have designed thus far. This fact can be verified by examining the energy multiplication values of our most recent tokamak designs listed below:

Reactor	Structure/ Breeder Combination	Blanket Energy Multiplication
UWMAK-I (1)	316SS/Li	1.17
UWMAK-II (2)	316SS/Be-LiAlO ₂	1.28
UWMAK-III (3)	TZM/Li	1.29
NUWMAK (8)	Ti-6Al-4V/Pb ₃₈ -Li ₆₂	1.22
SOLASE (16)	C/LiO ₂	1.09
WITAMIR-I	HT-9/Pb ₈₃ Li ₁₇	1.37

The neutron multiplication of the Pb along with its low parasitic absorption cross section combines with the high gamma heating rate in the HT-9 to yield this very attractive blanket design. The lack of violent chemical reactions between $\text{Pb}_{83}\text{Li}_{17}$ and water, even at 500°C⁽¹⁷⁾, will be a distinct safety advantage as well.

The tritium breeding ratio of 1.07 is considered adequate to account for inaccuracies in the T_2 breeding cross section, decay, and losses of tritium to

waste streams. Because of the low tritium solubility in the Pb-Li alloy, the total 'active' inventory of T_2 in WITAMIR-I is a mere 0.45 kg. This extremely low value compared to past multi-kg inventories in Li coolants alone will be a distinct safety advantage. The storage of tritium for 1 full power day (2.14 kg) should not represent a significant hazard as it can be done away from the reactor in a solid form which is not prone to accidental release.

The total afterheat in the blanket and shield of 24 MW at shutdown is only 0.8% of the heat generated in the blanket during plasma operation. Such heat generation can be easily conducted away through the liquid metal coolant without significant temperature increases in the blanket material. The total radioactivity in the WITAMIR-I blanket and shield at shutdown is 3×10^9 curies. This level is comparable to previous reactor studies on a curie per watt basis, i.e., about 1 curie per watt.

The flow of reactor coolant and breeder material in WITAMIR-I is from the top to the bottom through seamless HT-9 tubes which are 9.75 cm in diameter. These large tube diameters allow rather low flow rates, 0.13 meters per second, which in turn should alleviate corrosion/erosion rates and pumping power losses. The removal of the welded zones to at least 1 meter behind the first wall should help to reduce failures because welded structures are notoriously susceptible to neutron damage.

The displacement damage to the HT-9 first wall material is 40.5 dpa per FPY and the helium production rate is 281 appm. While the helium production rates should not present a problem, we anticipate that we will have to replace the blanket modules after 3 full power years, or 3.8 years at 80% availability.

The procedure for replacing the blanket modules is shown in Figure II-E-4. The central cell magnets on either side of the blanket module to be replaced are moved 0.75 m in each direction. The top shield is lifted off and the damaged module is removed through an access hatch in the evacuated reactor tunnel. Replacement of a new module follows the reverse procedure and the only connections that have to be made are the coolant headers. There are no welded joints to be broken because the blanket sections are not attached to adjacent modules. Vacuum tight seals are made at the back of the shield and the reactor tunnel is evacuated to 70 torr during operation.

ISOMETRIC VIEW OF WITAMIR I
CENTRAL CELL

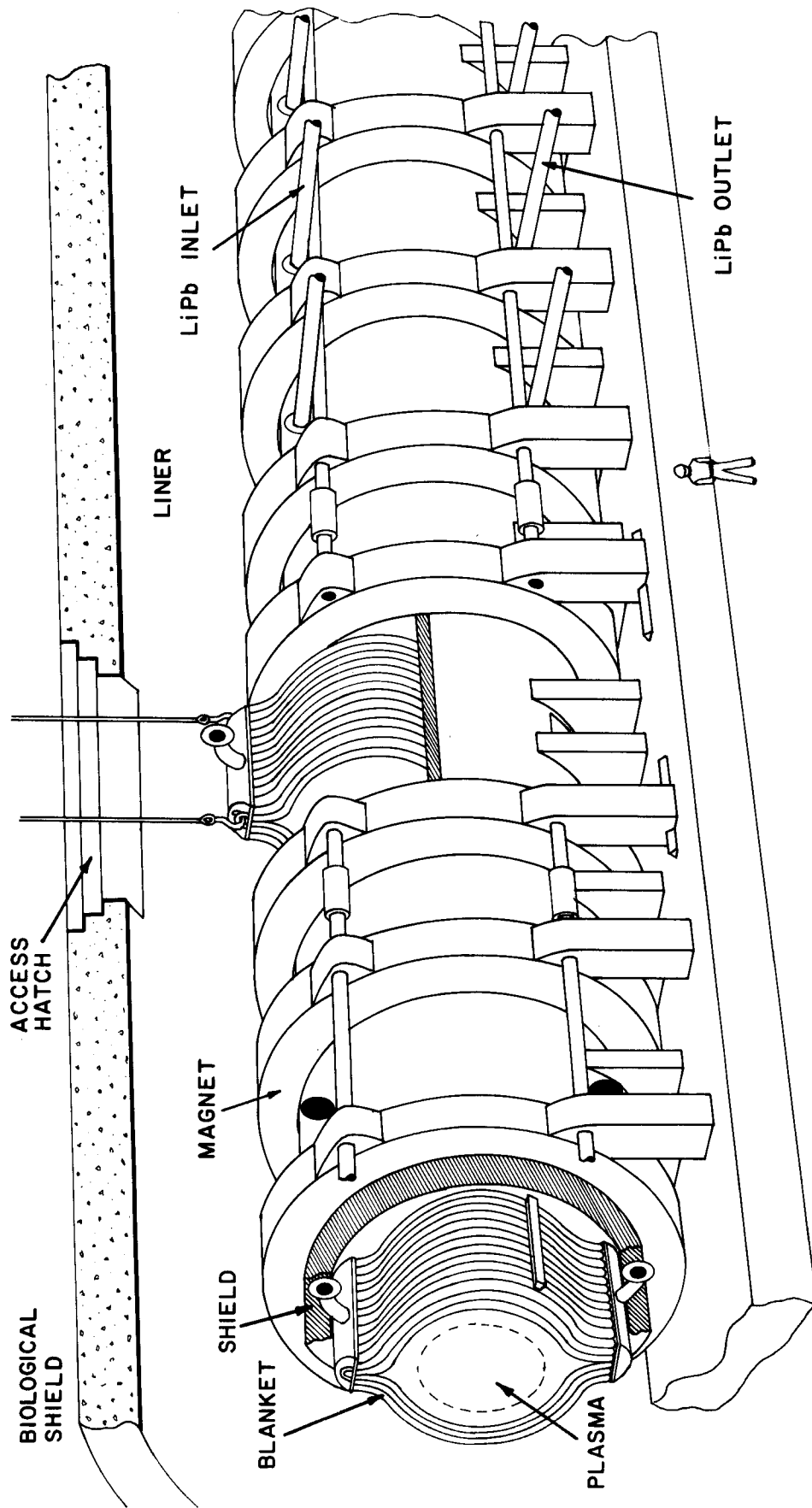


Fig. II-E-4

II-F. Power Cycle Features

The production of useful electric power in WITAMIR-I has been given considerable attention. The thermal energy released from the DT reactor is converted to electrical energy by both a conventional steam cycle and by a direct convertor. Table II-F-1 and Figure II-F-1 summarize the important parameters in this regard.

Starting from 3000 MW of DT power in the central cell we find 2400 MW of neutron power going to the blanket (plus 13 MW of neutron power from the kinetic energy of the reacting ions) and 600 MW in charged particle energy to the end plugs. With a blanket multiplication of 1.37 we find some 3306 MW of thermal power generated by the neutrons in the blanket, reflector and shield. The 2987 MW in the blanket is added to 30 MW from the barrier pump beams, 29 MW from surface heating of the central cell, 120 MW from the energy dump at the opposite end from the direct convertor, 59 MW from the barrier, 114 MW from the direct convertor plates and 310 MW from the reflector region to give 3649 MW to the primary heat exchanger. The gross efficiency of the steam cycle is 42% resulting in 1530 MW_e gross electrical output.

Because of the desire to keep the neutron shield cool, the difficulty in recovering the energy in the 500 keV beam that passes through the end plug plasma, and the difficulty in recovering energy from the director convertor grids, we make no attempt to recover the 9, 16 and 52 MW respectively associated with those regions. We also do not collect the 107 MW of beam power rejected from the formation of the neutral beams and ECRH system. Therefore, 2303 MW of thermal energy is dumped to the atmosphere.

Out of the 600 MW in charged particles and electrons, the 73 MW of particles injected into the plug and the ECRH heat (50 MW) to the electrons in the barrier and plug (a total of 723 MW), only 502 MW is actually converted directly to electricity at a 67% efficiency. Of the 338 MW_e electrical output from the direct convertor, 8 MW is used for the power supplies and the resulting 330 MW_e is added to the electricity from the steam system (1530 MW) to give a gross plant output of 1860 MW.

The plant requirements for auxiliary electricity amount to 24 MW for cryogenics and vacuum systems and 306 MW for neutral beams plus ECRH heating systems. This power drain results in a recirculating fraction of 17.7%, a quite reasonable number compared to previous mirror reactor designs. The net

Table II-F-1Summary of WITAMIR-I Power Parameters

DT power released in central cell - MW	3000
Neutron energy to blanket - MW	2400+13 ^(a)
Blanket and shield multiplication	1.37
Total neutron power - blanket - MW	2987
Total neutron power shield - MW	310
Power to first wall - MW	29
Power from beams collected by steam cycle - MW	322
Total thermal power to steam cycle - MW	3649
Gross electric output steam cycle ($\eta = 42\%$) - MW _e	1530
Total power to direct convertor - MW	502
Thermal power to plates - MW	114
Thermal power to grid - MW	52
Direct convertor efficiency - %	67
Net electrical power out - MW _e	330
Total gross electrical power reactor - MW _e	1860
Recirculating power - MW _e	330
Recirculating fraction - %	18
Net electrical output - MW _e	1530
Net efficiency - %	39.4

a) From kinetic energy of reacting ions

POWER FLOW DIAGRAM FOR WITAMIR I

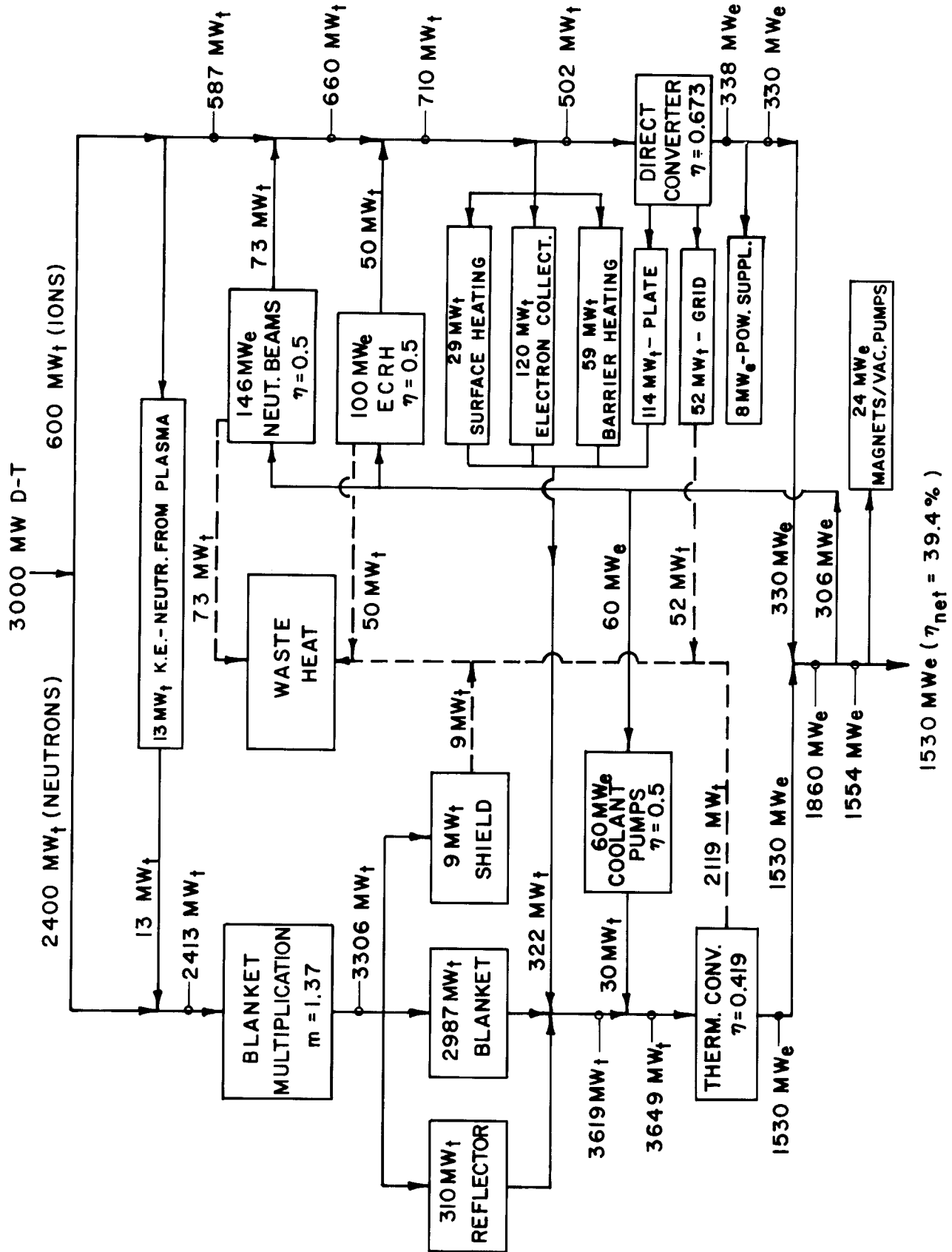


Fig. II-F-1

output of the plant is 1530 MW and the overall net efficiency is 39.4%, again a very respectable value.

II-G. Economic Features of WITAMIR-I

The WITAMIR-I reactor was costed in accordance with the DOE guidelines on "Fusion Reactor Design Studies - Standard Accounts for Cost Estimates". A summary of the major reactor capital costs is given in Table II-G-1 and the electricity costs in Table II-G-2. Graphical representation of the reactor plant equipment cost components is given in Figure II-G-1.

It can be seen from Table II-G-1 that 76% of the direct costs of WITAMIR-I are related to the Reactor Plant Equipment (RPE) and it was found that ~ 25% of the RPE costs are for the magnet system (roughly half of the magnet costs are in the central cell).

The next largest cost is the primary heat transport system (23% of RPE) followed by supplemental heating costs (21% of RPE). This latter cost is much higher than in tokamaks where the heating is only needed for a few seconds, each burn cycle to heat to ignition. Including in the indirect costs of 722 million dollars, we find the total of direct and indirect costs is 2,785 million dollars. The total capital costs are calculated for both constant dollar (1980) and current dollar (1988) conditions assuming an 8 year construction time. These calculations reveal a constant dollar capital cost of 2130 \$/kW_e (1980) and a current dollar cost of 3144 \$/kW_e (1988).

The cost of electricity from WITAMIR-I depends on capital, operation and maintenance (O&M), and fuel costs. Data from Table II-G-2 show that the capital costs dominate the electricity costs (84-89% of the total) and they are followed by O&M costs (11-16%) and low fuel costs (< 1%). The electricity cost of 36.1 mills per kWh (based on an availability of 80%) is roughly half of the current dollar value (75.7 mills/kWh).

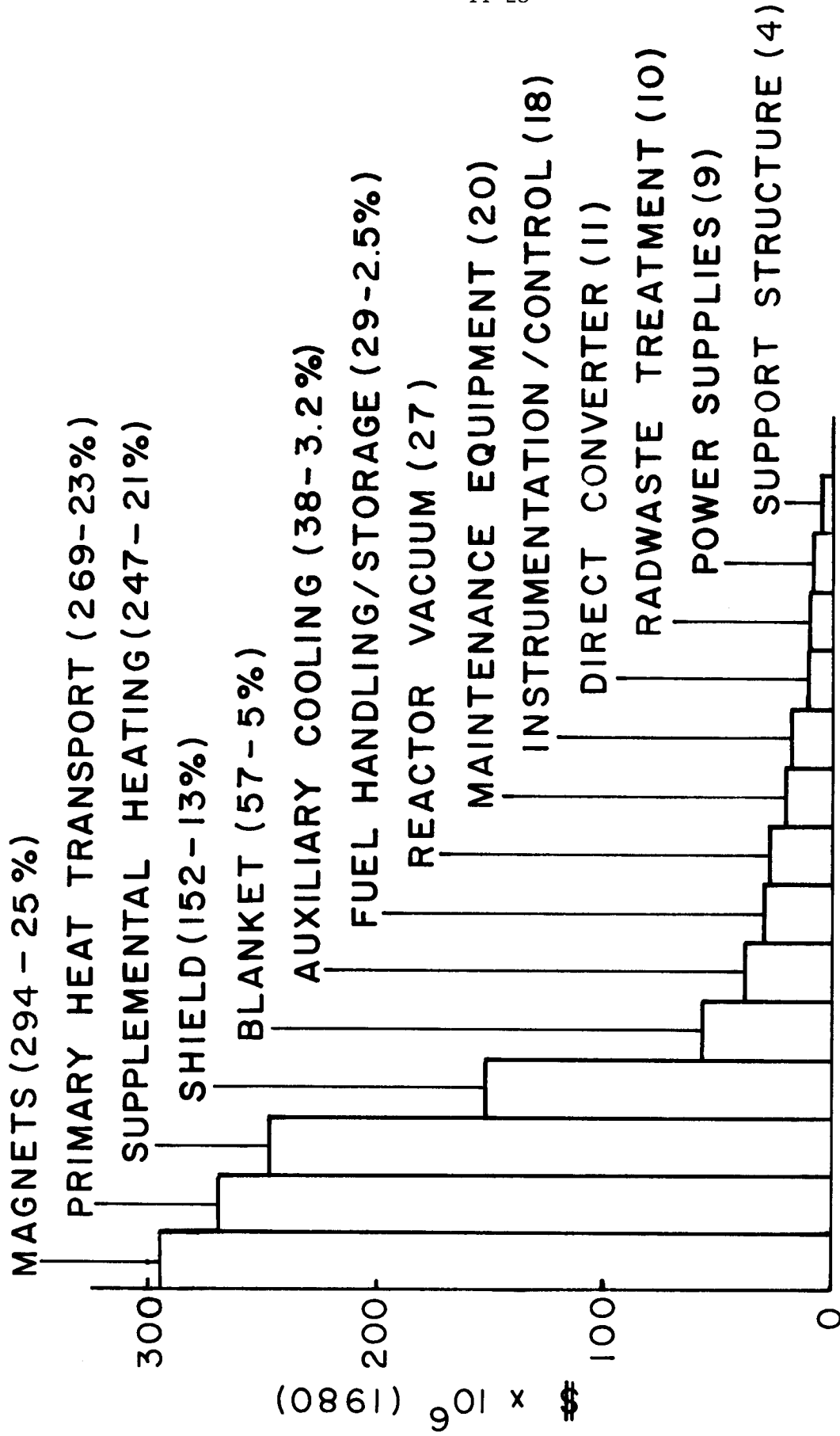
A comparison of capital and electricity costs of the tandem mirror reactor, WITAMIR-I, and the tokamak, NUWMAK, is made in Table II-G-3. Both reactors were costed on the same basis, by the same design group, and placed at the same dollar level. The main conclusion to be drawn from Table II-G-3 is that, to the present level of understanding, both reactors cost the same and produce electricity at roughly the same value even though the reactor concepts are quite different. In some respects, NUWMAK represents a rather advanced design, invoking plasma physics and technologies that have yet to be proven in practice. However, NUWMAK is smaller (DT power 2100 vs. 3000 MW_{th})

Table II-G-1
Summary of Estimated WITAMIR-I Costs

Account	Category	Constant Dollarx10 ⁶ (1980)	Current Dollarx10 ⁶ (1988)
	<u>Direct Costs</u>		
20	Land and land rights	3	
21	Structure and site facilities	129	
22	Reactor plant equipment	1565	
23	Turbine plant equipment	200	
24	Electric plant equipment	145	
25	Miscellaneous plant equipment	18	
26	Special materials	3	
		2063	
	<u>Indirect Costs</u>		
91	Construction facilities	309	
92	Engineering and construction	309	
93	Owner's cost	103	
		722	
Total Direct and Indirect Costs		2785	2785
Time Related Costs		474	2024
Total Capital Costs		3259	4210
Capital Costs of WITAMIR-I			
(1530 MW _e) \$/kWe		2130	3144

Table II-G-2
WITAMIR-I Cost of Electricity

	Constant	Current
	1980 \$	Dollar
	mills/kWh	1988 \$
Capital	30.4	67.3
O & M	5.6	8.3
Fuel	0.1	0.1
	36.1	75.7



BREAKDOWN OF REACTOR PLANT EQUIPMENT COSTS

Fig. II-G-1

Table II-G-3
Comparison of Plant and Busbar Costs Between
WITAMIR-I and NUWMAK

	Constant Dollars (1980)	Current Dollars (1988)
<u>Plant Costs</u>		
WITAMIR-I	\$2130/kW _e	\$3144/kW _e
NUWMAK	\$2227/kW _e	\$3288/kW _e
<u>Busbar Costs</u>		
WITAMIR-I	36.1 mills/kWh	75.7 mills/kWh
NUWMAK	37.5 mills/kWh	79 mills/kWh

and that may make it somewhat more expensive than WITAMIR-I on a per kW_e basis. Nevertheless, it is encouraging that the first full scale tandem mirror design has come so close to the economic assessment of the more advanced tokamak systems.

II-H. Conclusions

The WITAMIR-I reactor has several positive features that make it an attractive fusion power system compared to past tokamak designs.

- 1.) Its steady-state power production eliminates the critical fatigue problems, both in the first wall and magnets, which have plagued the tokamak reactor designs for a decade.
- 2.) The linear geometry makes maintenance of the most highly damaged sections relatively easy.
- 3.) The lack of high first wall heat fluxes, plasma disruptions, or high magnetic fields in the neutron damage region makes blanket design relatively simple and allows more effective use of liquid metals.
- 4.) The use of a direct convertor allows a relatively high overall net electrical efficiency to be attained (~ e.g. 39% in WITAMIR-I).
- 5.) The decoupling of the highest technology components (i.e., end plug regions) from the intense neutron flux allows competitive capital and electricity costs to be attained. This is especially true relative to tokamak reactors which have had a longer history of study.
- 6.) Radiation streaming is much more manageable in WITAMIR-I than in tokamaks due to the absence of large penetrations to the reaction chamber. There is a small solid angle opening to the plugs which is also shielded and contains a blocking shield to line of sight, on-axis streaming.

Other features of WITAMIR-I that need to be highlighted are:

- 1.) With a plasma Q of 28 the recirculating power is only 18%, much lower than past reactor designs.
- 2.) The use of the thermal barrier concept allows relatively high neutron wall loadings to be attained with modest extrapolations of current magnet and neutral beam technology. There are two exceptions to that statement, but neither is expected to present insurmountable problems:
 - A.) The design of the cylindrical superconducting barrier coil operating at a maximum field of 15T.
 - B.) The construction of ~ 40 MW of 500 keV, steady-state negative ion neutral beams to deliver 18 MW to the end plug plasmas.

3.) The damage induced by neutrons streaming into the end plug region does not appear to be a major problem. This is due to geometrical and R^{-2} effects.

4.) The physics basis for the thermal barrier concept needs to be verified experimentally (presumably in MFTF-B). Also, the stability limits to achieve reasonable central cell beta values of $\sim 40\%$ need to be verified.

5.) The cost of ECRH power needs to be carefully assessed. For example, if the ~ 100 MW of ECRH power in WITAMIR-I costs 1\$/Watt delivered, then this 100 million dollars represents a manageable amount of investment. However, should the cost of ECRH power rise to 5.00\$/Watt, then \$500 million dollars would be needed for heating electrons, probably more than can be economically included in the capital costs.

Finally, it is our overall conclusion that tandem mirrors with thermal barriers represent a sufficiently attractive concept that further study is highly desirable. Such reactor concepts could make more attractive technology and materials test facilities than can be the case for tokamaks and future studies should explore these possibilities.

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III. Plasma Analysis

III-A. Introduction

The tandem mirror as a confinement scheme was invented in 1976 by Fowler and Logan ⁽¹⁾, and independently in the USSR by Dimov ⁽²⁾. Since the motivation for the tandem mirror is the poor Q of a single mirror cell, the reactor implications of this concept were immediately a subject of study at Livermore ⁽³⁾, Wisconsin, and elsewhere. The standard tandem mirror configuration turned out to require very high magnetic field strengths in the plugs (~ 15 - 17 T) and very high energy neutral beams (~ 1 MeV). Even with these requirements, the Q was marginal (~ 5 - 10) and the neutron wall loading in the central cell was low ($\sim .5$ - 1 MW/m²). Using RF in the plugs allowed the neutral beam energy to be reduced to ~ 500 keV, but the other characteristics remained.

The introduction of the thermal barrier ⁽⁴⁾ allowed one to thermally isolate the plug electrons from the central cell electrons. The plug electrons can then be heated using ECRH, or other mechanisms, without having to heat the central cell electrons. The latter is not only power-consuming, but also undesirable since, for a given beta, it reduces the allowable ion density and thereby the power density in the central cell. The hotter electrons in the plug mean that the plug ion density can be reduced relative to the central cell density and still achieve a sufficient ambipolar potential for ion confinement. The consequence of this is that one can achieve a higher ion density in the central cell, raise the neutron wall loading, and simultaneously reduce the magnetic field and neutral beam injection requirements of the plug ^(5,6). The net effect is substantially higher Q (~ 20 - 30), neutron wall loading (~ 2 - 3 MW/m²) with a peak plug field of the order of 9 T at the coil, and neutral beam injection energy ~ 400 - 500 keV.

Sustaining the thermal barrier requires that ions trapped in the barrier by collisional scattering be "pumped out" in some manner. The LLL design ⁽⁵⁾ used neutral beam pumping to remove these ions. The neutral beams injected into the passing ion part of the velocity space "pump" trapped ions by charge exchange between the trapped ions and the neutral atoms. An alternative pumping mechanism is drift orbit pumping ⁽⁷⁾, where the drift of trapped ions carries them to limiters where they are neutralized. This appeared to us as an attractive alternative to neutral beam pumping since it is simpler, does not require additional beams, and does not introduce the cold electrons which

then have to be heated. As a consequence, it leads to somewhat higher Q than neutral beam pumping.

Recently, a careful theoretical analysis of the ambipolar potential in a mirror containing passing electrons with a colder temperature than the trapped electrons found that the Boltzmann relation between the plug and barrier potentials used in Ref. 4-6 is inappropriate and needs to be modified. The corrected result is

$$\phi_c + \phi_b = T_p \ln \left[\frac{n_p}{n_b} \left(\frac{T_c}{T_p} \right)^\nu \right] \quad (1)$$

where $\phi_c + \phi_b$ is the potential difference between the bottom of the barrier and the top of the plug, T_c is the central cell electron temperature, T_p is the plug electron temperature, n_p is the plug ion density, and n_b is the barrier ion density. The parameter ν is approximately .5 when the plug electrons are confined inside a magnetic mirror but $\nu \approx 1-1.5$ when the magnetic field is attempting to eject the electrostatically confined plug electrons. The latter case corresponds to the potential peaking at a magnetic hill, or in a region of a rising magnetic field. In the design presented in this report, the potential peaks inside a mirror, so we have taken $\nu = 0.5$.

The effect of the (T_c/T_p) term in Eq. (1) is to reduce the potential $\phi_c + \phi_b$ for a given density ratio (n_p/n_b) . As a consequence, the preliminary barrier tandem mirror reactor designs^(5,6) were optimistic since these designs were based on $\nu = 0$. The more recent ideas in which the ambipolar potential peaks on the side of a magnetic mirror are even worse off; for them the appropriate value for ν is ~ 1 . The effect of ν can be mitigated, however, by heating the electrons in the barrier with ECRH to create a population of hot magnetically trapped barrier electrons⁽⁹⁾. The effect of this is to reduce the density n_b of central cell electrons at the barrier minimum. In this case, Eq. (1) is modified by the replacement $n_b \rightarrow n_b - n_b(\text{hot})$, where $n_b(\text{hot})$ is the density of hot magnetically trapped electrons in the barrier. In this way one can recover a reactor performance similar to the previous designs which used $\nu = 0$.

The drift surfaces of the hot, magnetically confined barrier electrons are similar, however, to those of the trapped ions. Consequently, one cannot simultaneously use drift orbit pumping and ECRH heating of barrier trapped electrons. We have elected to use ECRH heating of the barrier trapped

electrons and drop drift orbit pumping in the reactor design. The former is more important to obtaining high Q operation.

A serious unresolved question of the tandem mirror is the MHD stability against ballooning modes. The numerical calculations of Kaiser⁽¹⁰⁾ yield a less than satisfactory limiting central cell beta ($\lesssim 25\%$) for ballooning stability so far. The main driving terms for the instability are the bad curvature of the magnetic field in the recircularizer region and in between the central cell and barrier coil where the cross-sectional area of the flux tube is being reduced by the high field barrier coil. Optimization of the rate of tapering of the flux tube cross-section has minimized the latter effect⁽¹¹⁾, but the bad curvature in the recircularizer region remains a serious problem. Accomplishing the recircularization of the magnetic flux surface at higher field should have a beneficial effect since it reduces the plasma beta in the region of bad curvature. Reducing the ellipticity of the "fan" in the minimum-B region should also reduce the bad curvature in the recircularizer region. It is hoped by "minor" adjustments of this sort that a central cell beta of 40% can be achieved. This is to be thought of as a goal, since de-tailed MHD stability calculations have not yet been done for the WITAMIR-I design.

A drastically different tandem mirror configuration has been proposed by Logan⁽¹²⁾ in which the magnetic mirror with its ambipolar potential is connected to the central cell through a magnetic cusp. In this configuration the central cell can have purely good curvature and thus have $\beta \sim 1$. There are many unassessed physics questions with this configuration. The cusp arrangement is also far removed from the present and planned tandem mirror experimental program (TMX, Phaedrus, MFTF-B). Consequently we feel that is premature to base the present reactor design on this configuration. The possibilities and limitations of the more standard configurations have, in our opinion, not yet been sufficiently established. Consequently, we have decided to base the reactor design on the more standard magnetic configuration.

In Section III-B the zero dimensional model for barrier tandem mirrors is presented and discussed. Parametric studies of the reactor performance have been performed using this model; these are presented in Section III-C. The particular design point chosen for WITAMIR-I is presented in Section III-D. A discussion of microstability considerations for WITAMIR-I is given in Section

III-E. Section III-F discusses plasma buildup and radial transport simulations.

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III-B. Plasma Analysis

This section presents the plasma physics model which supplies the reference case parameters given in Sec. III-D. The model follows the general outline of an analysis presented in Ref. 1. Now, however, the barrier physics is treated in detail and provision is made for hot, mirror-trapped electrons in the barrier, if desired. A zero-dimensional treatment was chosen for this analysis since, while capable of relatively quick solution, it contains most of the salient features of a tandem mirror reactor plasma.

All formulas are written in the CGS system except energies which are in keV.

Figure III-B-1 shows the approximate axial profiles used for magnetic field, potential, electron density, and ion density for one end of the machine. Flat radial profiles have been assumed. Magnetic field and potential labels are conventional. ϕ_e confines electrons; ϕ_c confines ions; and ϕ_b creates the thermal barrier between central cell and plug. Density labels will be explained later.

Total fusion power and central cell length are chosen which, assuming two 3.5 MeV alpha particle gyroradii between plasma and wall, define the central cell plasma radius and neutron wall loading. The plasma radius at a given $B(z)$ follows from flux conservation:

$$r(z) = r_c \left[\frac{B_c}{B(z)} \left(\frac{1 - \beta_c}{1 - \beta(z)} \right)^{1/2} \right]^{1/2} \quad (1)$$

where $\beta(z) \equiv (\text{plasma pressure})/(\text{magnetic field pressure})$ is given by MHD stability considerations, and r_c and β_c are the central cell plasma radius and beta. Because the plug is assumed to have a minimum-B configuration, the plug volume, V_p , is approximated by a sphere of radius r_p at B_p .

The densities in the central cell and plug are related to the limits on beta through

$$n_c = \frac{2.5 \times 10^7 \beta_c B_c^2}{(T_{ic} + T_{ec} + T_\alpha)} \quad (2)$$

and

$$n_p = \frac{2.5 \times 10^7 \beta_p B_p^2}{(0.9 E_p + T_{ep})} \quad (3)$$

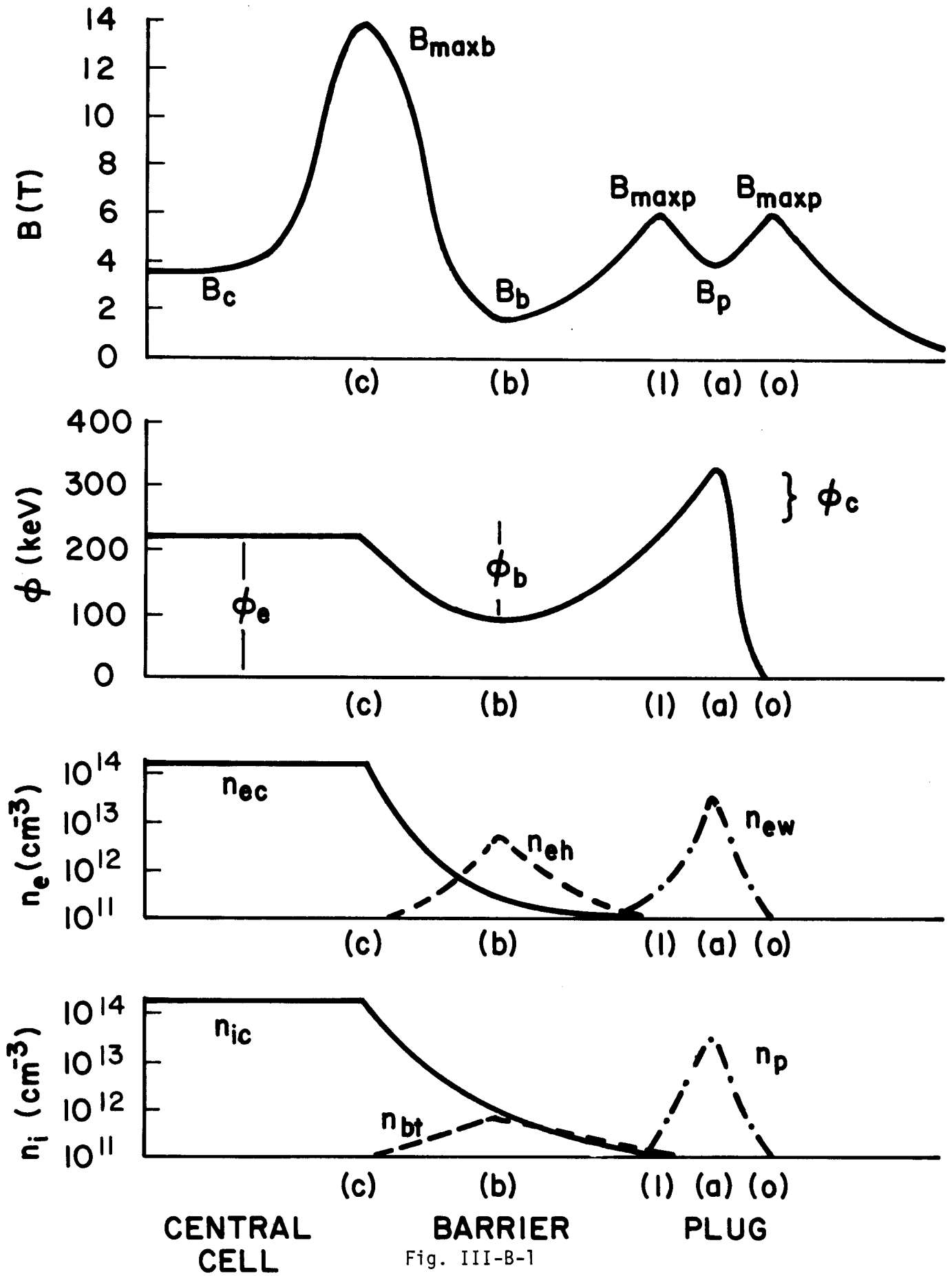


Fig. III-B-1

where the average perpendicular alpha energy is

$$T_{\alpha} = 8.3 \times 10^{10} \langle \sigma v \rangle_{DT} E_{\alpha} f_{e\alpha} T_{ec}^{1.5} ,$$

$\langle \sigma v \rangle_{DT}$ is the fusion reaction rate, E_{α} is the alpha creation energy, $f_{e\alpha}$ is the fraction of alpha energy deposited into electrons, T_{ec} is the central cell electron temperature, T_{ic} is the central cell ion temperature, T_{ep} is the plug electron temperature, E_p is the plug ion energy, and the factor of 0.9 appears in n_p because neutral beam injection causes most of the plug energy to be perpendicular.⁽⁵⁾

The barrier, central cell, and plug of a tandem mirror reactor will be dealt with in turn. The direct convertor does not affect the physics of the other regions and will not be treated here.

Most of the features incorporated into the model since Ref. 1 relate to barrier physics, so we begin there. A dip in the barrier ion density, n_b , is expected to give a dip, ϕ_b , in the potential, thus thermally insulating plug and central cell electrons. To create this density dip, passing central cell ions which collisionally scatter into trapped orbits must be pumped out of the barrier region. Three mechanisms are considered here:

1) Neutral beam particles injected in the barrier loss cone become central cell ions when they charge exchange. The resulting neutrals tend to have fairly large perpendicular velocities and should quickly leave the machine; thus, reionization of these particles is neglected. For a trapped barrier ion with parallel energy, $f_{phib}\phi_b$, the injection energy necessary to overcome the potential and ∇B forces and create a passing ion for injection at B_{inb} is

$$E_{inb} = \frac{f_{phib}\phi_b}{1 - \frac{B_{maxb}}{B_{inb}} \sin^2 \theta_{inb}} \quad (4)$$

where a reasonable choice of injection angle is

$$\theta_{inb} = \frac{\theta_{loss \text{ cone}}}{2} = 1/2 \arcsin R^{-1/2} . \quad (5)$$

Charge exchange of passing ions is neglected since the resulting ion can still pump the barrier. Since each charge exchange, which does the pumping, is accompanied by $\langle \sigma v \rangle_{ionb} / \langle \sigma v \rangle_{cxb}$ ionization events, the total energy change per

pumped ion is

$$E_{\text{inb}} = \frac{3}{2} T_{\text{ic}} + \frac{\langle \sigma v \rangle_{\text{ionb}}}{\langle \sigma v \rangle_{\text{cxnb}}} (E_{\text{inb}} - \phi_{\text{inb}}) \quad (6)$$

If the electron from an ionization event enters the central cell, it will add energy $\phi_{\text{inb}} = f_{\text{phinb}} \phi_b$ to the central cell electrons. If the electron enters the plug, it will add energy $\phi_{\text{inb}} + \phi_c$ (presuming $\phi_{\text{inb}} < \phi_b$), but a plug electron must then become a passing electron in order to maintain quasi-neutrality, carrying an energy $\phi_b + \phi_c + T_{\text{ep}}$ from the plug and adding $\phi_b + T_{\text{ep}}$ to the central cell.

2) Passive pumping consists of using a magnetic field configuration in the barrier region which does not confine particles, as in a barrier where the magnetic fields at the ends are parallel ellipses.⁽²⁾ High energy ions are preferentially pumped, so an ion would be ejected with an energy of approximately $\phi_b + T_{\text{ic}}$. A pumped electron is removed from the central cell with average energy of $1.5 T_{\text{ec}}$.

3) Radio frequency (RF) pumping may be possible, although investigation of this possibility has not progressed far. To model it in this study, RF is assumed to add a parallel energy of ϕ_b to a trapped barrier ion, thus pushing the average trapped ion over the loss boundary into the central cell.

To achieve a high barrier potential, ϕ_b , it is important to keep the total barrier density to a minimum. The lower limit on n_b is the density of central cell ions passing through the barrier, with flux conservation included:

$$n_{\text{pass},i}(z) = n_c \frac{B(z)}{B_{\text{maxb}}} \left[\frac{T_{\text{ic}}}{\pi \phi_b(z) + T_{\text{ic}}} \right]^{1/2} \quad (7)$$

The total barrier density is normalized to $n_{\text{pass},i}$ through the relation

$$n_b = g_b n_{\text{pass},i} \quad (8)$$

The calculation of g_b is difficult, and a reasonable value of $g_b = 2$ is generally used in this report.

The current per unit volume of passing ions which trap in the barrier at z is given by a formula fitted to Fokker-Planck studies:⁽³⁾

$$J_{\text{trap}}(z) = \frac{n_{\text{pass},i}(z)}{5.5 \times 10^9 T_{ic}^{3/2}} [1 + 0.185 \frac{B_{\text{maxb}}}{B(z)}] \quad (9)$$

Average B and ϕ values for the barrier are found assuming parabolic profiles,

$$\langle B \rangle_b = \frac{2}{3} B_b + \frac{1}{6} (B_{\text{maxb}} + B_{\text{maxp}}) \quad (10)$$

$$\langle \phi \rangle_b = \frac{2}{3} \phi_b \quad (11)$$

then these are inserted into J_{trap} and also used to find the barrier volume $V_b = \pi \langle r \rangle_b^2 L_b$. The total current of ions scattering into the barrier is then

$$I_{\text{trap}} \approx \frac{2V_b n_c^2}{5.5 \times 10^9 T_{ic}^{1/2} B_{\text{maxb}}^2} \left[\frac{\langle B \rangle_b^2 + 0.185 B_{\text{maxb}} \langle B \rangle_b}{\pi \langle \phi \rangle_b + T_{ic}} \right] \quad (12)$$

This approach agrees well with the values from Ref. 4, Table 1, using the Ref. 4 value of 0.55 instead of the more accurate 0.185 in J_{trap} . Total currents tend to be a factor of one to two higher than the Ref. 4 currents, in harmony with the philosophy used here of being slightly pessimistic when estimating uncertain effects.

The calculation of the potential ϕ_c , which confines central cell ions, has been examined in Ref. 5. Passing electrons scattering into the plug and electrons from neutral beam ionization balance the boiling out of electrons over the potential $\phi_b + \phi_c$ to give

$$\frac{n_{ew}^2(a)}{(n\tau)_{ep}} \left[1 - \frac{n_{ec}(b)}{n_{ew}(a)} \left(\frac{T_{ep}}{T_{ec}} \right)^{\nu_c} \exp \left(\frac{\phi_b + \phi_c}{T_{ep}} \right) \right] = \frac{n_p^2}{(n\tau)_{ip}} \quad (13)$$

where $n_p^2/(n\tau)_{ip}$ is the plug electron ionization source (equal to the plug ion fueling rate),

$$(n\tau)_{ep} = \frac{8.2 \times 10^9}{\Lambda_{ep}} \frac{G(\frac{B_{maxp}}{B_p})}{I(\frac{\tau_{ep}}{\phi_b + \phi_c})} \tau_{ep}^{3/2} \exp(\frac{\phi_b + \phi_c}{\tau_{ep}}) , \quad (14)$$

Λ_{ep} is the Coulomb logarithm,

$$G(x) \equiv \frac{\pi^{1/2}}{2} (1 + \frac{1}{x})^{1/2} \ln \left[\frac{(1 + \frac{1}{x})^{1/2} + 1}{(1 + \frac{1}{x})^{1/2} - 1} \right] \quad (15)$$

and

$$I(x) \equiv \frac{1 + \frac{x}{2}}{1 + \frac{x^2}{4}} . \quad (16)$$

The parameter v_c varies from 0.5 to 1.5; the lower value applies when the magnetic field gradient helps confine particles as it does in the present case, and the higher range applies when the gradient is pushing particles out of the plug. $v_c = 0$ corresponds to a Boltzmann relation, and previous studies, including Ref. 1, used this case.

Unfortunately, $\phi_c = 0.5$ requires, as can be seen in Eq. 13, a higher value of $n_{ew}(a)$ for a given $n_{ec}(b)$ than $v_c = 0$ does, and plug power varies as $n_{ew}^2(a)$. This motivates reducing $n_{ec}(b)$ by creating a hot, mirror-trapped electron population, n_{eh} , since $n_b = n_{ec}(b) + n_{eh}(b)$. The analysis given here is similar to that of Ref. 6, with suitable modifications to treat differences in magnetic field configuration.

Figure III-B-2 shows velocity space for electrons at (b), the midplane of the barrier. The electrons scattering into the mirror-trapped region have a perpendicular energy of

$$E'_b = \frac{\phi_b}{\frac{B_{maxb}}{B_b} - 1} . \quad (17)$$

The trapping current, assuming $n_{eh}(b) \gg n_{ec}(b)$, is⁽⁶⁾

$$J_{cb} \approx \frac{n_{ec}(b)n_b}{2.4 \times 10^8 \tau_{ec} E_{eh}^{1/2} \exp(E'_b/\tau_{ec})} . \quad (18)$$

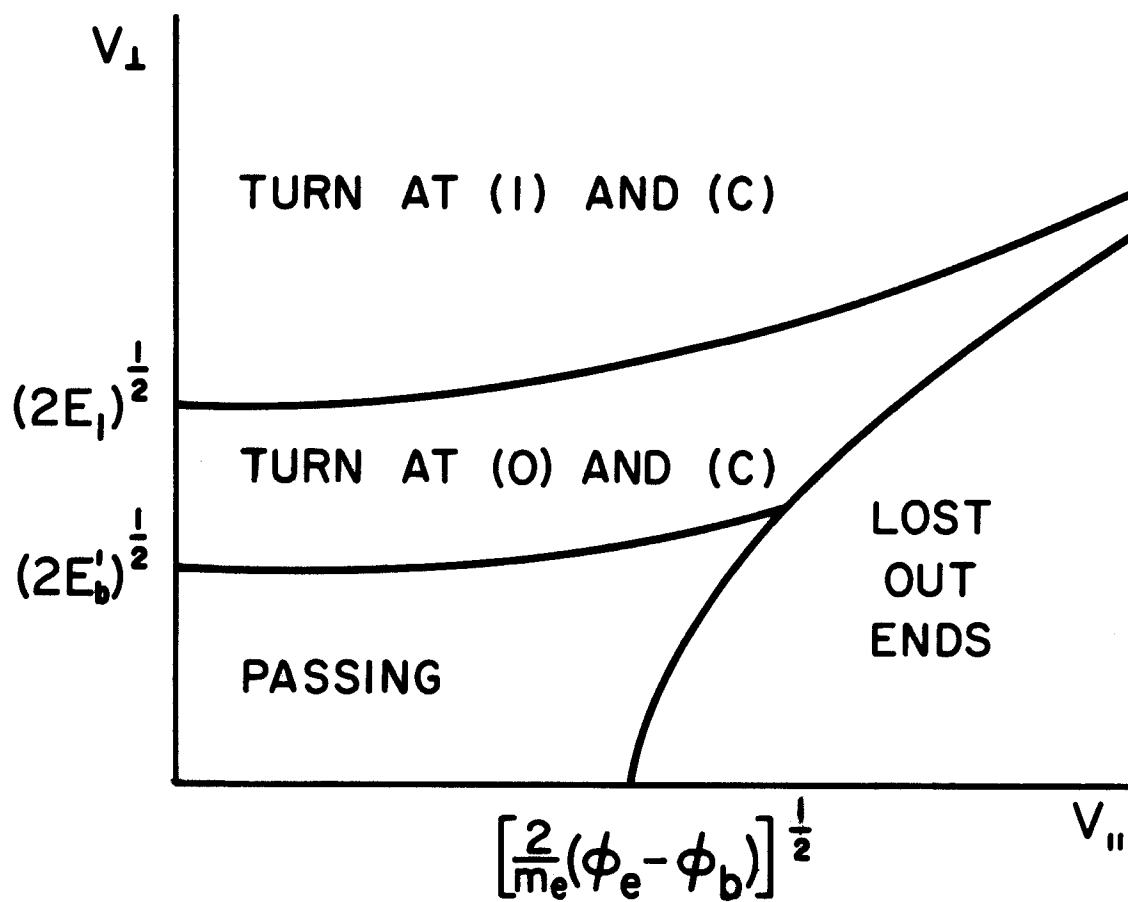


Fig. III-B-2

The loss rate of trapped barrier electrons follows a form similar to the Logan-Rensink plug model:⁽⁷⁾

$$J_{\text{loss}} \approx \frac{n_{\text{eh}}(b)n_b}{2.4 \times 10^8 E_{\text{eh}}^{3/2} \log_{10} R_{\text{beff}}} \quad (19)$$

where

$$R_{\text{beff}} = \frac{B_{\text{maxp}}/B_b}{1 + \frac{\phi_b}{E_{\text{eh}}}} \quad (20)$$

Defining $F_{\text{ec}} \equiv n_{\text{ec}}(b)/n_b$ and using particle balance, $J_{\text{cb}} = J_{\text{loss}}$, gives

$$F_{\text{ec}} = \frac{T_{\text{ec}}}{T_{\text{ec}} + E_{\text{eh}} \exp(-E_b'/T_{\text{ec}}) \log_{10} R_{\text{beff}}} \quad (21)$$

To keep the barrier electron distribution from having a hole at low energy, roughly

$$\frac{n_{\text{ec}}(b)}{n_b} \equiv F_{\text{ec}} \gtrsim \frac{E_b'}{E_{\text{eh}}} \quad (22)$$

To avoid a valley between E_b' and E_1 , defining $n_{\text{eh}}(ab)$ as the fraction of $n_{\text{eh}}(b)$ which can get to (a), roughly

$$\frac{n_{\text{eh}}(ab)}{n_b} \gtrsim \frac{E_1 - E_b'}{E_{\text{eh}}} \quad (23)$$

taking $E_1 \equiv \phi_b/(B_{\text{maxp}}/B_b - 1)$ as the boundary between particles which turn at point (1) and those which reach point (0). Mapping $n_{\text{eh}}(ab)$ to (a) and assuming $n_{\text{eh}}(ab) = 0$ if $B_{\text{maxp}} = B_{\text{maxb}}$ gives

$$n_{\text{eh}}(a) \gtrsim \left(\frac{E_1 - E_b'}{E_{\text{eh}}}\right) \left(\frac{B_{\text{maxb}} - B_{\text{maxp}}}{B_{\text{maxb}} - B_b}\right) \left(\frac{0.2 E_{\text{eh}}}{\phi_b + \phi_c + 0.2 E_{\text{eh}}}\right)^{1/2} n_b \quad (24)$$

where it has been assumed that 20% of the hot barrier electron energy is in the parallel direction. Then, since $n_{\text{ew}}(a) = n_p - n_{\text{eh}}(a)$,

$$\frac{n_{ew}(a)}{n_{ec}(b)} \approx \frac{n_p}{F_{ec} n_b} - \left(\frac{1 - F_{ec}}{F_{ec}} \right) \left(\frac{E_1 - E'_b}{E_{eh}} \right) \left(\frac{B_{maxb} - B_{maxp}}{B_{maxb} - B_b} \right) \left(\frac{0.2 E_{eh}}{\phi_b + \phi_c + 0.2 E_{eh}} \right)^{1/2} \quad (25)$$

Since $n_{ec}(b)$ should go to zero as ϕ_b approaches ϕ_e , we take

$$n_{ec}(\phi) \approx n_c \left[\frac{\exp\left(\frac{\phi_e - \phi}{T_{ec}}\right) - 1}{\exp\left(\frac{\phi_e}{T_{ec}}\right) - 1} \right] \quad (26)$$

which gives

$$\phi_b = T_{ec} \ln \left\{ \frac{n_c}{n_c \exp(-\phi_e/T_{ec}) + F_{ec} n_b [1 - \exp(-\phi_e/T_{ec})]} \right\} \quad (27)$$

Electrons enter the barrier with energy E'_b and are assumed to take out an average energy of $E_{eh}/2$, which gives

$$P_{eaux,b} \approx \frac{n_{eh}(b) n_b 2V_b}{2.4 \times 10^8 E_{eh}^{3/2} \log_{10} R_{beff}} \left(\frac{E_{eh}}{2} - E'_b \right) + P_{synch,b} \quad (28)$$

where⁽⁸⁾

$$P_{synch,b} \approx 2.6 \times 10^{-7} \left[\frac{(1 - F_{ec}) n_b}{r_b} \right]^{1/2} (E_{eh} \langle B \rangle_b)^{5/2} (1 - R_{eflec})^{1/2} V_b \quad (29)$$

where R_{eflec} is the barrier reflection coefficient. For a given maximum barrier beta, β_b ,

$$E_{eh} \lesssim \frac{2.5 \times 10^7 \beta_b B_b^2}{0.8(1 - F_{ec}) n_b} \quad (30)$$

III-B-1. Central Cell

For ions in the central cell, $(n\tau)_{ic}$ is given by Pastukhov's analysis as corrected by Cohen, et al.⁽⁵⁾

$$(n\tau)_{ic} = \frac{1}{Z_{ic}(2m_i)^{1/2} \pi e^4 \Lambda_i} \frac{G(Z_{ic} R_c)}{I(\frac{T_{ic}}{\phi_c})} T_{ic}^{1/2} \phi_c e^{\phi_c/T_{ic}} \quad (31)$$

where m_i is the ion mass, e is the electronic charge, Λ_i is the Coulomb logarithm, $Z_{ic} = 0.5$ for ions because they scatter only slightly off of electrons, and $R_c = B_{maxb}/B_c$ is the central cell mirror ratio.

For electrons in the central cell, similar analysis gives

$$(n\tau)_{ec} = \frac{1}{Z_{ec}(2m_e)^{1/2} \pi e^4 \Lambda_e} \frac{G(Z_{ec} R_c)}{I(\frac{T_{ec}}{\phi_e})} T_{ec}^{1/2} \phi_e e^{\phi_e/T_{ec}} \quad (32)$$

where m_e is the electron mass and $Z_{ec} = 1$ for electrons because they scatter off both ions and electrons.

The potential ϕ_e , which confines passing electrons, follows from particle balance:

$$\begin{aligned} (1 + f_{is}) \frac{n_c^2}{(n\tau)_{ic}} V_c + \frac{n_c^2}{(n\tau)_{xfi}} V_c + \frac{n_c^2}{2} \langle \sigma v \rangle_{DT} V_c + \frac{n_p^2}{(n\tau)_{ip}} 2V_p \\ = \frac{n_c^2}{(n\tau)_{ec}} V_c + \frac{n_c^2}{(n\tau)_{xfe}} V_c \end{aligned} \quad (33)$$

where f_{is} is the ratio of auxiliary cold electron current to ion ionization current in the central cell, if present; V_c , V_b , and V_p are the central cell, barrier and plug volumes; $(n\tau)_{xfi}$ and $(n\tau)_{xfe}$ are ion and electron $(n\tau)$'s for central cell cross field transport.

The collisional energy transfer rate from species β to species α is approximated by assuming both species are Maxwellian and using standard formulas:⁽⁹⁾

$$\nu^{\alpha/\beta} = 5.7 \times 10^{-24} \frac{(m_{\alpha} m_{\beta})^{1/2} Z_{\alpha}^2 Z_{\beta}^2 n_{\beta} \Lambda_{\alpha\beta}}{(m_{\alpha} T_{\beta} + m_{\beta} T_{\alpha})^{1.5}} . \quad (34)$$

The assumption of Maxwellians is inaccurate for streaming components, but collisional terms tend to be small, and no serious consequences of this approximation are anticipated. The $\nu^{\alpha/\beta}$'s also incorporate volume ratios when necessary to include the effect of the energy given to passing electrons being spread over the whole central cell.

We define f_{NB} as the fraction of the pumping done by neutral beams, f_{RF} as the fraction done by RF, and f_K as the fraction done by passive pumping (Kesner's suggestion). Ionization of barrier neutral beams is considered to give a fraction f_c of electrons to the central cell and fraction f_p to the plug. Note that $f_{NB} + f_{RF} + f_K = 1$ and $f_c + f_p = 1$ are constraints; the pumping fraction relation is extended in an obvious way when more than one neutral beam or RF mechanism is used.

The main contributor to central cell power is the collisional slowing of fusion alpha particles on ions and electrons which is chiefly balanced by end loss. The central cell electron power balance is given by

$$\begin{aligned} & \frac{n_c^2}{4} \langle \sigma v \rangle_{DT} f_{e\alpha} E_{\alpha} \left(1 + \frac{E_r}{E_{fus}}\right) + f_{is} \frac{n_c^2}{(n\tau)_{ic}} \phi_e + \frac{3}{2} n_c \nu^{ec/ic} (T_{ic} - T_{ec}) \\ & + n_c \nu^{ec/ip} \left(E_p - \frac{3}{2} T_{ec}\right) + 3 \frac{n_{ec}^{(b)} n_{ew}^{(a)}}{(n\tau)_{ep}} \frac{V_p}{V_c} \left(\frac{T_{ep}}{T_{ec}}\right)^{\nu_c} (T_{ep} - T_{ec}) \exp\left(-\frac{\phi_b + \phi_c}{T_{ep}}\right) \\ & + f_{NB} J_{trap} \frac{2V_b}{V_c} + [f_c f_{phib} \phi_b + f_p \frac{\langle \sigma v \rangle_{ionb}}{\langle \sigma v \rangle_{cxb}} (\phi_b + T_{ep})] + \frac{n_p^2}{(n\tau)_{ip}} \frac{2V_p}{V_c} (\sigma_b + T_{ep}) \\ & = 3f_K J_{trap} \frac{V_b}{V_c} T_{ec} + \left[\frac{n_c^2}{(n\tau)_{ec}} + \frac{n_c^2}{(n\tau)_{xfe}}\right] (\phi_e + T_{ec}) + P_{synch,c} + P_{brem,c} \\ & + J_{bar} \frac{2V_b}{V_c} E'_b \end{aligned} \quad (35)$$

where $f_{e\alpha}$ is the fraction of alpha particle energy, E_α , which goes to electrons,⁽³⁾

$$f_{e\alpha} = 0.882 \exp \left(\frac{-T_{ec}}{67.4} \right) ; \quad (36)$$

E_{fus} is the energy output per fusion; E_r is the average reacting ion energy, taken to be⁽³⁾

$$E_r = 45 + \frac{3}{2} T_{ic} ; \quad (37)$$

$P_{synch,c}$ is synchrotron radiation power,⁽⁸⁾

$$P_{synch,c} = 2.6 \times 10^7 \left(\frac{n_c}{r_c} \right)^{1/2} (B_c T_{ec})^{5/2} (1 - R_{eflec})^{1/2} V_c ; \quad (38)$$

$P_{brem,c}$ is bremsstrahlung radiation power,⁽⁹⁾

$$P_{brem,c} = 3.33 \times 10^{-15} n_c^2 T_{ec}^{1/2} V_c ; \quad (39)$$

and $J_{bar} = J_{loss}$ is the rate at which electrons become mirror-trapped in the barrier. The first term is power from fusions; the second comes from a cold electron current, if present; the third and fourth from collisional energy transfer; the fifth and seventh from conduction and convection of energy between the plug and central cell⁽⁶⁾; the sixth from ionization of barrier pumping neutral beams; the eighth from passive pumping; the ninth from end loss and cross-field transport; the tenth from synchrotron radiation; the eleventh from bremsstrahlung; and the last from trapping of central cell electrons into the barrier.

The central cell ion power balance is given by

$$\begin{aligned}
& \frac{n_c^2}{4} \langle \sigma v \rangle_{DT} \left[f_{i\alpha} E_\alpha \left(1 + \frac{E_r}{E_{fus}} \right) - E_r \right] + f_{RF} J_{trap} \frac{2V_b}{V_c} \phi_b \\
& + f_{NB} J_{trap} \frac{2V_b}{V_c} \left[E_{inb} - \frac{3}{2} T_{ic} + \frac{\langle \sigma v \rangle_{ionb}}{\langle \sigma v \rangle_{cxb}} (E_{inb} - \phi_{inb}) \right] \\
& = \left[\frac{n_c^2}{(n\tau)_{ic}} + \frac{n_c^2}{(n\tau)_{xfi}} \right] (\phi_c + T_{ic}) + f_K J_{trap} \frac{2V_b}{V_c} (\phi_b + T_{ic}) \\
& + \frac{3}{2} n_c v^{ic/ec} (T_{ic} - T_{ec}) + \frac{3}{2} \left[\frac{n_c^2}{(n\tau)_{ic}} + \frac{n_c^2}{(n\tau)_{xfi}} + \frac{n_c^2}{2} \langle \sigma v \rangle + f_K J_{trap} \frac{2V_b}{V_c} \right. \\
& \left. - f_{NB} J_{trap} \frac{2V_b}{V_c} \frac{\langle \sigma v \rangle_{ionb}}{\langle \sigma v \rangle_{cxb}} \right] \frac{\langle \sigma v \rangle_{cxc}}{\langle \sigma v \rangle_{ionc}} T_{ic}
\end{aligned} \tag{40}$$

where⁽³⁾ $f_{i\alpha} = 1 - 0.908 \exp \left(\frac{-T_{ec}}{101.7} \right)$ (41)

is the fraction of alpha particle energy deposited in ions; $\langle \sigma v \rangle_{cxc}$ is the central cell charge exchange rate; and $\langle \sigma v \rangle_{ionc}$ is the central cell ionization rate. The first term is power from fusions, the second arises if RF barrier pumping is used; the third comes from neutral beam barrier pumping; the fourth is end loss and cross-field transport; the fifth comes from passive pumping, if used; the sixth is collisional energy transfer; and the last term arises from charge exchange of fueling ions, if present. This study assumes that fueling will be done by pellets and that the last term is therefore zero.

III-B-2. Plug

The Logan-Rensink model is used for $(n\tau)_{ip}$ of ions in the plug:⁽⁷⁾

$$(n\tau)_{ip} = \left[\frac{\Lambda_{iip} (m_D/m_{ip})^{1/2}}{3.9 \times 10^{12} E_{inj}^{1.5} \log_{10} R_{eff}} + \frac{\Lambda_{eip} (m_H/m_{ip})}{10^{13} T_{ep}^{1.5} \ln(E_{inj}/E_{out})} \right]^{-1} \tag{42}$$

where m_D is the mass of deuterium, m_H is the mass of hydrogen, m_{ip} is the plug ion mass, Λ_{iip} and Λ_{eip} are Coulomb logarithms, E_{inj} is the neutral beam injection energy, E_{out} is given by

$$E_{out} = \frac{E_{inj}}{1 + \frac{\tau_i}{\tau_{DR}}} + \frac{(\phi_c + \phi_e)}{\left[\frac{R_p \sin^2 \theta}{(1 - \beta_p)^{1/2}} - 1 \right] \left(1 + \frac{\tau_i}{\tau_{DR}} \right)} \quad (43)$$

where R_p is the plug mirror ratio; θ is the beam injection angle, taken here as $\pi/2$; β_p is the plug beta; and τ_i/τ_{DR} , the ratio of ion collision time to electron drag time, is given by

$$\frac{\tau_i}{\tau_{DR}} = .11 \left(\frac{E_{inj}}{T_{ep}} \right)^{1.5} \frac{\Lambda_{eip}}{\Lambda_{iip}} \frac{\log_{10} R_{eff}}{\ln \left(\frac{E_{inj}}{E_{out}} \right)} \left(\frac{m_D}{m_{ip}} \right)^{1/2} \quad (44)$$

where T_{ep} is the plug electron temperature and

$$R_{eff} = \frac{R_p / (1 - \beta_p)^{1/2}}{1 + \frac{(\phi_e + \phi_c)}{E_{inj}}} \sin^2 \theta \quad (45)$$

The plug electron power balance is given by

$$\begin{aligned} P_{eaux} + \frac{3}{2} n_{ew}^{(a)} v_{ep/ip} (T_{ip} - T_{ep}) + f_p f_{NB} J_{trap} \frac{\langle \sigma v \rangle_{ionb}}{\langle \sigma v \rangle_{cxb}} \frac{V_b}{V_p} (f_{phib} \phi_b + \phi_c) \\ = \frac{3}{2} \frac{n_{ec}^{(b)} n_{ew}^{(a)}}{(n\tau)_{ep}} \left(\frac{T_{ep}}{T_{ec}} \right)^{1/2} e^{\frac{\phi_b + \phi_c}{T_{ep}}} (T_{ep} - T_{ec}) + P_{synch,p} \\ + \left[\frac{n_p^2}{(n\tau)_{ip}} + f_p f_{NB} J_{trap} \frac{\langle \sigma v \rangle_{ionb}}{\langle \sigma v \rangle_{cxb}} \frac{V_b}{V_p} \right] (\phi_b + \phi_c + T_{ep}) \end{aligned} \quad (46)$$

where P_{eaux} is the amount of auxiliary heating of plug electrons;

$$P_{synch,p} = 2.6 \times 10^{-7} \left[\frac{n_{ew}^{(a)}}{r_p} \right]^{1/2} (B_p T_{ep})^{5/2} (1 - R_{eflec})^{1/2} V_p \quad (47)$$

is the plug synchrotron radiation loss;⁽⁸⁾ the second term is collisional energy transfer; the third comes from ionization of barrier pumping neutral beams; and the fourth and sixth are energy conduction and convection between plug and central cell electrons.⁽⁶⁾

The plug ion power balance is given by

$$\begin{aligned} \frac{n_p^2}{(n\tau)_{ip}} (E_{inj} - E_{out}) &= \frac{n_p^2}{(n\tau)_{ip}} \frac{\langle\sigma v\rangle_{cxp}}{\langle\sigma v\rangle_{ionp}} (E_p - E_{inj}) + n_p v^{ip/ep} (E_p - \frac{3}{2} T_{ep}) \\ &+ n_p v^{ip/ec} (E_p - \frac{3}{2} T_{ec}) \end{aligned} \quad (48)$$

where $\langle\sigma v\rangle_{cxp}$ and $\langle\sigma v\rangle_{ionp}$ are the plug charge exchange and ionization rates. The first term is the power deposited by neutral beams; the second represents energy lost to charge exchange; and the third and fourth terms give collisional energy transfer.

III-B-3. Method of Solution

The power balance equations are written as rate equations for the various dT/dt 's by subtracting loss terms from gain terms. Given a set of initial conditions, the rate equations are then time-stepped until equilibrium is reached. The method is based on that of Shaing et al.⁽¹⁰⁾ Note that, since equilibrium equations are solved, solutions are technically valid only near equilibrium, and time serves only as an iteration variable.

Magnetic field profile, β 's, fusion power P_{fus} , central cell length, injection energy, and pumping parameter g_b are held fixed, while densities, radii, neutron wall loading, potentials, T_{ep} , T_{ec} , and E_p are iterated. For a given set of parameters, T_{ic} is held constant. This gives ϕ_c through the condition $dT_{ic}/dt = 0$; which in turn gives T_{ep} from Eq. 13. P_{eaux} is then chosen such that $dT_{ep}/dt = 0$ unless this causes $P_{eaux} < 0$ in which case T_{ep} is allowed to vary.

Generally then, only for T_{ec} and E_p are rate equations solved by the iterative procedure. These are solved in a loop along with the $(n\tau)$'s; then the n 's, r 's, ϕ 's, T_{ep} and P_{eaux} are updated and the procedure is repeated. Solution time is generally a few seconds on the MFECC CDC 7600 machine.

Input guesses for the ϕ 's and T's are required, but the final results are only mildly sensitive to these values.

The ratio of fusion power to input power is then given by

$$Q = \frac{P_{fus} V_c}{(P_{NB,p} + P_{eaux,p}) 2V_p + (P_{NB,b} + P_{eaux,b} + P_{RF}) 2V_b} \quad (49)$$

where $P_{eaux,p}$ is given by plug electron power balance, $P_{eaux,b}$ is given by Eq. 26,

$$P_{NB,p} = \frac{n_p^2}{(n\tau)_{ip}} E_{inj} \quad (50)$$

$$P_{NB,b} = f_{NB} J_{trap} \left(1 + \frac{\langle\sigma v\rangle_{ionb}}{\langle\sigma v\rangle_{cxb}}\right) E_{inb} \quad (51)$$

and
$$P_{RF} = f_{RF} J_{trap} \phi_b \quad (52)$$

References for Section III-B

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III-C. Parametric Analysis

The reference case for the parametric analysis appears in Tables III-C-1 and III-C-2. Generally, when one parameter varies, only a few others change significantly; these changes will be indicated as necessary.

There usually exists a maximum ratio of $n_{ec}(b)/n_{ew}(a)$ above which an adequate ϕ_c cannot be found, as may be seen from Eq. 13 (Section III-B). When varying a parameter, this sometimes limits one end of the parameter's range.

The four parameters P_{fus} , r_w , L_c , and r_c are related through

$$P_{fus} = 4 \times 10^{-23} n_c^2 \langle \sigma v \rangle_{DT} E_{fus} \pi r_c^2 L_c$$

and

$$r_w = \frac{0.8 P_{fus}}{2\pi(r_c + 2\rho_\alpha)L_c}$$

where $E_{fus} = 1.76 \times 10^4$ keV is the total fusion energy per reaction and two alpha particle radii, ρ_α , are allowed between the plasma and the reactor first wall. These show that r_w is almost linearly proportional to r_c , which behavior is shown in Fig. III-C-1 where L_c is kept constant and P_{fus} varies. When P_{fus} is constant, Q rises linearly with L_c as shown in Fig. III-C-2. This occurs because r_c decreases to adjust to the constant P_{fus} and therefore approximately constant central cell volume, but the consequent drop in barrier and plug radii gives lower volumes there. Thus, input power is lower, so Q is higher. Although $2 \rho_\alpha$ were used between the plasma and wall for the parametric studies, the WITAMIR-I reference case allowed $2.39 \rho_\alpha$ in order to get a neutron wall loading of 2.4 MW/m^2 and a wall radius of 0.97 m, for consistency with the engineering design work.

The dependence of Q and P_{eaux} , the plug RF power, on the plug neutral beam injection energy, E_{inj} , is given in Fig. III-C-3. Barrier power is not significantly affected by E_{inj} . There is a knee in the dependence at 300 to 500 keV, where E_{inj} begins to dominate $\phi_c + \phi_e$ in R_{eff} , the effective plug mirror ratio (cf. Eq. 45, Section III-B). R_{eff} is thus approaching a plateau where the plug most efficiently traps injected particles, and Fig. III-C-3 reflects this.

As the pumping process becomes more efficient and g_b drops, Q rises until the power needed for neutral beam pumping of the barrier overcomes the power

Table III-C-1Model Reactor Power and Machine Parameters

Powers are given as absorbed plasma power without efficiencies folded in

<u>Parameter</u>	<u>Value</u>
Q	28.0
Fusion power	3000 MW
Neutron wall loading	2.4 MW/m ²
Central cell power density	11.3 MW/m ³
Plug ECRF power	16.5 MW
Plug neutral beam power at 500 keV injection	2.4 MW
Plug trapping fraction	0.13
Barrier ECRF power	33.3 MW
Barrier neutral beam power at 9.6 keV injection	12.7 MW
at 190 keV injection	42.5 MW
Central cell surface heat load	2.75 W/cm ²
Plug surface heat load	3.43 W/cm ²
Barrier surface heat load	50.0 W/cm ²
Central cell wall radius	0.97 m
Central cell length	165 m
Barrier length	10. m
Plug length	5.5 m
Central cell magnetic field	3.6 T
Barrier maximum field	14. T
Barrier minimum field	1.4 T
Plug maximum field	6.0 T
Plug minimum field	4.0 T

Table III-C-2Model Reactor Plasma ParametersCentral Cell

Density	$1.51 \times 10^{14} \text{ cm}^{-3}$
Ion temperature	32.5 keV
Electron temperature	32.8 keV
Potential, ϕ_C	102. keV
Beta, β_C	0.40
Plasma radius	0.72 m
$(n\tau)_{ic}$	$7.8 \times 10^{14} \text{ sec cm}^{-3}$

Barrier

Density average	$6.9 \times 10^{12} \text{ cm}^{-3}$
Mean hot electron energy, E_{eH}	270. keV
Passing electron fraction, F_{ec}	0.27
Pumping parameter, g_b	2.0
Pumping fraction at low energy	0.95
Pumping fraction at high energy	0.05
Potential, ϕ_b	141. keV
Beta, β_b	0.235
Plasma radius average	0.59 m

Plug

Density average	$2.7 \times 10^{13} \text{ cm}^{-3}$
Mean ion energy	905. keV
Electron tempeature	123. keV
Potential, $\phi_C + \phi_e$	326. keV
Cohen parameter, v_C	0.5
Beta, β_p	0.64
Plasma radius	0.77 m
$(n\tau)_{ip}$	$9.8 \times 10^{13} \text{ sec cm}^{-3}$

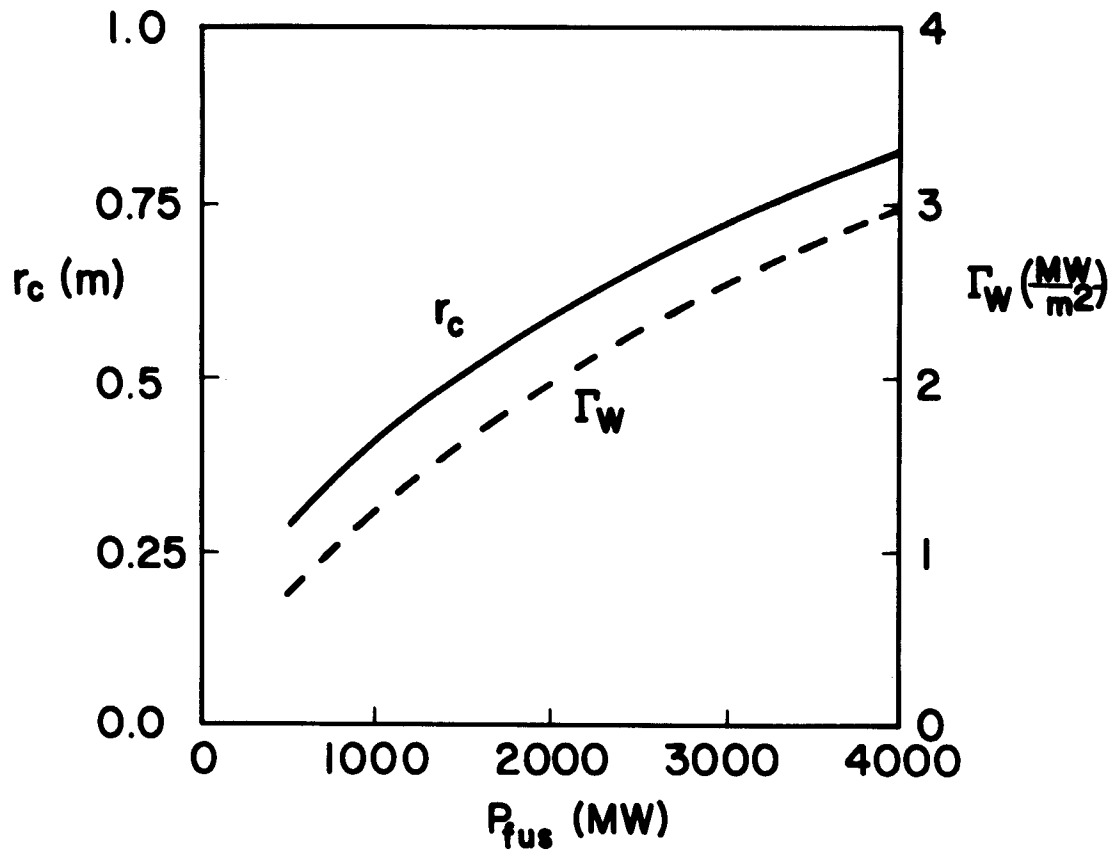


Fig. III-C-1

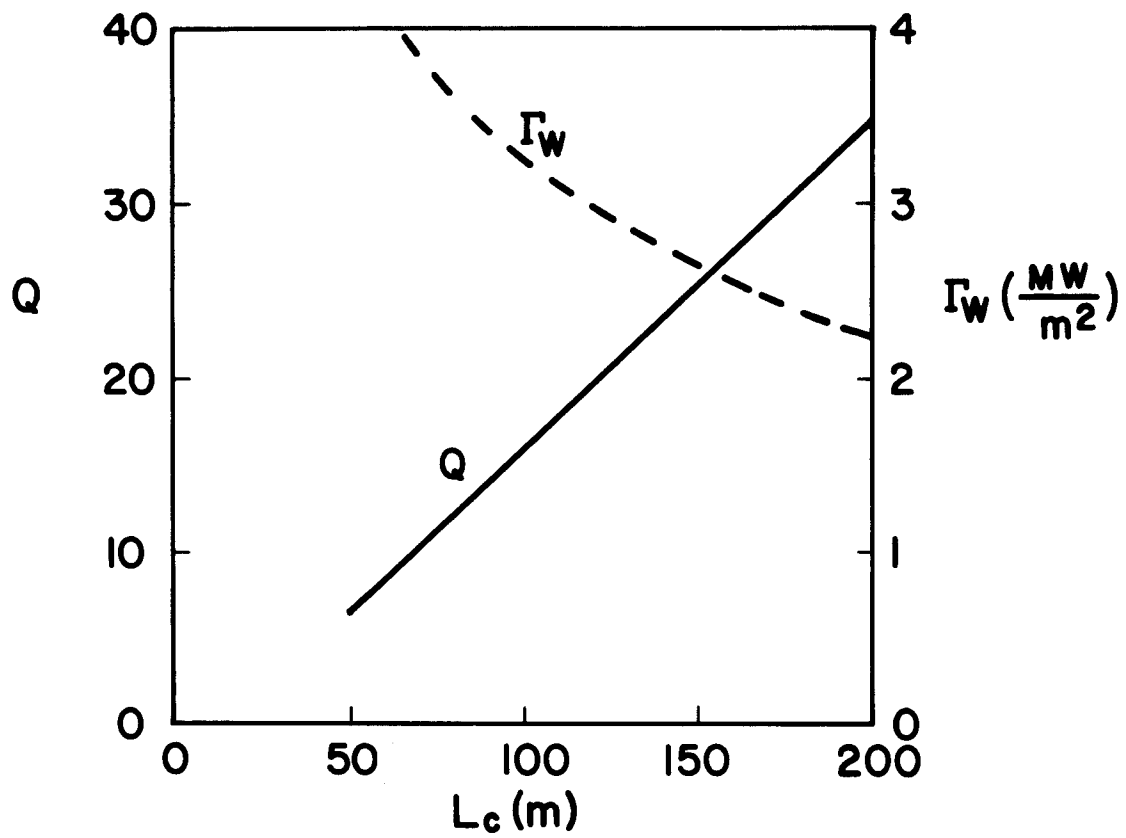


Fig. III-C-2

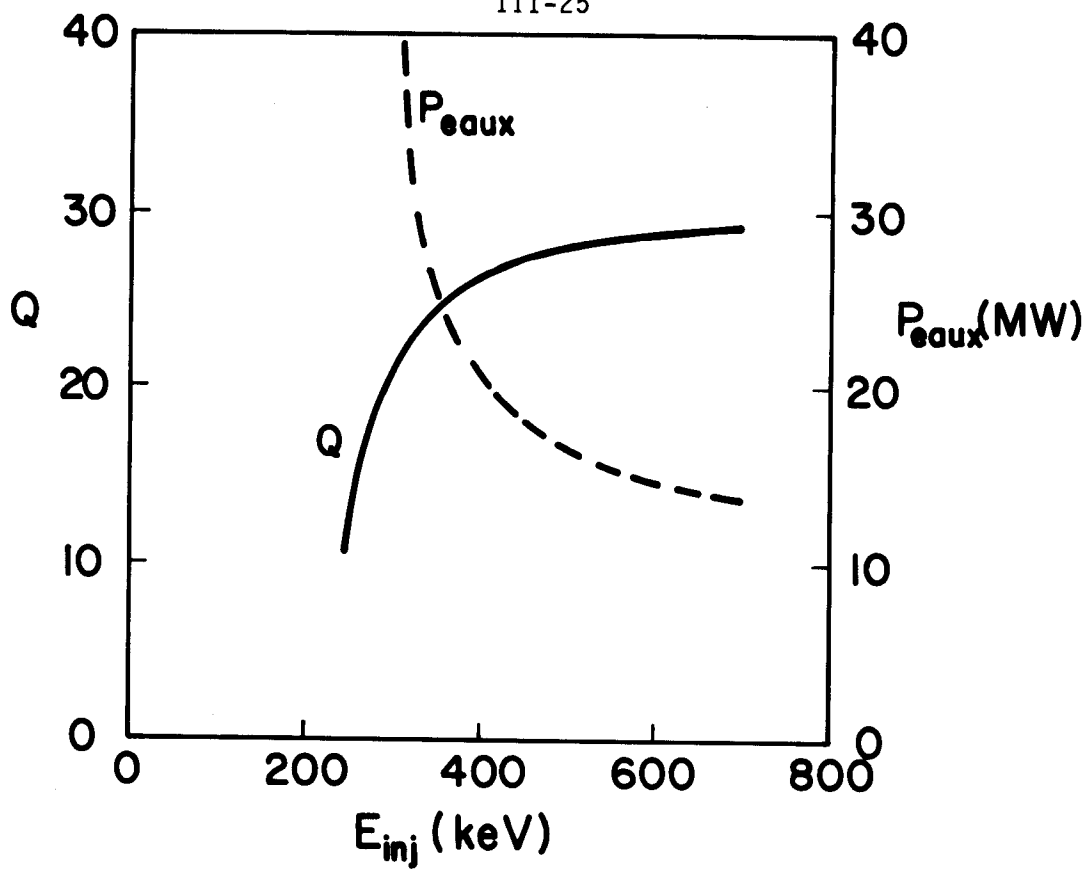


Fig. III-C-3

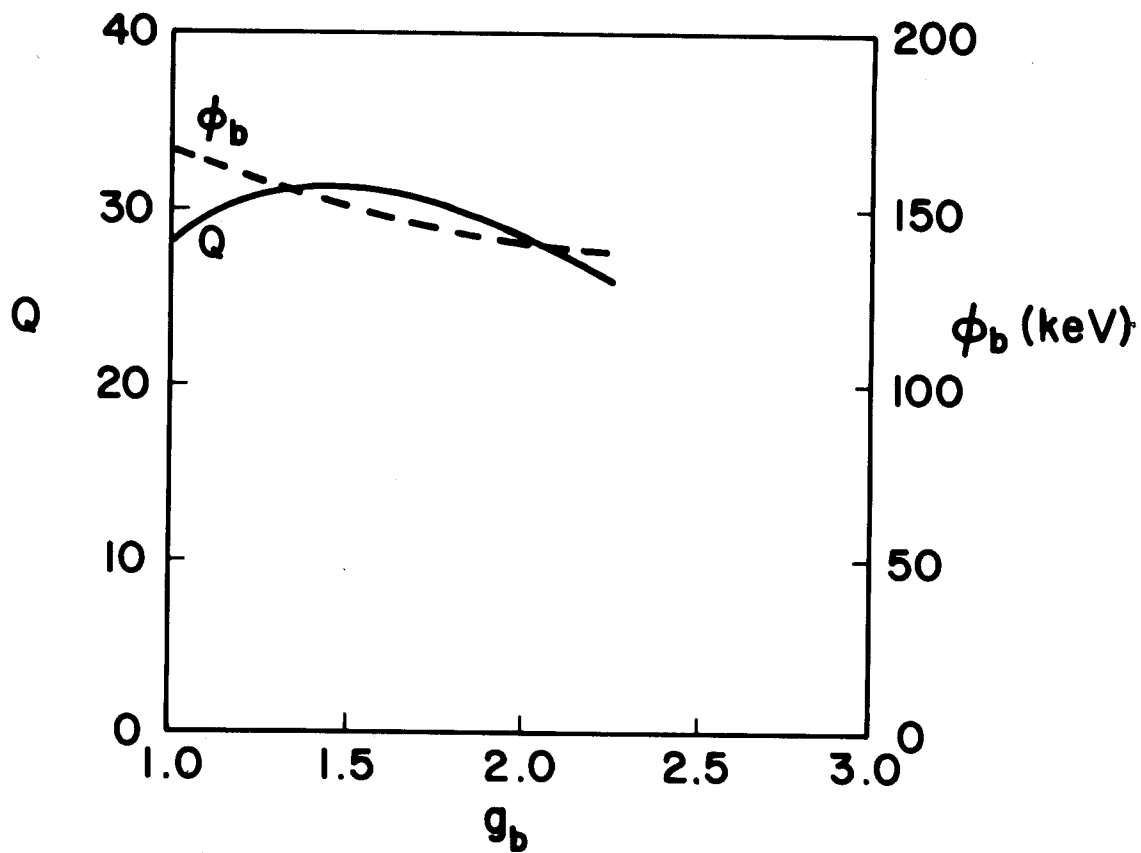


Fig. III-C-4

decrease associated with higher ϕ_b . This is a difficult process to model, and a value of g_b is simply legislated here. However, the model used for barrier ECRF gives the same qualitative behavior, as shown in Fig. III-C-4. Note that there is no strong dependence of Q on g_b for $g_b \lesssim 2$, but much larger values may be unacceptable.

The pumping process is sensitive to the potential, $f_{\text{phinb}}\phi_b$, at which the low energy pumping is done, as seen in Fig. III-C-5, which also shows that the magnetic field input value has very little effect.

Figure III-C-6 gives Q as a function of central cell beta, β_c . The strong dependence implies that high β_c is an important requirement for a tandem mirror reactor. Q rises sharply with β_c chiefly because P_{fus} is proportional to n_c^2 which is proportional to β_c^2 . The upper limit on β_c depends on MHD stability criteria.

As β_b rises, Q remains constant within 10%, with a slight maximum at about $\beta_b = 0.3$. However, E_{eh} is also rising as shown in Fig. III-C-7. This complicates the physics because of relativistic effects and adiabaticity questions. The ratio of passing electrons to total barrier electrons is falling as β_b rises, but higher f_{ecb} is probably preferable since there will then be a more certain source of electrons to the hot population.

As the central cell magnetic field decreases, Q remains approximately constant until B_c reaches 3T as shown in Fig. III-C-8. The subsequent falloff seems to be primarily due to the fall of central cell density with B_c^2 at constant β_c . MHD stability considerations imply that lower B_c will allow a higher β_c limit, but details of that limitation are still under investigation.

Figure III-C-9 covers two related cases: varying mirror ratio (solid line) and varying magnitude (dashed line) of barrier magnetic fields. There is a large effect on Q when B_{maxb} is held constant and the mirror ratio is varied by varying B_b . This neglects the longer barrier and consequent power increase necessary as B_b decreases, but this should be a smaller effect; also, as B_b rises, β_b falls, possibly helping MHD stability. There is a lesser, but still significant effect when the barrier mirror ratio, R_b , is kept constant at ten and the magnitudes are both varied. Again, larger magnitudes give smaller β_b and may help MHD stability.

The magnitudes of the plug fields for constant plug mirror ratio, R_p , have little effect on Q as shown by the dashed line in Fig. III-C-10. Q is somewhat more sensitive to plug mirror ratio as shown by the solid line.

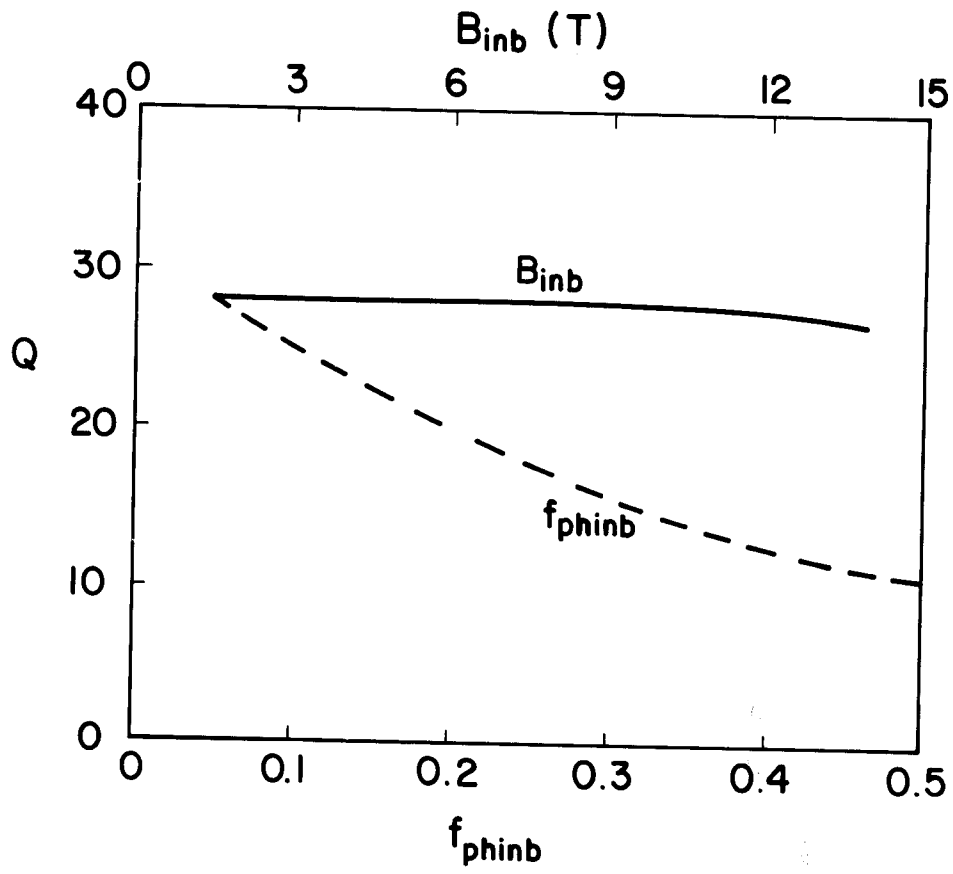


Fig. III-C-5

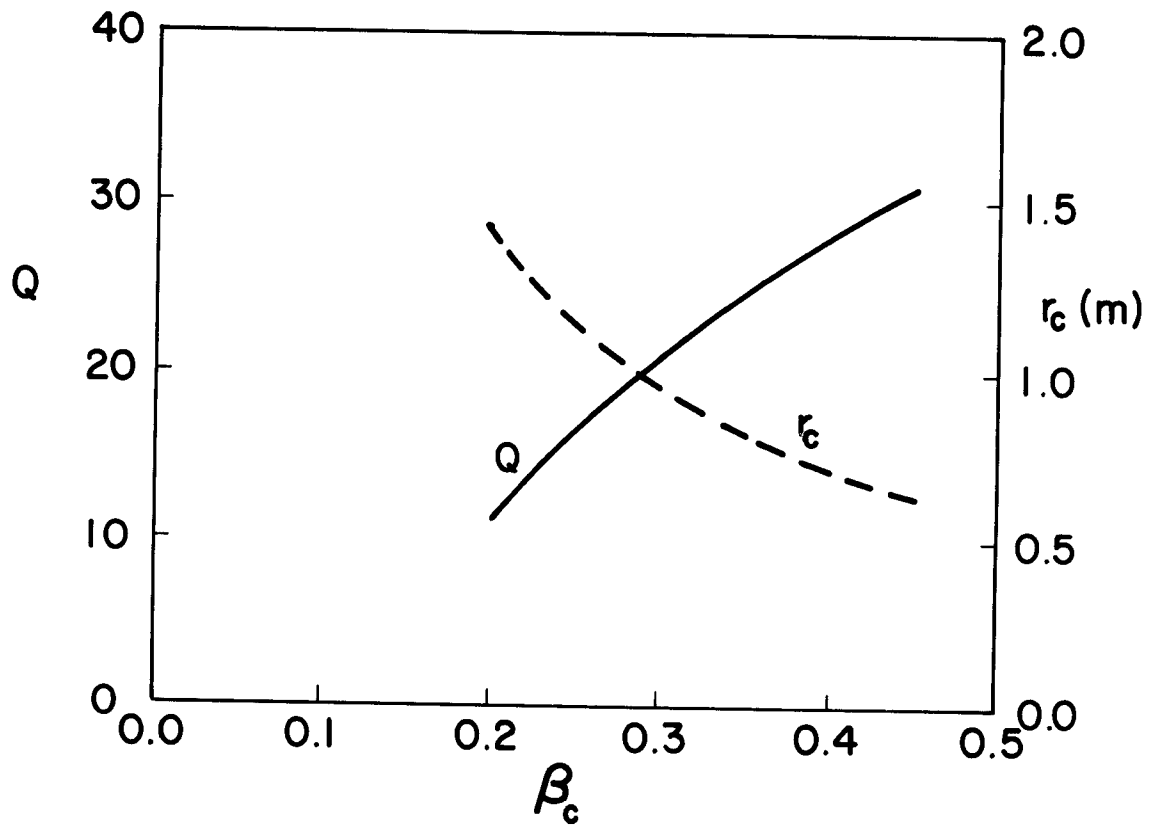


Fig. III-C-6

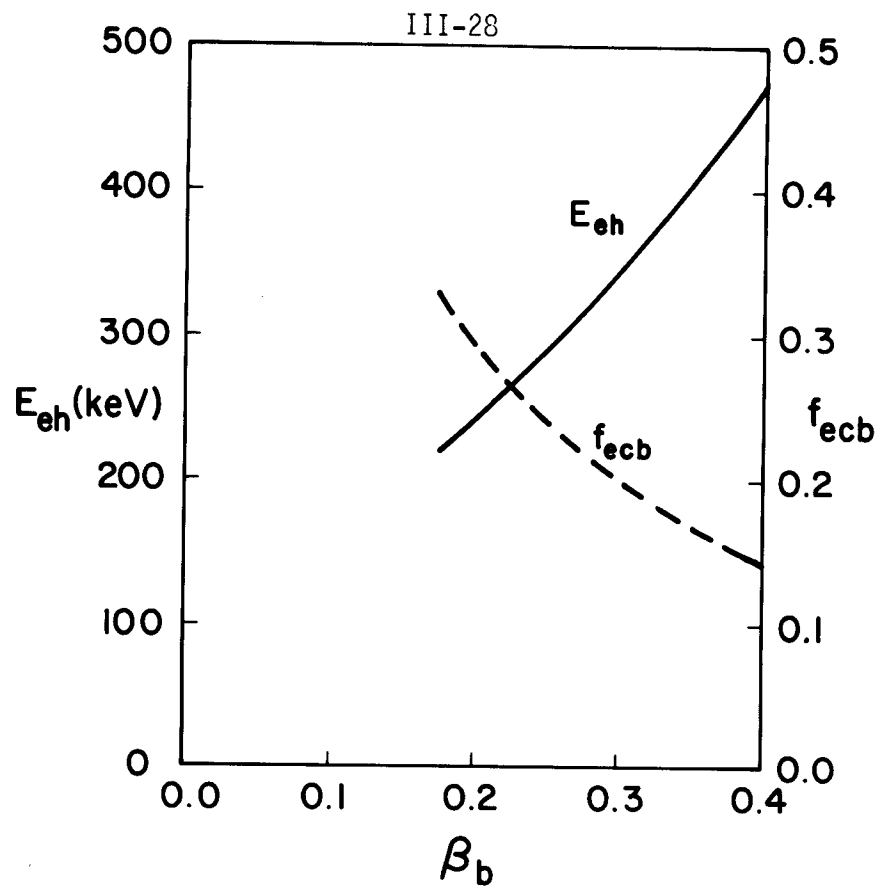


Fig. III-C-7

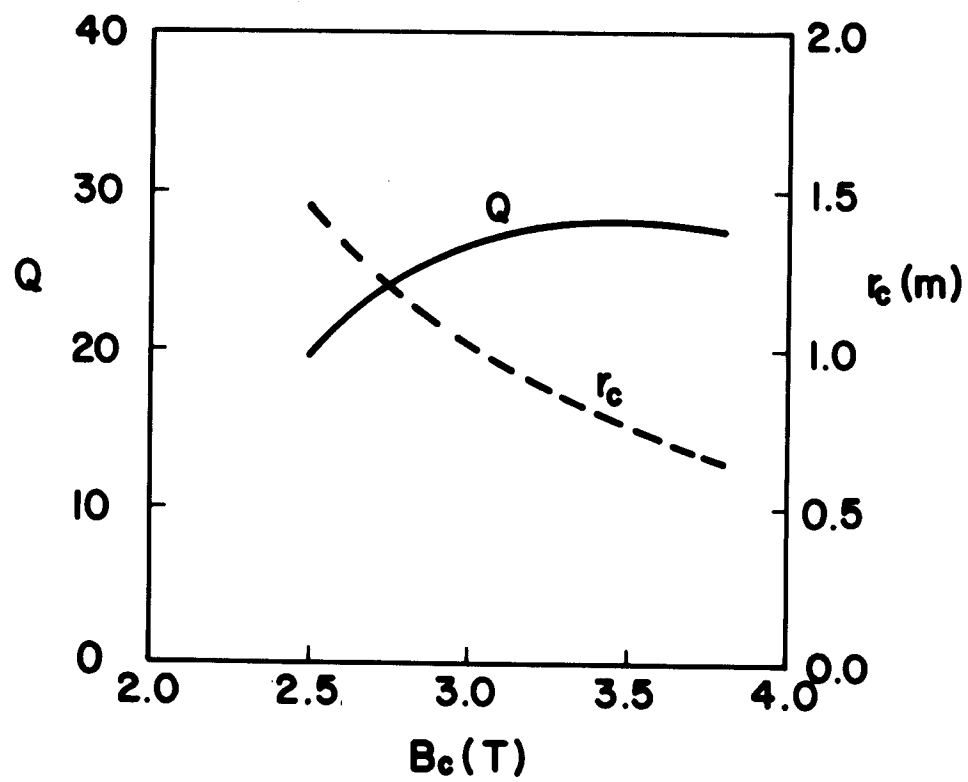


Fig. III-C-8

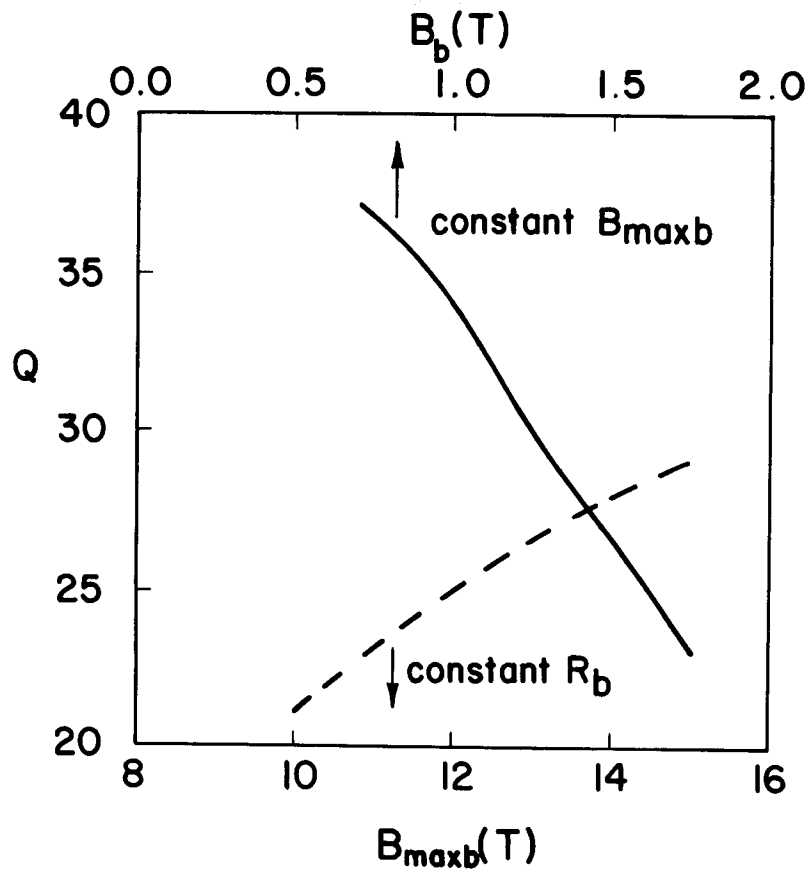


Fig. III-C-9

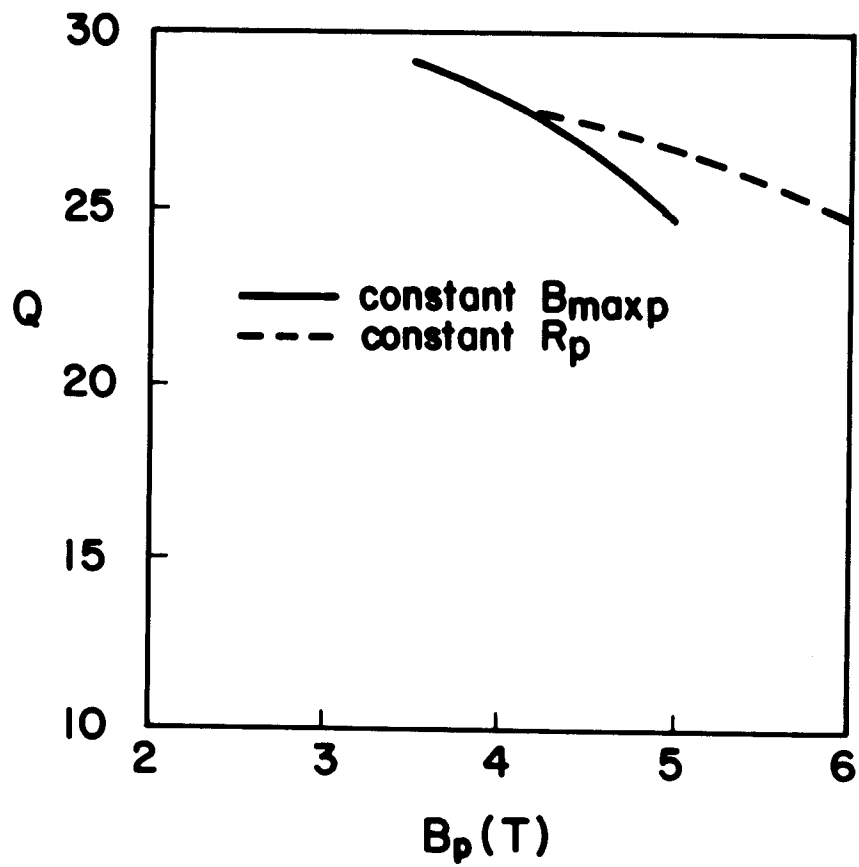


Fig. III-C-10

Although Q drops slightly as central cell ion and electron temperatures drop, as shown in Fig. III-C-11, temperatures in the barrier and plug also decrease. It may be advantageous to choose a lower temperature case in order to help satisfy physics limitations such as adiabaticity of hot particles in the barriers and plugs or relativistic radiation levels.

Figure III-C-12 shows that, as expected, it is best to pump the barrier with the highest possible ratio of low energy beam pumping fraction, f_{NB} , to high energy fraction, f_{BN} . Passive pumping was also explored, and little change in Q resulted when passive pumping replaced all or part of the low energy beam; passive pumping preferentially pumps faster particles and therefore does not access those localized near $B_p = 1.4T$ which still must be pumped by the high energy beam. However, neutral beam gun injection efficiencies are not reflected in the plasma Q , giving passive pumping a net advantage there. Unfortunately, the hot electron population would also be lost to passive pumping, so barrier ECRH is then ineffective, resulting in a degradation of Q which more than balances the advantages of passive pumping.

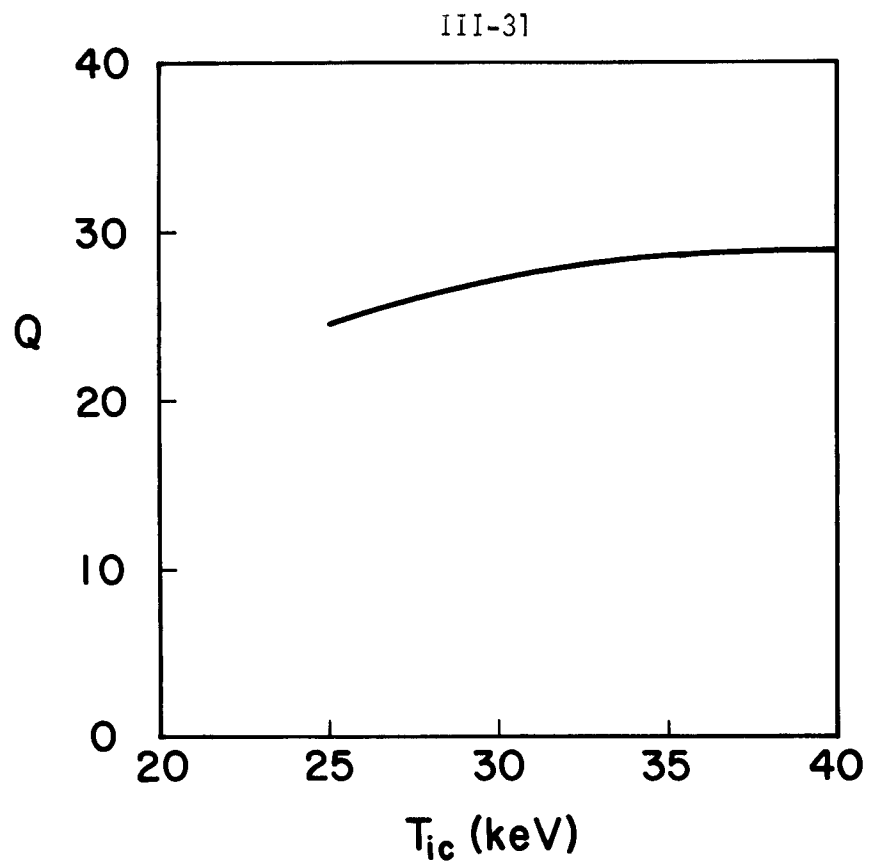


Fig. III-C-11

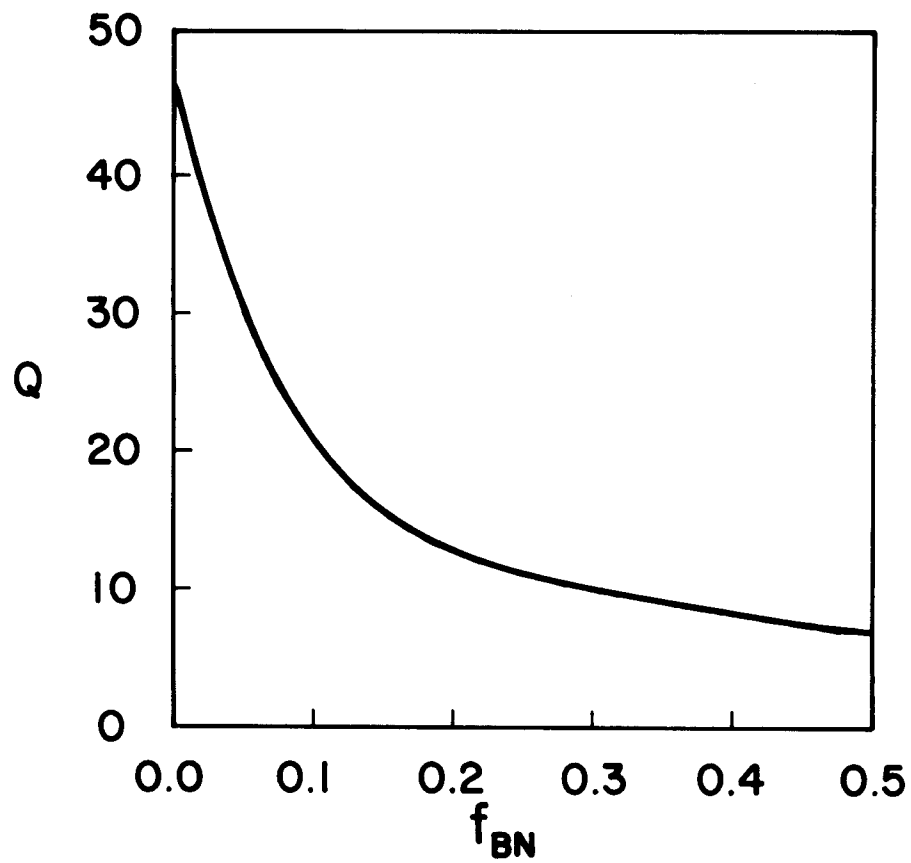


Fig. III-C-12

III-D. Details of the Design

The reference case plasma parameters used for WITAMIR-I are listed in Tables III-C-1 and III-C-2. The final plasma physics iteration occurred in July, 1980, so that work on other systems could be concluded by October.

A 3000 MW fusion power plant is chosen in order to give 1200 to 1500 MW_e of gross electric power. Then, reaching a Q of almost 30 achieves the design goal of about 100 MW of input power to the plasma. In particular, total ECRF power is less than 50 MW. High Q requires a central cell with short radius and long length because input power is proportional to barrier and plug volumes which are tied to central cell radius through flux conservation, while fusion power output is proportional to central cell volume = $\pi r_C^2 L_C$. A smaller radius allows smaller magnetic field coils for a given field strength, giving less expensive coils.

The central cell ions are, of course, deuterium and tritium. Protons are used in the plugs because the consequent absence of fusion neutrons from plug fusions allows the yin-yang coils to be designed with only central cell streaming neutrons in mind. Better microstability is also expected because protons have smaller gyroradii than deuterium or tritium.

Since, as Fig. III-C-3 shows, plug injection above 200 keV will be needed, which entails using negative ion beam technology, a value of 500 keV is used. This allows reasonable ECRF levels without unduly straining beam requirements. Negative ion technology will probably also be necessary for the 190 keV barrier pumping beam.

As discussed in Sec. III-B, electron cyclotron resonance heating (ECRH) is used in the barrier region to create a hot, trapped electron population. This complicates the physics and the machine while allowing higher Q and some lower technology requirements as shown in Sec. III-C. Although the barrier ECRH option is chosen here, it should be stressed that a tandem mirror reactor without barrier ECRH seems viable if not quite as attractive in terms of Q.

MHD stability questions remain with regard to the magnetic field design, with numerical calculations indicating a maximum central cell beta, β_C , of about 25% so far. Experiments in tokamaks^(1,2,3) and in the Wisconsin octupole⁽⁴⁾ exhibit beta values in excess of theoretical limits. The value $\beta_C = 40\%$ chosen for the reference case seems plausible in light of probable future improvements in the field design and the fact that the MHD theory is

somewhat tentative. All of the field magnitudes are attainable, as discussed in Chapter VI.

The pumping parameter, g_b , is taken to be 2.0 but is difficult to calculate precisely. It does not, however, have a drastic effect on other parameters, as shown in Fig. III-C-4.

Pellet fueling is chosen over gas puff fueling since charge exchange is expected to be small in the pellet fueling case and there will thus be no energy loss from cold fuel charge exchanging with warm central cell plasma. Charge exchange is of the same order of magnitude as ionization in the gas puff fueling case.

Plug and central cell surface heat loads arise from synchrotron and bremsstrahlung radiation. The barrier surface heat load comes from charge exchange neutrals from the barrier pumping process and synchrotron radiation.

Plausible values of the fractions of the barrier trapping current to be pumped by the low and high energy neutral beams were chosen. A detailed study of the pumping of the barrier would require a Fokker-Planck analysis and has not been attempted here. Passive pumping was also considered but was rejected because, as explained in Sec. III-C, the consequent loss of the ability to use barrier ECRH results in a net drop in Q . The passive pumping option may yet be pursued, however, especially if physics questions surrounding barrier ECRH cannot be resolved. It is important to note that a tandem mirror reactor based on passive pumping, while less attractive than one with barrier ECRH, is not unattractive and remains of interest.

References for Section III-D

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III-E. Microstability

Plasma confinement in present magnetic mirror machines is often limited by microinstabilities⁽¹⁾ that cause anomalous (relative to that due to classical Coulomb collisions) diffusion of particles in velocity space into loss-cones, from which they are lost in a very short (transit) time. These microinstabilities are driven by the free energy associated with the velocity-space anisotropy arising from the empty loss-cones. In a tandem mirror such as WITAMIR-I the end plugs are essentially just single mirror machines and hence are likely to be susceptible to the same kinds of microinstabilities. The various types of possible modes in the end plugs are discussed in Section III-E-1 below. A somewhat surprising conclusion is that the DCLC mode, which seems nearly ubiquitous in present experiments unless stabilized by warm plasma throughout, is apparently marginally unstable or damped in WITAMIR-I. However, there are other loss-cone driven microinstabilities which are probably unstable. In the barrier region the ion distribution function has a two-stream character, with an admixture of trapped ions. The combination is probably stable in spite of the velocity space anisotropy -- see Section III-E-2. Finally, in the central cell region the "loss-cones" are quite small and hence we do not expect any velocity-space driven microinstabilities; however, other unstable modes could occur and these are discussed briefly in Section III-E-3.

III-E-1. Microinstabilities in the End Plugs

Plasma is to be confined in the end plugs of WITAMIR-I primarily by the 1.5:1 magnetic mirrors there. This relatively weak mirror ratio (M) is enhanced by a factor of $(1 - \beta)^{-1/2} = 1/0.6$ by the finite β there; the effective mirror ratio is thus about 2.5. The loss-cone angle for such a mirror ratio is at about 45° ; hence the ion velocity distribution function should be quite anisotropic in the end plugs. In addition to particle losses due to classical Coulomb scattering into the loss-cone region, the free energy associated with the anisotropic velocity distribution can drive microinstabilities which could greatly increase the losses. The possible types of loss-cone driven microinstabilities and their possible consequences for the WITAMIR-I design are discussed later in this section.

Some important microinstability parameters for the WITAMIR-I end plugs are as follows. The square of the ratio of the plasma to gyrofrequency for

the electrons (ω_{pe}^2/Ω_e^2) ranges from 0.18 ($\beta = 0$) to 0.5 ($\beta = 0.64$). Thus, in contrast to many loss-cone microinstability calculations where it is assumed that $\omega_{pe}^2 \gg \Omega_e^2$, in WITAMIR-I we have $\omega_{pe}^2 < \Omega_e^2$. The ion gyroradius (ρ_i) ranges from 3.4 cm ($\beta = 0$) to 5.7 cm ($\beta = 0.64$). Hence the ratio of plasma radius (R_p) to ion gyroradius is 23 ($\beta = 0$) to 14 ($\beta = 0.64$). Similarly, the ratio of the axial magnetic scale length ($L_B \approx L/\pi(M - 1)^{1/2}$, where L is the plug length from mirror peak to mirror peak) to the ion gyroradius ranges from 73 ($\beta = 0$) to 25 ($\beta = 0.64$). Thus, the finite geometry can be expected to have significant effects on the loss-cone driven microinstabilities. The end-plug plasma is nearly collisionless ($v_{ij}/\omega_{bi} \sim 10^{-8}$); the classical Coulomb collision loss time is quite long -- 3.6 seconds. Since typical growth times of loss-cone induced microinstabilities are say 10 ion gyroperiods ($\sim 3 \times 10^{-8}$ sec), if any microinstabilities are present they would be expected to have sufficient time to grow, saturate, and induce anomalous losses. The usual "quasi-linear" consequence⁽²⁾ of the anomalous scattering processes is to "drag" a sufficient number of ions down into the loss-cone region of velocity space to produce enough "warm plasma" to stabilize the modes. In a tandem mirror the losses from the central cell can provide a warm plasma component, at least in the plug through which plasma is exiting into the direct converter. For the central cell particle loss rate in WITAMIR-I ($n = 2 \times 3.9 \times 10^{21}$ #/sec) the warm plasma density is about 5×10^{-5} of the plug ion density. This is a negligibly small component -- $n_w/n_H \gtrsim 10^{-2}$ is required^(2,3) for any significant effect on loss-cone driven microinstabilities. Thus, we see that, roughly speaking, if loss-cone driven microinstabilities were unstable and producing enough warm plasma for saturation, they would have to provide a warm plasma fraction at least 100 times that due to the central cell losses. This would imply a corresponding increase in the power flowing into a direct converter -- 447 MW $\rightarrow$ 44.7 GW!? Clearly such a power loss is intolerable and we should thus seek cases where the loss-cone driven microinstabilities are stable, or at least easily stabilized by other means.

With this general overview of the parameter regime of interest, we will now consider the various possible loss-cone driven microinstabilities. The mode of greatest consequence in present experiments⁽⁴⁾ is the drift-cyclotron loss-cone (DCLC) mode.⁽⁵⁾ While the source of free energy for this mode is the velocity space anisotropy, the radial plasma pressure gradient provides a (drift) wave into which the free energy can be coupled and hence facilitates

the microinstability. The mode becomes stable in the limit where the plasma radius becomes infinite ($R_p / \rho_i \rightarrow \infty$). Ignoring the axial variation effects, the critical radial pressure gradient scale length ($R_p^{-1} \equiv d \ln p / dr$) for stability of the DCLC modes for the WITAMIR-I parameter regime ($\beta \lesssim 1$, $\omega_{pe}^2 < \Omega_e^2$) is⁽⁶⁾

$$R_p > \frac{c/\Omega_i}{(2/C2)(1 + \omega_{pe}^2/\Omega_e^2)^{1/2}}$$

in which $C2 \approx 1 + \beta_i/2$ is a constant. The critical R_p for WITAMIR-I derived from this criterion ranges from 36 cm ($\beta \rightarrow 0$) to 70 cm ($\beta = 0.64$). Since the plug plasma radius is 77 cm, it would seem that the radial pressure gradients in WITAMIR-I may be weak enough to stabilize the DCLC mode.

This conclusion of stability (or marginality) of the DCLC modes in WITAMIR-I can also be arrived at from a number of other published criteria, most of which ultimately relate back to the work by Tang et al.⁽⁶⁾ For example, work done with respect to the MX proposal⁽⁷⁾ in 1976 indicated that for $\omega_{pe}^2 \gg \Omega_e^2$ the critical R_p/ρ_i for DCLC stability was about 100. However, to adapt those calculations to WITAMIR-I we must divide this critical value by the following factors: $\sqrt{2}$ for hydrogen rather than deuterium ions; ~ 2 for an axial average;⁽⁶⁾ and $(1 + \Omega_e^2/\omega_{pe}^2)^{1/2}$ for the small ω_{pe}/Ω_e limit. Thus the critical R_p/ρ_i is in the range of 14 ($\beta \approx 0$) to 20 ($\beta \approx 0.64$), which is comparable to the values given above for the WITAMIR-I design. Similarly, in the "Status of Mirror Fusion Research 1980" document⁽⁸⁾ (Fig. 2.4-17) the critical density gradient for $\omega_{pe}^2 \ll \Omega_e^2$ is given as $R_p/\rho_i \sim 70$ for $\beta \sim 0.64$. However, applying the same factors as above we then find the critical R_p/ρ_i to be about 14. As the variation in these estimates indicate, there is uncertainty in the conclusion about the stability of the DCLC mode in WITAMIR-I. However, the results discussed seem to favor the conclusion that it would be stable, and hence not cause any anomalous losses. Thus, it seems that the DCLC modes, which are so virulent in many current experiments, may be stable and hence unimportant in WITAMIR-I.

Next, we consider other loss-cone driven microinstabilities^(1,8): convective loss-cone (CLC),^(5,9) axial loss-cone (ALC),⁽¹⁰⁾ negative energy wave (NEW),⁽¹¹⁾ and Alfvén-ion-cyclotron (AIC)⁽¹²⁾ modes. In general the microinstability theory story here is less hopeful. However, a compensating factor is that few of these modes have been clearly identified experimentally. The

convective loss-cone mode⁽⁵⁾ causes⁽⁹⁾ a spatially inhomogeneous amplification of the thermal noise spectrum and hence enhanced "Coulomb scattering" losses. For WITAMIR-I parameters this enhanced scattering could easily exceed the standard Coulomb scattering losses by a factor of 10 or more. The axial loss-cone mode,⁽¹⁰⁾ is an absolute version of the CLC modes produced by "reflections" from ion cyclotron resonance regions; it can be stabilized only at very short axial scale lengths^(8,10) ($L_B/\rho_i < 7.5/\sqrt{\beta} \approx 10$). The negative energy wave⁽¹¹⁾ is apparently the same as the ALC, and hence has the same essentially unsatisfiable stability criterion. Finally, there is the Alfvén ion-cyclotron mode⁽¹²⁾ which is an electromagnetic mode driven by anisotropy in the ion velocity distribution. The anisotropy in the WITAMIR-I end plugs appears to be strong enough, given the high β , to drive this mode, at least in an infinite homogeneous plasma. However, while there have been some finite geometry calculations of this mode, it is not clear why these modes have not been observed experimentally, and hence if the mode would occur in WITAMIR-I.

The conclusion of this section is thus that the experimentally identified DCLC mode may be stable. However, there are a number of other loss-cone driven microinstabilities, most of which have not been observed experimentally in present devices, that apparently would be unstable in the WITAMIR-I end plugs. The only viable means of stabilizing these modes would appear to be⁽⁸⁾ by the trapping (via ECH or a "sloshing ion" distribution -- cf. Sections IV-B and V-B) of a warm plasma component of sufficient density and temperature to effectively remove the positive slope region in the ion velocity distribution function that is caused by the empty loss-cones.

III-E-2. Microinstabilities in the Barrier Region

In an ideal barrier the "loss-cone" regions of velocity space would be filled and the trapped region would be empty. However, in any barrier having a finite pumping speed there will be some trapped ions in the barrier. In the WITAMIR-I barriers there are approximately as many trapped particles as "passing" or "loss-cone" region particles.

The type of microinstability most likely to occur^(13,14) in the barrier region is a sort of two-stream instability in the ions. Numerical calculations^(13,14) for a perfectly pumped barrier indicate possible instability of these modes. However, when the trapped component due to imperfect pumping is added, the modes are easily stabilized. Thus, we tentatively conclude that the barrier may be free of significant microinstabilities.

III-E-3. Microinstabilities in the Central Cell

The plasma in the central cell is in the form of a long thin cylinder with a length to radius ratio of about 230. The velocity distribution functions of ions and electrons are both expected to be nearly Maxwellian. There is a radial pressure gradient which is a source of free energy that can drive drift-wave type⁽¹⁴⁾ microinstabilities.

In the absence of any experimental tests it is difficult to assess the potential importance of the drift modes to radial plasma confinement in the central cell. An estimate⁽¹⁴⁾ of the possible transport based upon a free energy argument⁽¹⁵⁾ yields a diffusion coefficient

$$D \sim v_e R_p^2 (m_e/m_i \beta)^2 \sim 2 \text{ cm}^2/\text{sec}, \text{ and consequently a plasma containment time}$$

$\tau \sim a^2/6D \sim 400 \text{ sec.}$ Since this is much longer than the axial containment time, the possible radial anomalous transport losses in the central cell due to drift waves may be negligible. However, in view of the lack of experimental evidence or much theory, this is at best a very tentative conclusion.

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15. T. K. Fowler, in Advances in Plasma Physics, A. Simon and W. B. Thompson, Eds. (Wiley, New York, 1968), Vol. I, p. 201.

III-F. Plasma Buildup and Radial Transport Simulations

A fluid model of the central cell plug plasmas generated by taking velocity moments of the bounce-averaged drift kinetic equation has been used to perform numerical simulations of the buildup and steady-state phases of operation of a tandem mirror reactor with inboard thermal barriers. The model is 1-D in cylindrical geometry and incorporates classical radial transport, three dimensional molecular cold gas transport in cylindrical geometry, and neutral beam transport corrected for finite gyroradius effects on the beam deposition profile in both the central cell and plugs. Unlike previous codes⁽¹⁾, which assumed fixed plug conditions, this code allows for radial transport in the plug as well as in the central cell.

The thermal barrier is treated using $\nu = .5$, as in the plasma analysis section (Section III-B), but the code contained no provisions for hot, magnetically confined electrons in the thermal barrier. As a consequence, the barrier potential, ϕ_b , is not as large as it otherwise would be and the energy transfer between the plug electrons and the central cell electrons is increased. Maintaining the plug electron temperature requires that the plug ECRH power be increased. In order to at least partially correct for this lack of correspondence between the code and the WITAMIR-I design, which uses hot, magnetically trapped electrons in the thermal barrier, the pumpout parameter, g_b , was set equal to unity (no trapped particles in the barrier). The WITAMIR-I design assumes $g_b = 2$. This reduction of g_b is not as effective, however, as hot, magnetically trapped barrier electrons in increasing ϕ_b and reducing the plug ECRH power.

A second difference between the code and WITAMIR-I is that WITAMIR-I uses pellet refuelling of the central cell, whereas the code has no provision for calculating the pellet deposition profile. Consequently the code assumed a combination of gas and low energy neutral beam refuelling of the central cell. This will have some impact on the steady-state parameters of the central cell.

Table III-F-1 lists the basic machine parameters used in the radial transport code. Since the code is time-dependent, initial profiles are required to start the calculation. The density profiles were chosen to be Gaussian with an e-folding distance about half the central cell radius. The temperature profiles were chosen to be flat over most of the radius and drop sharply near the edge. The boundary conditions at the edge were chosen to be fixed low values (typically 10^4 cm^{-3} for the densities, 3 eV for the ion

Table III-F-1

Machine Parameters for Radial Transport Calculations

Central Cell Length, m	165
Plug Length, m	4
Central Cell Magnetic Field, T	3.6
Barrier Maximum Magnetic Field, T	15
Barrier Minimum Magnetic Field, T	1.5
Plug Maximum Magnetic Field, T	6
Plug Minimum Magnetic Field, T	4
Barrier Pumpout Parameter (g_b)	1
Neutral Beam Energy-Plug, keV	500
Central Cell Refuelling Beam Energy, keV	85

temperatures, and 1 eV for the electron temperatures). Since the basic equations are much the same as those used in the 0-D plasma analysis (Section III-B), except for the addition of radial transport in the particle and energy balance equations, they will not be repeated here.

Figure III-F-1 shows the time dependence of the on-axis central cell ion density, ion temperature, and electron temperature for a particular startup scenario in which it is attempted to bring the central cell to full density and temperature in 1 sec. The central cell is fueled and heated by a 85 keV neutral beam. The initial beam current is large to build up the density and then reduced to provide the required steady-state refuelling rate. The ECRH power to the plug electrons is 36 MW and has a rise time of .5 seconds. The ion temperature rises rapidly at first because the power is being absorbed by a low density plasma and then flattens as the density rises and as energy losses increase. The time dependence of the potential ϕ_c , plug electron temperature, and the alpha density are shown in Fig. III-F-2. The ion confining potential ϕ_c drops as buildup proceeds; this is due to progressively lower plug to central cell density ratios. In this calculation, the various densities and temperatures reach their quasi-steady-state values in 1-2 seconds without serious overshoot. Overshoot can occur, however, if too much power is injected when the density is too low.

The central cell alpha density, shown in Fig. III-F-2, does not approach a steady-state value but continues to increase throughout the burn. The alpha particles are too well confined by the plugs because of their doubly-charged state and, with 10 times classical diffusion assumed here for radial transport, do not diffuse sufficiently rapidly in the radial direction. A mechanism for removing alphas so that a true steady-state burn can be achieved in a tandem mirror reactor is a fundamental and as yet unsolved problem.

The quasi-steady-state values for the plug and central cell density and temperatures obtained in this calculation are considerably below the WITAMIR-I design values. This is because of the increased plug-central cell electron energy transfer due to the lack of hot, magnetically trapped barrier electrons in the code. As a consequence, ϕ_b is reduced; this leads to reduced plug electron temperature and reduced ϕ_c , the plug confining potential. Shown in Fig. III-F-3 are the radial profiles of the density and temperatures in the central cell when the plug ECRH is raised to 108 MW in an attempt to come closer to the WITAMIR-I design values. The central cell ion and electron

Fig. III-F-1

Variation of Central Cell Parameters During Startup

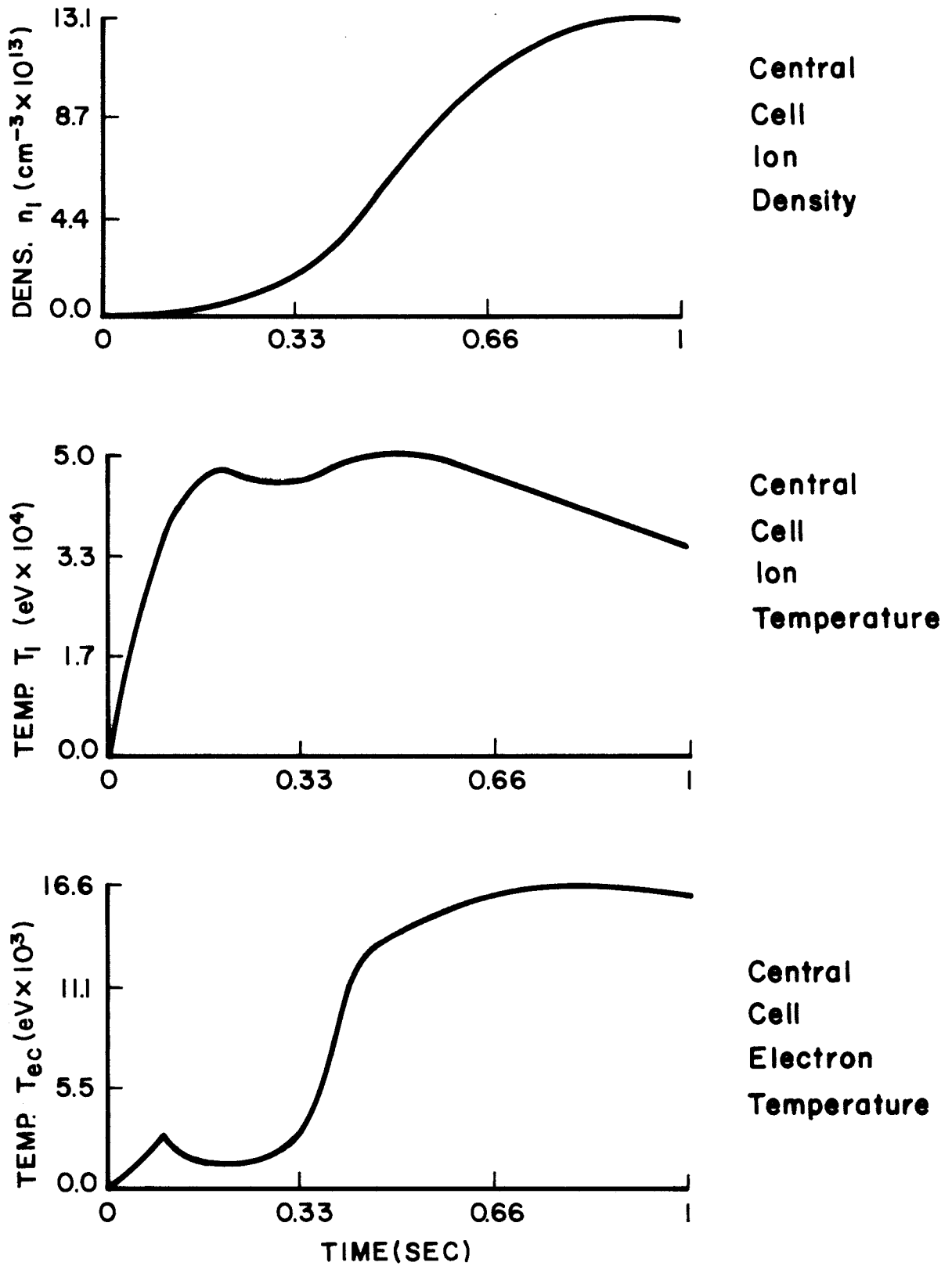
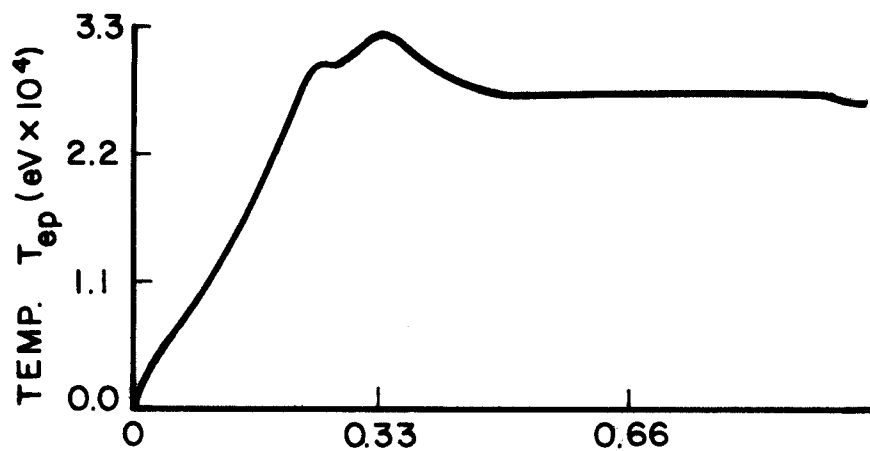
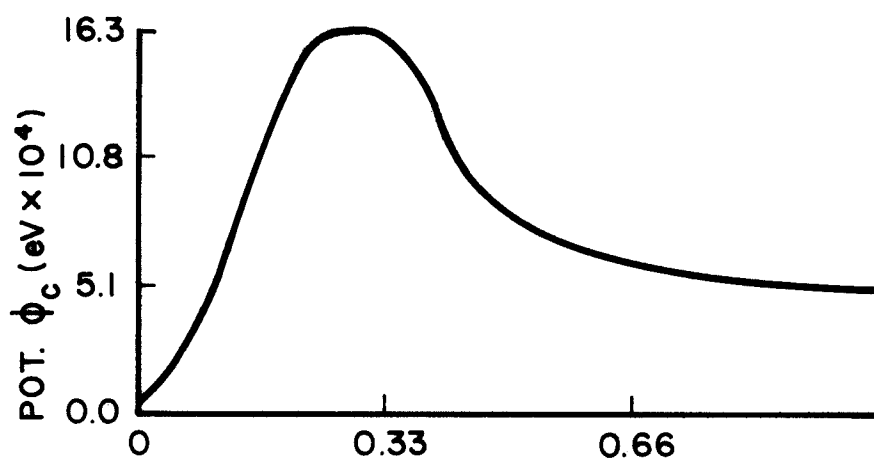


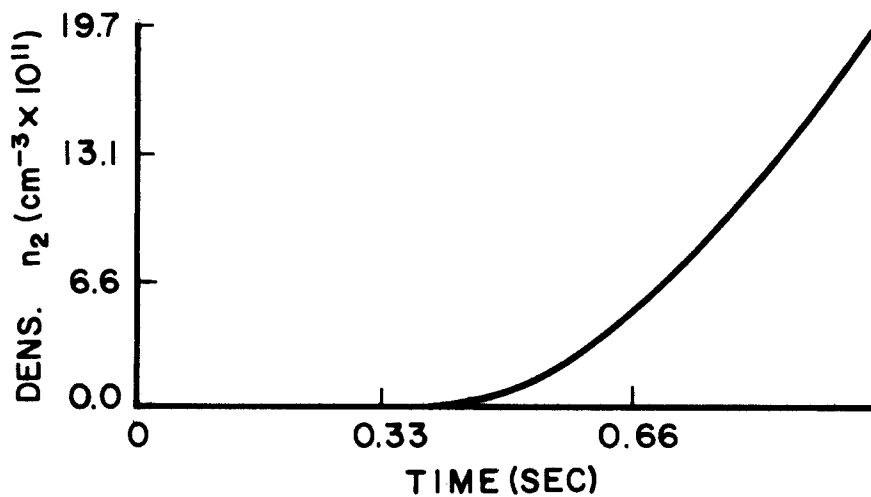
Fig. III-F-2
Variation During Startup



Plug
Electron
Temperature



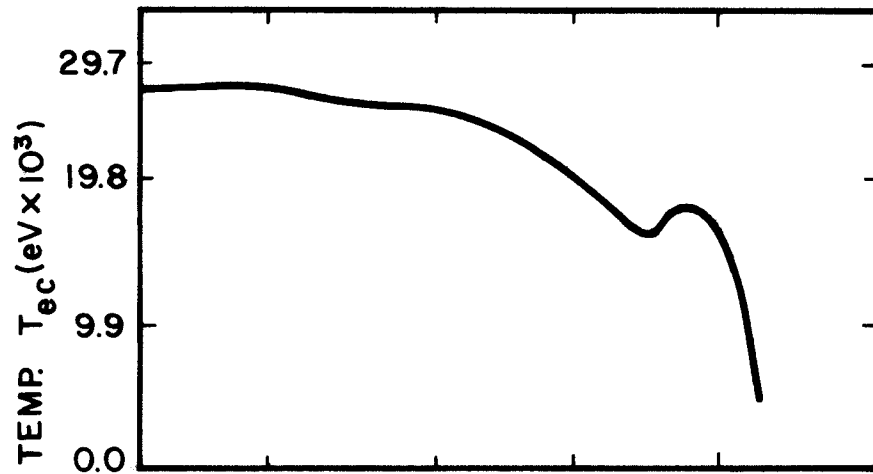
Plug
Confining
Potential
(ϕ_c)



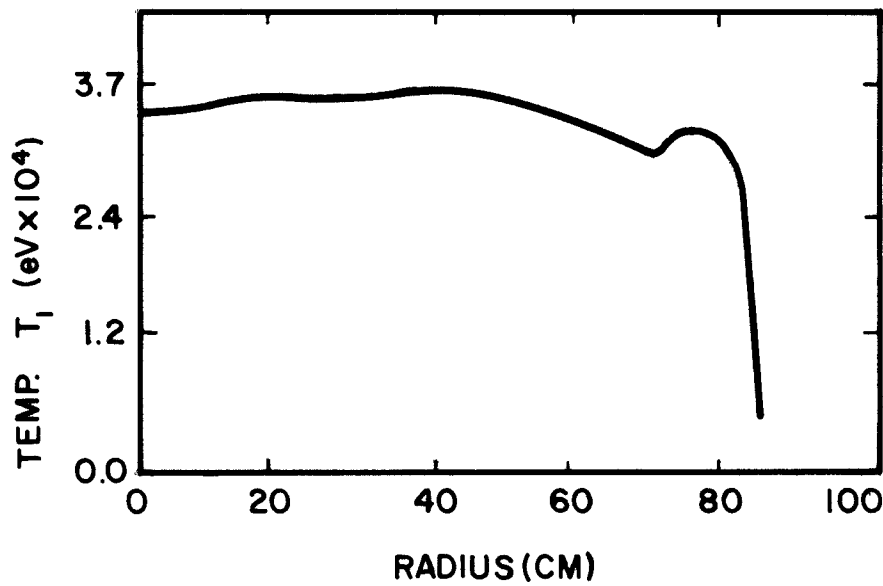
Central
Cell
Alpha
Density

III-45
Fig. III-F-3

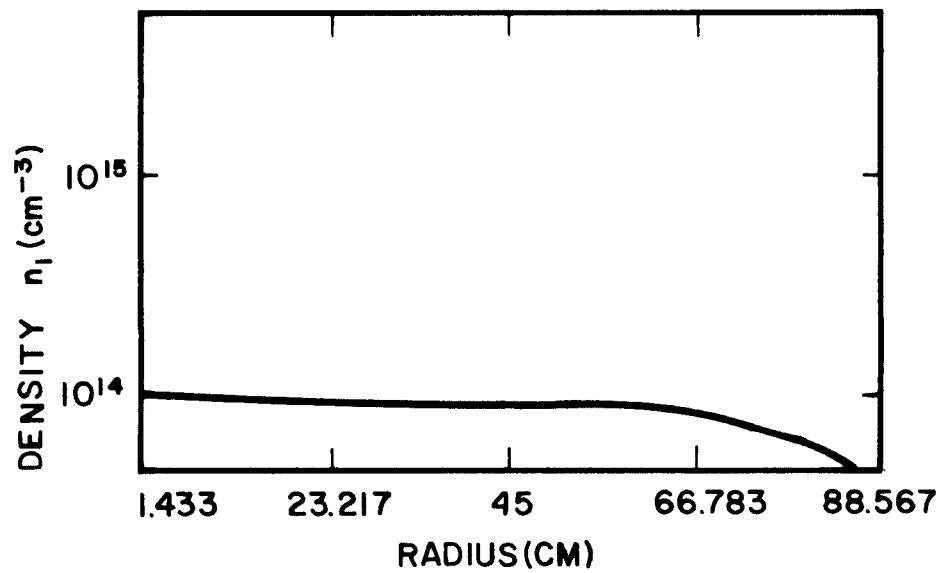
Radial Profiles in Central Cell



Electron
Temperature



Ion
Temperature



Ion
Density

temperatures of 34 and 29 keV, respectively, during the quasi-steady-state operation compare favorably with the design values of 32.5 and 32.8 keV. The central cell density ($1.1 \times 10^{14} \text{ cm}^{-3}$) is somewhat below the design value of $1.5 \times 10^{14} \text{ cm}^{-3}$.

The profiles shown in Fig. III-F-3 are rather flat, which is good from the point of view of achieving the maximum fusion power for a given on-axis beta. The flat profiles, however, make central cell refuelling more difficult. Cold gas injection at the edge leads to some refuelling in the interior, because of the generation of fast neutrals by charge exchange. This, however, is about an order of magnitude too small on axis to support the central plasma against end-loss. The flat density and temperature profiles also make it more difficult for the pellets to penetrate to the center (see Section V-C).

The calculations presented here are representative of a variety of cases run. Although these calculations are still rather preliminary, certain general features have emerged:

- 1) Careful programming of the injection power and particle sources during startup will be required to reach the desired operating point. Because of the cooling effect of particle sources and because of the overall $T_e^{-1/2}$ scaling of the dominant energy loss terms at low T_e , more injection power is needed during startup than at the steady-state operating point (at least in the calculations so far). Nevertheless, the equilibrium operating points are accessible.
- 2) Feedback control of the plug electron temperature may be necessary during the steady-state operation because of the $T_e^{-1/2}$ scaling of the energy transfer between the plug and central cell electrons.
- 3) Radial diffusion may not be sufficient to remove the helium "ash" created in the central cell. Ash removal is a continuing major concern for the tandem mirror reactor. This conclusion, however, depends very much on the radial transport processes, for which no experimental data is yet available.

Reference for Section III-F

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IV. End Plug Design

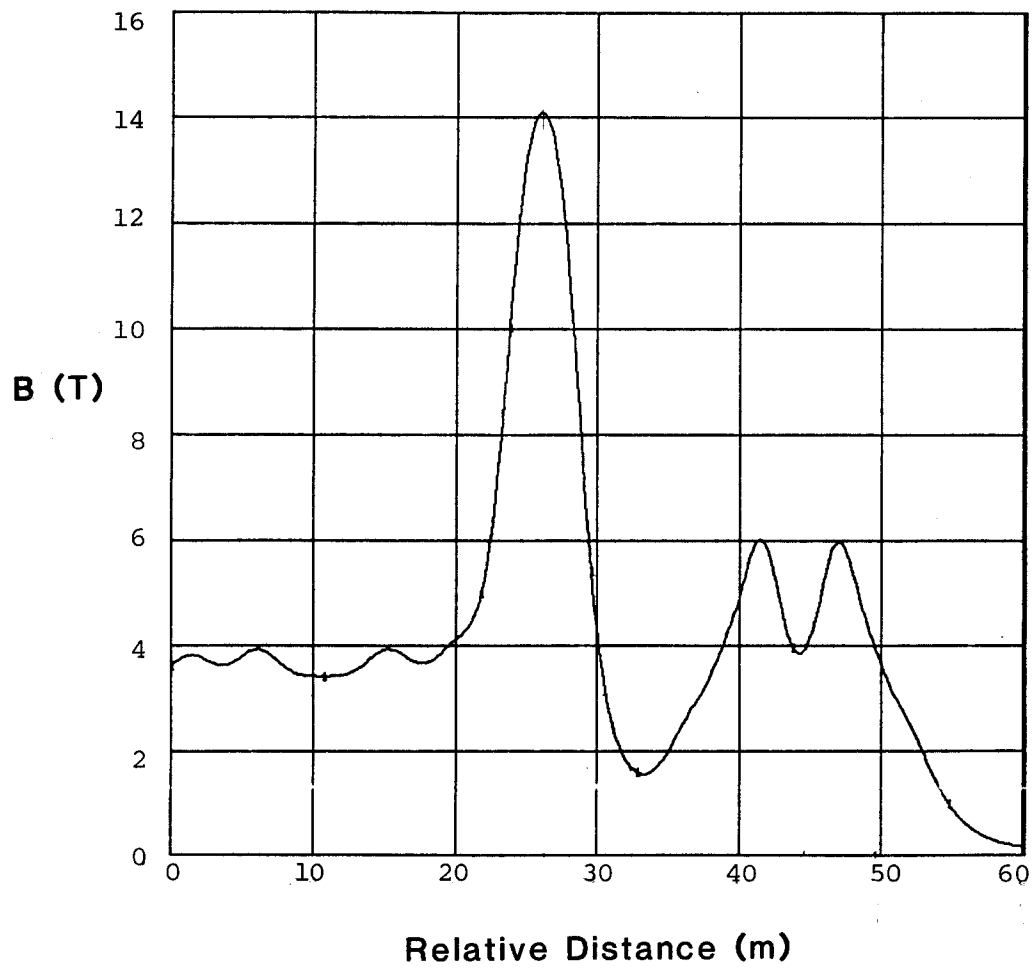
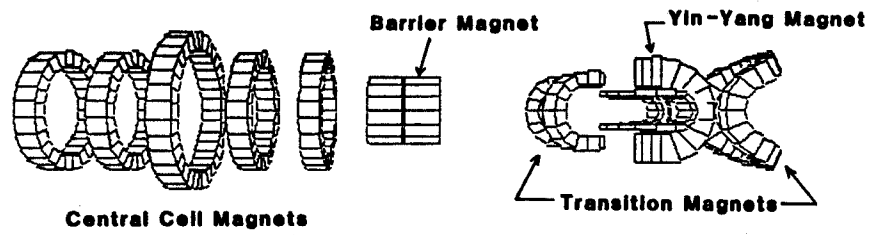
IV-A. The WITAMIR-I Configuration

The end plug configuration used in WITAMIR-I is shown schematically in Fig. IV-A-1. The electrostatic potential peak, which "plugs" the central cell in the axial direction, is generated by the plasma confined in the minimum-B magnetic mirror (called the "plug"). This plug also provides the good curvature needed to compensate for bad curvature of the magnetic field elsewhere, so that the plasma is stable against MHD instabilities. (A MHD stability analysis of the WITAMIR-I design has not been performed; see Section III-A for a discussion.) The cross-section of a flux tube is circular on the midplane of the plug and becomes elliptical at the mirror throat; it is then recircularized by the transition coil (sometimes called a "c" coil) before entering the central cell. The thermal barrier, generated by the high field solenoidal magnet, lies between the transition coil and the central cell. A second transition coil, outside of the plug, helps to reduce the ellipticity of the flux tube before it enters the direct convertor and to balance the forces on the yin-yang magnets in the plug. The magnet system is described in detail in Chapter VI.

Because of the thermal isolation provided by the thermal barrier, the plug electrons can be heated above the central cell electron temperature without requiring excessive power. This heating is accomplished using ECRH resonant on the plug midplane. A second use for ECRH is to generate a hot, magnetically trapped electron component in the thermal barrier, which increases the barrier potential. The RF power is generated using gyrotrons and transported to the plasma with beam waveguides. The analysis of plasma heating with ECRH and the design of the beam waveguide system is discussed in Chapter V.

The hot magnetically confined ions in the end plug are sustained by a 500 keV neutral beam injected into the end plug plasma. Neutral beams are also used to "pump" the thermal barrier. Ions become trapped in the thermal barrier by collisional scattering and must be removed to prevent the density dip in the barrier from filling in. The barrier pump beams are injected at a small angle to the magnetic field; pumping is accomplished by charge exchange between the beam and trapped ions. The pump beams are injected between the control cell magnets and pass inside the bore of the high-field barrier solenoid before intercepting the plasma. A discussion of the neutral beam

Fig. IV-A-1
THE WITAMIR-I END PLUG CONFIGURATION



requirements and sources is give in Section V-D. The physics analysis of the interaction of the beams with the plasma, both in the plug and in the barrier, is given in Section III-B.

The various engineering considerations of shielding in the end plugs, heat transfer requirements, structural design, etc. are given in their respective sections.

The WITAMIR-I configuration is not the only possible end plug configuration; several alternatives have been proposed. Some consideration has been given in this reactor design study to an alternate configuration proposed by Kesner. This configuration utilizes an axisymmetric potential peak which is separated from the min-B plasma (the MHD anchor) and is described in Section IV-B.

IV-B. Alternative End Plug Configuration

A number of adverse effects, including enhanced crossfield transport, increased sensitivity to field errors, and currents which distort the central flux tube are known to derive from the non-axisymmetric quadrupole nature of the MHD anchors that bind the central cell of a tandem mirror. Therefore a significant gain would be obtained from an axisymmetric confinement scheme.

Kesner⁽¹⁾ has recently proposed that the use of sloshing ions can enable axisymmetric ion confinement in a tandem mirror, as well as permitting enhanced microstability. The proposed arrangement is to have the central cell bounded by axisymmetric sloshing ion cells in which both the thermal barrier and ion plugging potential are created. These axisymmetric cells in turn connect, through a transition region, to minimum-B "anchor" cells, located on the ends of the tandem mirror.

The geometry of the axisymmetric tandem is quite similar to WITAMIR-I, which has an inner thermal barrier which joins up to outer yin-yang plugs. This arrangement is also quite complimentary to the MFTF-B proposed arrangement, in which the sloshing ion cell is located on the outside of the anchor. This setup is hoped to exhibit enhanced stability at the expense of decreased central cell confinement resulting from the non-axisymmetry induced neo-classical transport.

The magnetic field arrangement is shown schematically in Fig. IV-B-1 and the expected magnetic field, density and potential profiles are shown in Fig. IV-B-2. Since all central cell confined ions will reflect either at the high

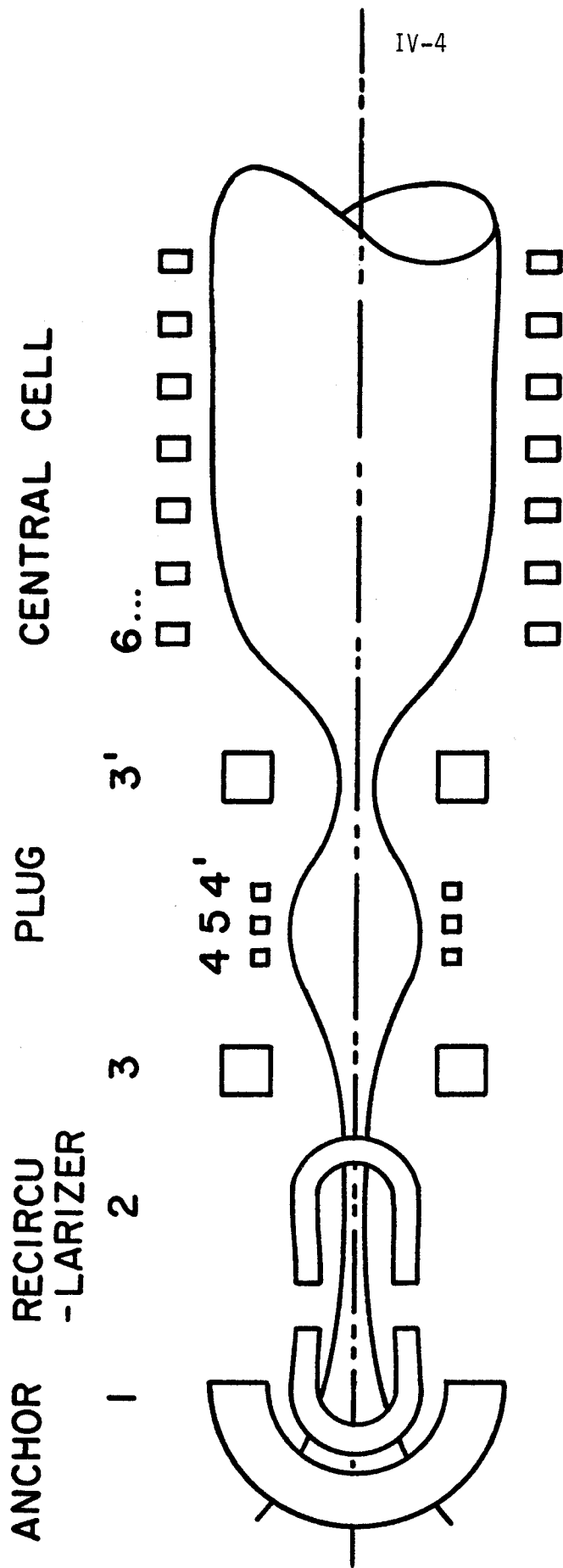


Fig. IV-B-1

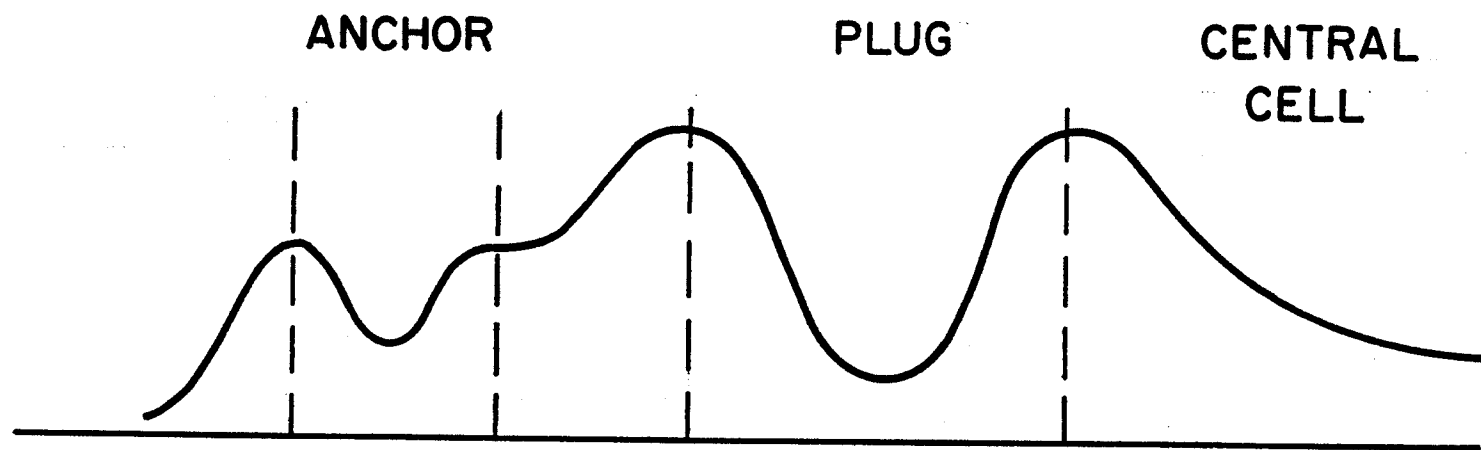
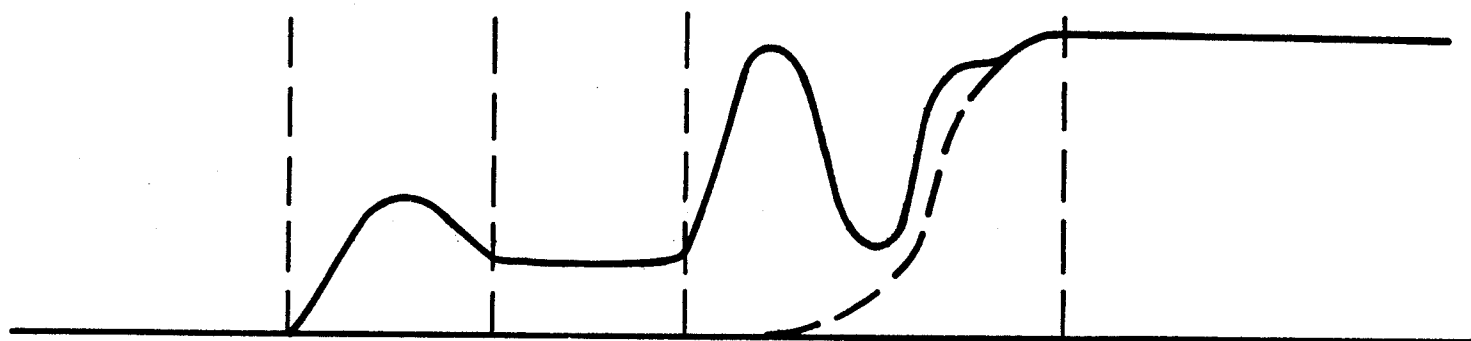
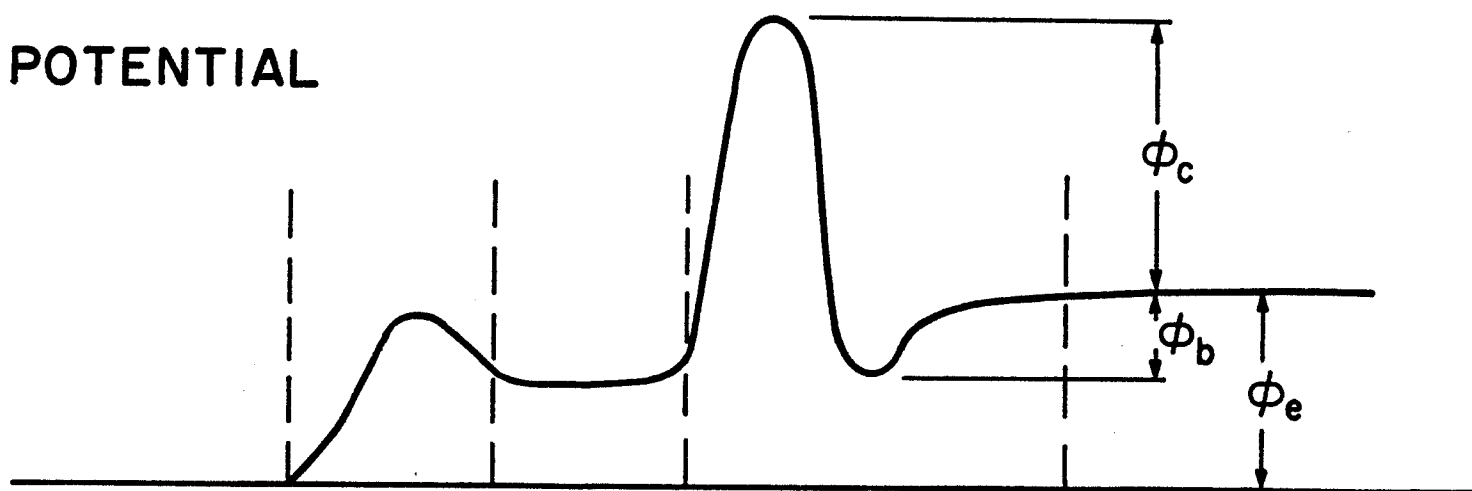
MAGNETIC FIELD**DENSITY****POTENTIAL**

Fig. IV-B-2

magnetic field bounding the central cell or at the potential peak in the outer part of the sloshing-ion cell they cannot sample the non-axisymmetric anchor.

An expanded view of the field and potential profile of the sloshing ion cell is shown in Fig. IV-B-3. As discussed by Logan ECRH power must be applied at the field minimum (b) to create a "hot" magnetically trapped electron component as well as at point (a) which establishes a "warm" electron component. For access to (b) we will require $\omega_{ce}(b) > \omega_{pe}(b)$. ECRH access will require substantial fields and low β in this cell. (The lower beta of the S.I.C. will be seen to diminish the bad curvature-induced destabilization introduced by this cell.)

Some of the physics considerations of such a tandem mirror are presented in ref. (1). Logan has made a more detailed analysis of sloshing ion cell physics which appears in ref. 9., Section III-A.

In considering an axisymmetric tandem mirror containing sloshing ion plugs as a reactor a number of questions require careful evaluation:

- 1) The neutral beams that create the sloshing ion component can be injected at either the sloshing ion cell midplane, the potential peak or the inner bounce point. Injection at the potential peak permits the minimal beam energy, but introduces cold electrons into the region where hot RF-heated electrons are required, thus increasing RF power requirements. At the other two locations less RF power would be required but at the expense of higher beam energies. Also at these locations, charge-exchange of the sloshing ion beams does some pumping, but this effect is probably small at energies required in a reactor.
- 2) Pumping the thermal barrier in this scheme as in WITAMIR-I presents a substantial problem. Neutral beam charge-exchange pumping still appears feasible but analysis of other schemes may be useful.
- 3) The overall MHD stability to ballooning needs to be considered. Since recircularization at high fields is inherent in this scheme the possibility of improved beta limits exists. A comparison of ballooning among various reactor schemes would be useful.
- 4) Microstability of the outermost "anchor" cell remains an important consideration. Sloshing ions may be useful here as a stabilization technique and other techniques such as use of ECRH could be considered. The desire for high-beta in this cell coupled with the large potential drop and presence of fairly hot electrons, suggests that this cell be run at the

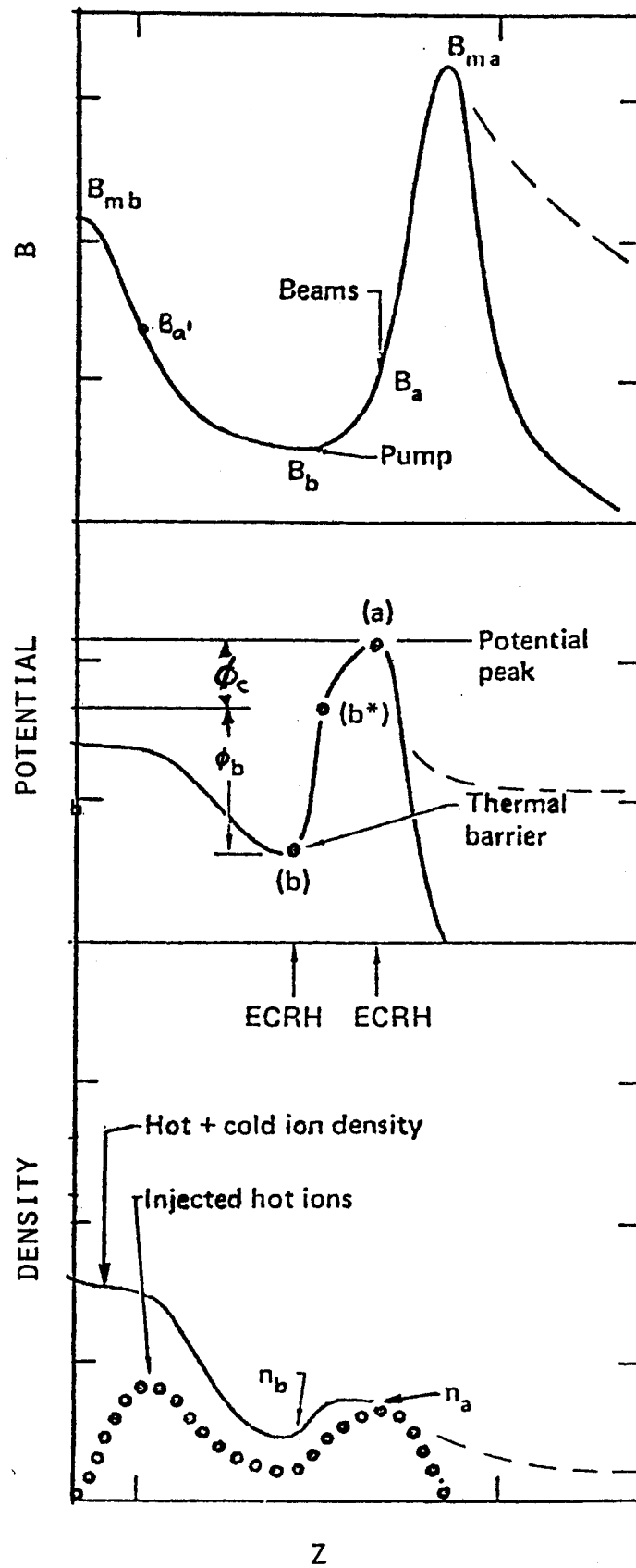


Fig. IV-B-3

maximum possible ion energy and the minimum density consistent with the desired value of beta.

- 5) A more quantitative treatment of the benefits of axisymmetry would be made more feasible by the detailed design of an axisymmetric tandem mirror reactor.

Reference for Section IV-B

1. J. Kesner, "Axisymmetric Sloshing Ion Tandem Mirror Plugs," University of Wisconsin Report UWFD-328 (1979); Nuc. Fus. 20 (1980), 557.

V. Heating and Injection

V-A. Introduction

We have considered the problems of electron heating of the end plugs and the thermal barrier in the University of Wisconsin Tandem Mirror Reactor (WITAMIR). The thermal barrier concept provides an attractive method for reducing the plug density, neutral beam injection energy and power requirements as well as enhancing the reactor Q. We have concentrated on the use of Electron Cyclotron Resonance Heating (ECRH) and have performed a ray tracing analysis for plasma heating in the end cells which is discussed in Section V-B. We have also briefly considered the possible roles of the Ion Cyclotron Range of Frequencies (ICRF) regime for supplementary heating in the end plug as an alternative.

Radio frequency heating of the plug and barrier electrons plays a major role in the success of the recent implementation of a thermal barrier in the tandem mirror reactor. A high plug electron temperature ($T_{ep} \sim 120$ keV) is designed to raise the central cell ion confining potential ϕ_c and to lower the plug density so that neutral beam and magnet technological requirements in the plug will be eased. Heating the electrons at the barrier minimum to very high energies ($T_{eb} \sim 270$ keV) together with barrier pumping reduces the local potential, providing thermal isolation of the hot electrons in the plug from the central cell. Among the various candidates for electron heating, electron cyclotron resonant heating (ECRH) stands out because of its near-100% absorption efficiency and the projected availability of efficient high-power gyrotron tubes in the 40-120 gigahertz (GHz) frequency range.

For the WITAMIR design parameters, the electron heating zones are located near the plug and thermal barrier magnetic field midplane regions. For local magnetic fields of 4 and 1.4 Tesla the cyclotron frequencies are 112 and 40 GHz, respectively. Electron heating in the plug and barrier regions requires a careful analysis of the electron velocity distribution, collisional interactions with particles from other regions, and proper modelling of electron cyclotron radiation losses. These matters are largely the determining factors in the reduction of end losses and enhancement of the overall Q of the reactor design. The problem is complicated by the complexity of the magnetic geometry in these regions. The plasma beta is 23.5% in the barrier cell and reaches 64% in the plug; this will substantially modify the local magnetic field and

hence the wave propagation and absorption and will be incorporated in future analyses.

The total power (for both end cells) absorbed by the electrons is 48 MW. Assuming a 100% electron absorption efficiency and a 50% gyrotron RF power source efficiency, the d.c. power requirement is 96 MW. Due to the important cost factors associated with ECRH power, we consider its impact on the economics. Presently, a continuous wave gyrotron with an output of 212 kW at 28 GHz is being tested at Varian Associates' Microwave Tube Division in Palo Alto, CA. This gyrotron uses a tube and superconducting magnet combination priced at \$195,000. This amounts to a cost slightly below \$1 per watt of microwave power delivered. The device requires a highly regulated power supply of 80 kV at 8A for the tube and a 4 x (25 V at 500 A) power supply for the magnets at a cost of roughly \$700,000. This yields a cumulative total of \$3.7/watt delivered. Including present waveguide feeds, this would cost about \$4/watt delivered for a prototype. The power supply was not optimized for this particular application. Furthermore, the efficiency of the tube was 40-42% and Varian anticipates that ultimate efficiencies as high as 70% are realizable in this frequency range. Optimizing the power supply for a given application and increasing the tube efficiency is expected to bring the cost of the power supply for large scale installations down to ~ \$1/watt. The cost of the tube and magnet is not expected to go down very much, so that the overall cost per unit power delivered eventually should stabilize at \$2/watt in 1980 dollars. Thus, while cost factors for ECRH systems are important, there is some reason for optimism when one scales to high power 40-120 GHz systems with specialized power supplies.

Refueling of the central cell by pellet injection is discussed in section V-C. The neutral beam sources for plug heating and refueling and for barrier pumping are discussed in section V-D.

V-B-1. Role of ECRH in Tandem Operation

Direct heating of the electrons in the plugs and the barrier cells of a tandem mirror reactor is a possible means to improve central cell confinement and, perhaps, to stabilize the DCLC-mode in the end plugs. Recent experimental results, improved theoretical understanding and research and development on high-power, high-frequency gyrotrons lead us to believe that electron cyclotron resonance heating (ECRH) of plasmas with reactor parameters will soon become a possibility.

V-B-1-a. Plug Heating

The WITAMIR design provides central cell ion confinement by the potential

$$\phi_c = T_{ep} \ln \left[\left(\frac{n_p}{n_b} \right) \left(\frac{T_{ec}}{T_{ep}} \right)^{.5} \right] - T_{ec} \ln \left(\frac{n_c}{n_b} \right) \quad (1)$$

and requires an electron temperature in the plug of 123 keV. These electrons are designated "warm", and their peak density, which occurs in the center of the plug, is $2.7 \times 10^{13} \text{ cm}^{-3}$. A study of the energy balance (cf. Chapter III) has shown that to maintain this combination of density and temperature a power of 8.25 MW per plug will be required.

Heating electrons locally (experimental evidence from the TM3 and ISX indicates that this is feasible) in the center of the plug enhances the ambipolar potential difference for central cell ion confinement. It also digs a small potential well in which low-energy ions are trapped, thus filling the loss-cone and contributing to the stabilization of the DCLC mode. The minimum magnetic field strength in the plug is 4T which corresponds to an electron cyclotron frequency ω_{ce} of 112 GHz. This is the upper limit of the frequency range in which powerful continuous wave gyrotrons are expected to become available, and heating will, per force, be at the fundamental frequency (ω_{ce}).

Accessibility problems may occur due to the presence of cutoff layers surrounding the center of the plug. The ECRH stabilization will be discussed in further research.

V-B-1-b. Barrier Heating

The potential difference between barrier and central cell (ϕ_b) which provides thermal isolation between the plug and central cell electrons is given by

$$\phi_b = T_{ec} \ln \frac{n_c}{n_b - n_{eH}} \quad (2)$$

where T_{ep} is the plug electron temperature, and n_p , n_b and n_{eH} are the plug electron density, total barrier electron density and hot barrier electron density, respectively. Figure V-B-1 represents the velocity space diagram for the barrier electrons. It can be seen that ECRH, which preferentially increases the perpendicular energy, will raise n_{eH} , thus increasing the potential difference and improving the central cell ion confinement.

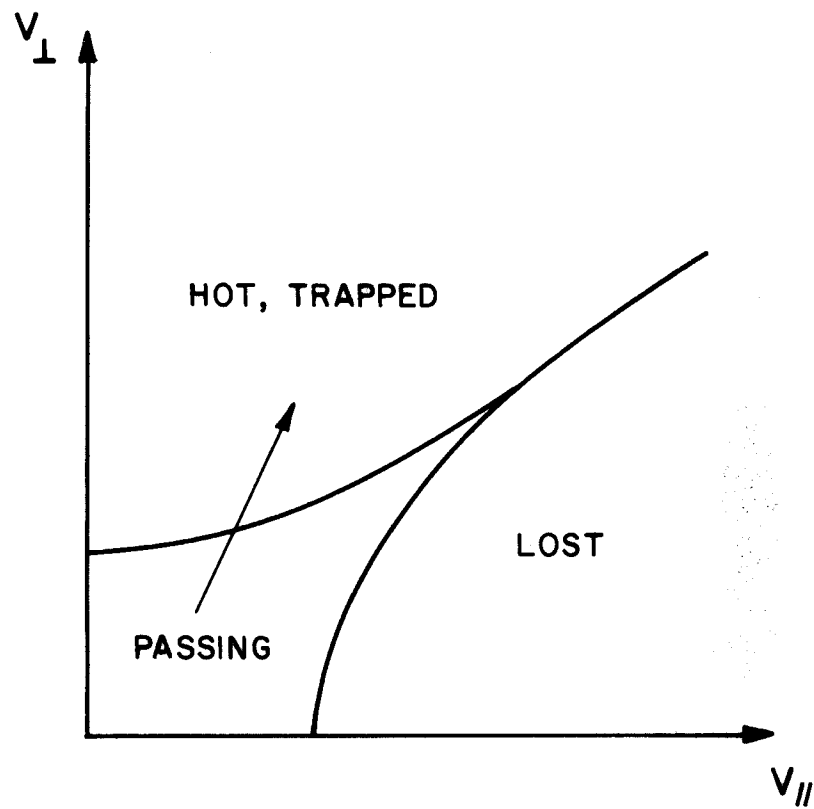


Fig. V-B-1

Velocity-space diagram for barrier electrons.
ECRH preferentially increases $v_{\perp}$, thus
trapping passing electrons.

The minimum magnetic field in the barrier is 1.4 T which corresponds to electron cyclotron resonances at 40 GHz (the fundamental frequency), 80 GHz (second harmonic) and higher frequencies outside the range in which gyrotrons are expected to become operational. At the second harmonic the extraordinary mode has the strongest absorptivity. At a temperature of 270 keV and a hot electron density of $7 \times 10^{12} \text{ cm}^{-3}$ about 16.5 MW will be needed for each barrier cell.

V-B-2. Ray Tracing Theory

V-B-2-a. The Equations of Geometrical Optics

The geometric optics approximation requires that the waves are weakly damped along the ray trajectory; a sufficient (though not necessary) condition for weak damping is that the Hermitian part $\bar{\sigma}^H$ of the conductivity tensor⁽²⁾ be much smaller than the anti-Hermitian part. Under this approximation, the dispersion relation of the plasma is

$$D = \text{Det } (\bar{D}^H) = 0 \quad (1)$$

where

$$\bar{D}^H = \left(1 - \frac{c^2 k^2}{\omega^2}\right) \bar{I} + \frac{c^2 \bar{k} \bar{k}}{\omega} + \frac{4\pi i}{\omega} \bar{\sigma}^A \quad (2)$$

The dispersion relation is related to the eikonal equation, the eikonal being the phase function ψ of the wave electric field:

$$\bar{E}(\bar{r}, t) = \text{Re} \{ \bar{E}_0(\bar{r}, t) e^{i\psi(\bar{r}, t)} \} \quad (3)$$

where in general

$$\omega = - \frac{\partial \psi}{\partial t} \quad ; \quad \bar{k} = \nabla \psi \quad (4)$$

Using the general form $\psi(\bar{r}, t)$ we avoid the restriction to plane waves. Note that $D = D(\bar{k}, \omega, \omega_p(\bar{r}(t)), \omega_c(\bar{r}(t)))$ and that, by virtue of Eq. 4, the dispersion relation can be regarded as a partial differential equation for the eikonal $\psi(\bar{r}, t)$. Linearizing this P.D.E. around a point where $\psi = \psi_0(\bar{k}_0, \bar{r}_0, \omega)$ yields the following characteristic:

$$\frac{d\bar{r}}{dt} = - \frac{\partial D / \partial \bar{k}}{\partial D / \partial \omega} = \frac{\partial \omega}{\partial \bar{k}} \quad (5)$$

The partial derivatives $\frac{\partial D}{\partial \bar{k}}$ and $-\frac{\partial D}{\partial \omega}$ are the coefficients of $\frac{\partial \psi}{\partial \bar{r}}$ ($= \bar{k}$) and

$\frac{\partial \psi}{\partial t}$ ($= -\omega$) in the original equation. Using the conservation law

$$\nabla \omega + \frac{\partial \bar{k}}{\partial t} = 0 \quad (6)$$

(which follows from Eqs. 4), we also find an equation for the evolution of the wave vector $\bar{k}$:

$$\frac{\partial \bar{k}}{\partial t} = - \frac{\partial \omega}{\partial \bar{r}} = \frac{\partial D / \partial \bar{r}}{\partial D / \partial \omega} \quad (7)$$

Equation 5 merely states that the wave energy moves along the ray trajectory with the group velocity $\partial \omega / \partial \bar{k}$. For a cold plasma the partial derivatives of D with respect to $\bar{k}$, $\bar{r}$ and ω can be obtained analytically and the right-hand sides of Eqs. 5 and 7 are explicitly known functions of ω , $\bar{k}$, $\bar{r}$, and t , i.e. the equations reduce to a set of ordinary differential equations which can be solved by a variety of numerical methods, e.g. a Runge-Kutta scheme. We follow Batchelor et al.⁽¹⁾ and solve the ray equations using the Appleton-Hartree dispersion relation.

V-B-2-b. The Absorption Coefficient

For wave propagation in spatially dependent equilibria the absorption coefficient

$$\alpha = 2 \bar{k}_i \cdot \frac{\bar{v}_{gr}}{v_{gr}} \quad (8)$$

can be derived from the energy balance⁽²⁾ and expressed in terms of the dielectric tensor $\bar{\bar{K}}$:

$$\alpha = \frac{\omega}{4\pi} \frac{\bar{E}^* \cdot \bar{\bar{K}}^A \cdot \bar{E}}{|\bar{S}(\bar{k}_r)|} \quad (9)$$

where

$$\bar{S}(\bar{k}_r) = \frac{c}{4\pi} \text{Re}(\bar{E} \times \bar{B}^*) - \frac{\omega}{8\pi} \bar{E}^* \cdot \frac{\partial \bar{\bar{K}}}{\partial \bar{k}} \cdot \bar{E} \quad (10)$$

is the total energy-flux density per unit $\bar{k}_r$ -volume and $\frac{\omega}{4\pi} \bar{E}^* \cdot \bar{R}^A$. $\bar{E}$ is the power density absorbed per unit volume in $\bar{k}_r$ -space. The first term is the Poynting vector, while the second refers to a flux of kinetic energy related to the particle coherent motion in the wave.

For a further discussion of the absorption of electromagnetic waves in the electron cyclotron frequency range one has to distinguish different regimes with respect to both the angle of propagation and the plasma density. As far as the angle θ (the angle of propagation, i.e. the angle included between $\bar{k}$ and $\bar{B}_0$) is concerned, there exist two main regimes. One is defined where the width of the resonance is determined by Doppler broadening,

$$\frac{ck_r}{\omega} |\cos \theta| > \frac{v_{th}}{c} , \quad (11.a)$$

and the other where relativistic effects prevail and

$$\frac{ck_r}{\omega} |\cos \theta| < \frac{v_{th}}{c} . \quad (11.b)$$

Inspection of Eqs. 11 reveals that the relativistic regime is limited to a cone centered about $\theta = 90^\circ$ and that this cone becomes larger with increasing temperature. Hence we will refer to either the Doppler (oblique regime), or the relativistic (quasi-perpendicular) regime.

With respect to the density, again two different regimes appear related to the magnitude of the electric susceptibility χ of the plasma electrons. If χ is low, plasma effects are weak and essentially proportional to the plasma density. If any of the elements of the susceptibility tensor are large compared to 1, strong plasma effects appear and both dispersion and polarization depend non-linearly on the plasma density.

We use the following expressions for the absorptivity $\alpha^{(3)}$ which are correct to the lowest order in v_{th}/c :

* Oblique propagation, low density, $\omega \approx \omega_c$

$$\alpha^{x,0} = \frac{\omega_p^2}{2c\omega} \frac{c}{v_{th}} \left[\frac{1 \pm 1}{\sqrt{8\pi}} (1 + \cos^2 \theta) - \sqrt{2} \frac{\omega - \omega_c}{\omega_c} \sin^2 \theta \frac{1 \mp \sin^2 \theta}{1 + \cos^2 \theta} \right] \quad (12)$$

$$\times \frac{\pi}{|\cos \theta|} e^{-\zeta^2}$$

where $\zeta \equiv \frac{1}{\sqrt{2}} \frac{\omega - \omega_c}{\omega} \frac{c}{v_{th}} \frac{1}{N |\cos \theta|}$, N is the refractive index of the mode

under consideration and the upper (lower) sign refers to the "extraordinary" ("ordinary") mode.

* Oblique propagation, high density, $\omega \approx \omega_c$

$$\alpha^{x,0} = 2\sqrt{2} R^{x,0} \frac{v_{th}}{c} \frac{\omega_c}{c} \text{Im} \left[-\frac{1}{Z(\zeta)} \right] \quad (13)$$

$$\text{where } R^{x,0} \equiv \frac{\left[\left(1 - \frac{\omega_p^2}{\omega_c^2}\right) \left(1 - \frac{\omega_p^2}{2\omega_c^2} - N^2 \cos^2 \theta\right) - N^2 \sin^2 \theta \right]^2}{\sqrt{a^2 + b^2}} \left(\frac{\omega_c}{\omega_p}\right)^2 |\cos \theta|$$

$$a^2 \equiv \left[\left(1 - \frac{\omega_p^2}{2\omega_c^2} - N^2 \sin^2 \theta\right)^2 + \left(1 - \frac{\omega_p^2}{2\omega_c^2}\right) N^2 \cos^2 \theta \right]^2 \sin^2 \theta$$

$$b^2 \equiv \left(1 - \frac{\omega_p^2}{2\omega_c^2} - N^2 \sin^2 \theta\right)^2 \left[2\left(1 - \frac{\omega_p^2}{2\omega_c^2}\right) - N^2 \sin^2 \theta \right]^2 \cos^2 \theta$$

and $Z(\zeta)$ is the familiar plasma dispersion function⁽⁴⁾. Note that $R^{x,0}$, and hence $\alpha^{x,0}$, depend inversely on the density n via the factor $(\omega_c/\omega_p)^2$.

* Quasi-perpendicular propagation, low density, $\omega \approx \omega_c$

$$\alpha^0 = \frac{1}{2} \left(1 - \frac{\omega_p^2}{\omega_c^2}\right)^{1/2} \frac{\omega_p^2}{\omega_c^2} \frac{\omega_c}{c} \left[-F_{7/2}''(z) \right] \quad (14)$$

$$\alpha^x = \frac{\omega_p^2}{\omega_c^2} \left(\frac{c}{v_{th}}\right)^2 \frac{\omega_c}{c} \left[-\frac{1}{2} F_{5/2}''(z) \right] \quad (15)$$

* Quasi-perpendicular propagation, high density, $\omega \approx \omega_c$

$$\alpha^0 = \frac{1}{\sqrt{2}} \left(1 - \frac{\omega_p^2}{\omega_c^2}\right)^{1/2} \frac{\omega_p^2}{\omega_c^2} \frac{\omega_c}{c} \frac{-F''_{7/2}(z)}{|G_{7/2}| \sqrt{G'_{7/2} + |G_{7/2}|}} \quad (16)$$

$$\alpha^x = \sqrt{2} \left(1 - \frac{\omega_p^2}{2\omega_c^2}\right)^{3/2} \left(\frac{\omega_c}{\omega_p}\right)^2 \left(\frac{v_{th}}{c}\right)^2 \frac{\omega_c}{c} \frac{-F''_{5/2}(z)}{|F_{5/2}(z)|^2} \cdot \quad (17)$$

The function $F_q(z)$ is the relativistic counterpart of the plasma dispersion function and is defined by

$$F_q(z) = F'_q(z) + iF''_q(z) = -i \int_0^\infty \frac{e^{izt}}{(1 - it)^q} dt \quad (18)$$

with $z \equiv \frac{c^2}{v_{th}^2} \frac{\omega - \omega_c}{\omega_c}$. $G_q(z)$ merely is a shorthand for $1 + \frac{1}{2} \frac{\omega_p^2}{\omega_c^2} F_q(z)$. We

note the $1/n$ dependence of α^x . The ordinary mode does not show this anomalous behavior, and Eq. 14 simply is the low-density limit of Eq. 16.

A word of caution should be added here. While these expressions treat relativistic effects correctly, they are derived by means of an expansion in v_{th}/c , a technique which is clearly questionable in the 100 keV electron temperature range. Although work is under way, as yet no reliable formulae are available that treat the very strong angular dependence correctly in the very high temperature regime. We expect that the estimates presented are on the low (i.e. pessimistic) side, because the spectral width of the absorption band increases with temperature and, hence, overlapping of various harmonics will increase the absorption at any given frequency.

V-B-3. Plasma and Magnetic Field Models

The magnetic field in the end plugs is described by a model due to Kaiser⁽⁵⁾ who treats the field produced by the yin-yang coils as the sum of a mirror field and a quadrupole field. Finite-beta effects are not taken into account.

The field variation along the longitudinal axis (z-coordinate) is described by

$$f(z) = \frac{1}{2} B_p \left(1 + \frac{\gamma}{4}\right) \left[\frac{1}{1 + k_m^2 \left(z + \frac{1}{2} \ell_p\right)^2} + \frac{1}{1 + k_m^2 \left(z - \frac{1}{2} \ell_p\right)^2} \right] . \quad (1)$$

Here ℓ_p is the distance between the mirror throats,

$\gamma \equiv 4 R_{VAC} - 3 + \sqrt{16 R_{VAC} (R_{VAC} - 1) + 1}$ is a function of the vacuum mirror ratio R_{VAC} and k_m is a decay parameter in the z-direction. Because B_p represents the magnetic induction in the center of the yin-yang, $f(0) = B_p$ and it is required that

$$k_m = \ell_p / \sqrt{\gamma} . \quad (2)$$

An additional parameter k_p is required to describe the ellipticity of the B-profile in the x,y-plane:

$$\begin{aligned} B_x &= (-k_p f(z) - \frac{1}{2} \frac{\partial f}{\partial z}) x \\ B_y &= -(-k_p f(z) + \frac{1}{2} \frac{\partial f}{\partial z}) y \\ B_z &= f(z) . \end{aligned} \quad (3)$$

This is a paraxial model where $B_x(x)$ and $B_y(y)$ are linearized functions around $(x,y) = 0$ and is valid only within a radius which is small with respect to the length of the yin-yang field, ℓ_p .

A good analytic description of the magnetic field in the barrier - which is essentially a transition region between a solenoidal and a yin-yang geometry - is not readily available. While future plans call for a numerical calculation of the barrier field using a modified EFFI-code, a simple model based on a quadratic regression along the z-axis has been used in this study:

$$\begin{aligned}
B_x &= (-.1375 z + 16.5) x \\
B_y &= (-.1477 z + 17.4) y \\
B_z &= .148 z^2 - 36.1 z + 1.68 \times 10^4
\end{aligned} \tag{4}$$

with x, y, z in cm and B in Gauss. The ellipticity parameter is zero in this case. The origin is chosen at the minimum of the magnetic well and the model holds for $-120 \text{ cm} < z < +80 \text{ cm}$.

For either heating zone - end plug and barrier - the density in the (x,y) -plane is modelled as a function of the axial magnetic flux ψ , assuming the ψ -dependence to be exponential. Modelling the variation along the z -axis as a Gaussian, one eventually finds:

$$n(x,y,z) = n_0(0) \exp \left\{ -\left[\frac{B}{B_p} \left(\frac{x^2}{a^2} e^{2k_p z} + \frac{y^2}{a^2} e^{-2k_p z} \right) + \frac{z^2}{\ell^2} \right] \right\} \tag{5}$$

a and ℓ being scale lengths in radial and axial directions, respectively. For our initial modelling purposes the electron temperature is assumed constant and Maxwellian over the plasma volume under consideration.

V-B-4. Ray Trajectories and Absorption

A numerical code has been developed which traces the ray in either plug or barrier geometry starting from a prescribed launching position and scanning a given range of launching angles. The theory outlined in section V-B-2 is easily adaptable to a numerical approach to solve the ray equations and calculate the optical depth τ by integrating the absorptivity α along the ray. The cumulative power absorbed up to a given point along the ray is then given by

$$P_A(x,y,z) = P_0 e^{-\int s(x,y,z) \alpha ds'} = P_0 e^{-\tau(x,y,z)} \tag{1}$$

where P_0 is the power launched into the plasma and ds is a line element along the ray.

The code has a graphical output in which all the relevant resonance and cut-off surfaces are represented. Coordinates are in cm, and the numbers (1 through 7) along the ray trajectories indicate the points where the incoming power has been absorbed for (1) 25%, (2) 50%, (3) 75%, (4) 90%, (5) 95%, (6) 99%, and (7) 99.9%, respectively. Table V-B-1 summarizes the relevant plasma parameters.

V-B-4-a. Heating of the Plug Electrons

The limited accessibility of the interior of the yin-yang coils restricts the launching position to $x = 0$, $y = \pm 80$ cm, $z = -30$ cm, and $x = \pm 80$ cm, $y = 0$, $z = 30$ cm, the origin being at the minimum of the yin-yang field. These positions are symmetric as far as the plasma properties are concerned, and we need to examine only one of them. The range of possible launching directions is equally restricted, extending from 10° to 25° with respect to a plane normal to the axial direction.

Figures V-B-2 through V-B-4 show ray trajectories and absorption patterns for the extraordinary mode (i.e. the mode which is usually labeled as such when it propagates in a direction perpendicular to the local magnetic field) at frequencies of 108 GHz, 112 GHz, and 120 GHz. For the two lower frequencies no resonance or cut-off surfaces are present in the picture and the ray trajectories are fairly uncomplicated, with the 112 GHz signal showing the most promising absorption characteristics. The asymmetry of the absorption pattern is explained by the strong dependence of the absorptivity on the angle θ included between the wave vector $\vec{k}$ and the local static magnetic field $\vec{B}$; to illustrate this field lines have been drawn in Fig. V-B-3.

In Fig. V-B-4 (i.e. at 120 GHz) the cyclotron resonance, the plasma resonance and the RH-branch of the cyclotron cut-off surfaces are all present, dividing the plasma volume in CMA-areas #1, 3, 5, and 7⁽⁶⁾. This situation is very unfavorable because area #3 is an evanescent zone for the extraordinary mode, and a ray launched from (80, 0, -30) cm in the prescribed direction can barely propagate. At even higher frequencies the plasma appears very tenuous and rays will propagate in almost straight lines without any appreciable absorption.

The ordinary mode is much less affected by the plasma properties and propagates along almost straight trajectories. Figures V-B-5 through V-B-7 show rays and absorption patterns for the same frequencies, 108 GHz, 112 GHz,

Table V-B-1
ECRH Parameters

	<u>Plug</u>	<u>Barrier</u>
Electron temperature, T_e	123 keV	270 keV
Peak density (in center), n_0	$2.7 \times 10^{13} \text{ cm}^{-3}$	$7 \times 10^{12} \text{ cm}^{-3}$
Density decay length		
in axial direction, ℓ_z	300 cm	500 cm
in radial direction, ℓ_r	40 cm	60 cm
Plasma radius in center, r	77 cm	108 cm
Magnetic field parameter, k_p	$.0041 \text{ cm}^{-1}$	0 cm^{-1}
γ	7.15	---
ℓ_p	560 cm	---
B_p	4 T	1.4 T
Launching position $\bar{r}_0$	(80, 0, -30) cm	(± 80 , 0, -100) cm
Launching angles ϕ_0	10° , 25° (5°)	50° , 80° (10°)

Extraordinary Mode
Frequency: 108 GHz
Density at origin: $2.7 \times 10^{13} \text{ cm}^{-3}$
B field at origin: $4 \times 10^4 \text{ Gauss}$
Electron temperature: 120 keV
Incidence: from -25° to -10°

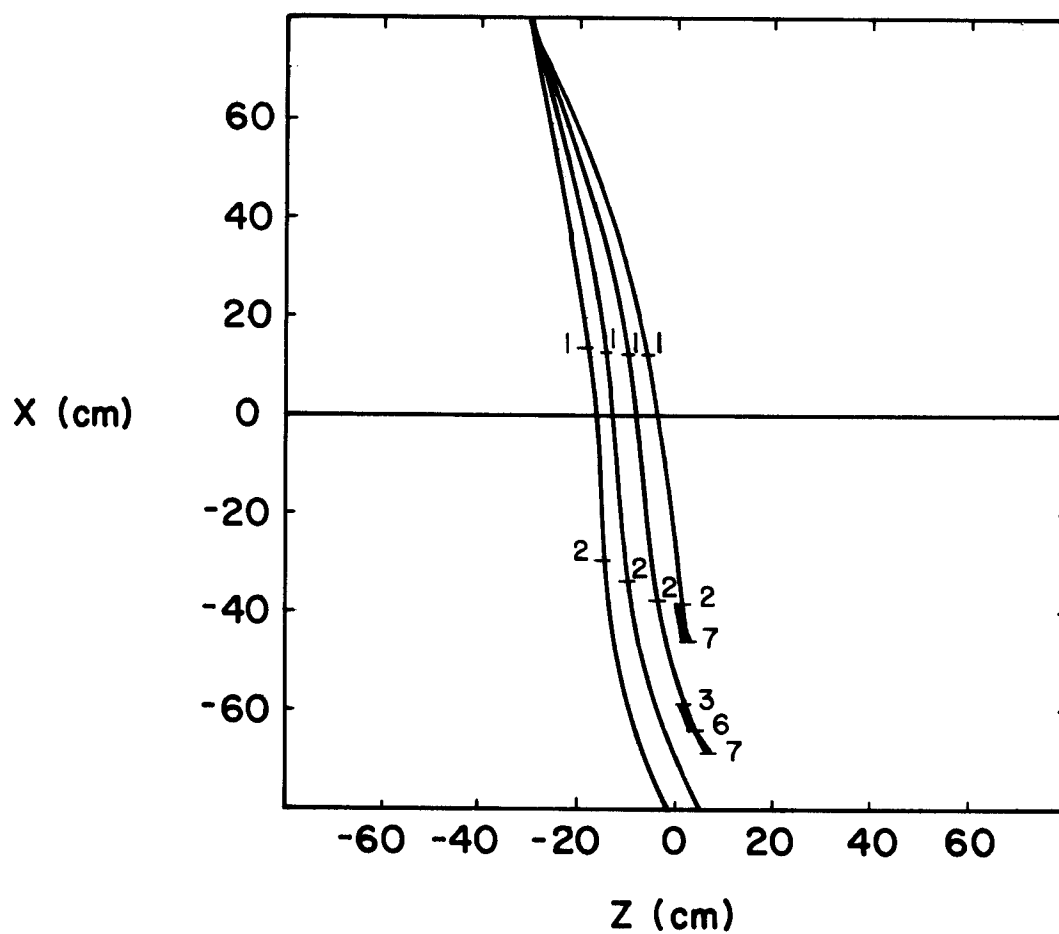


Fig. V-B-2

Extraordinary Mode
Frequency: 112 GHz
Density at origin: $2.7 \times 10^{13} \text{ cm}^{-3}$
B field at origin: $4 \times 10^4 \text{ Gauss}$
Electron Temperature: 120 keV
Incidence: from -25° to -10°

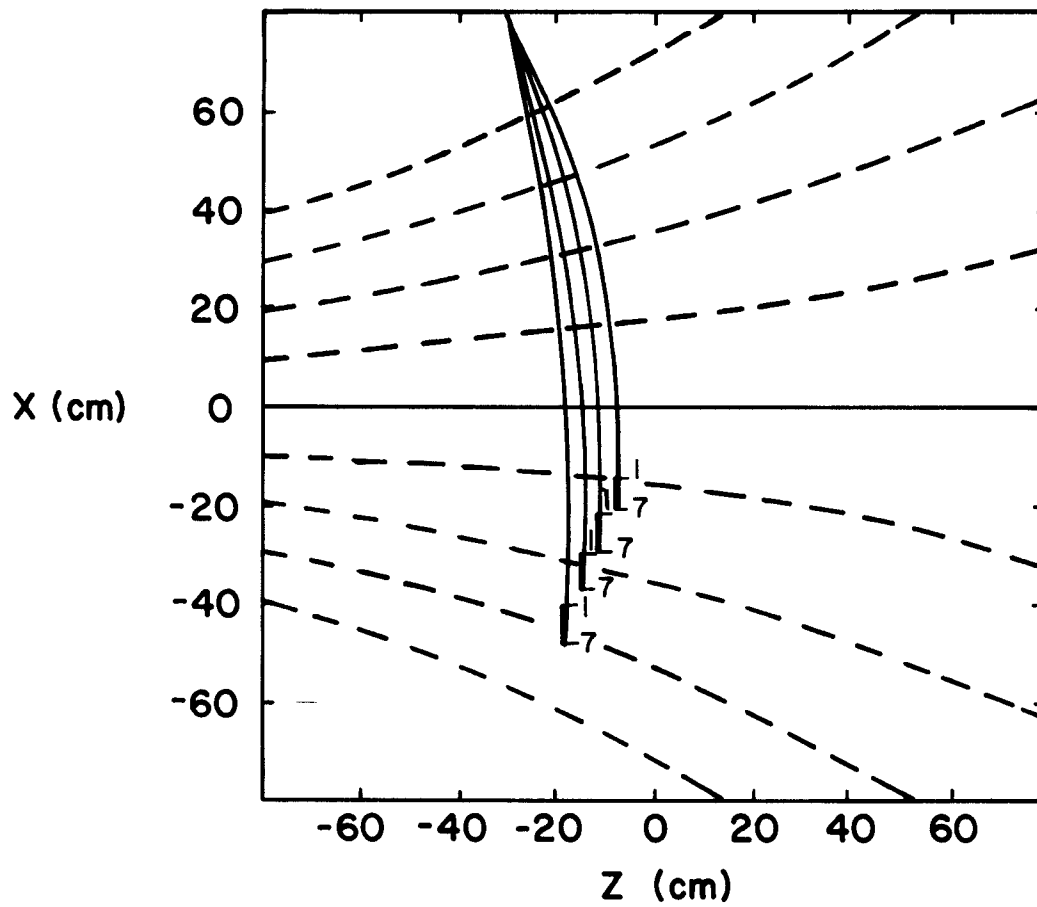


Fig. V-B-3

Extraordinary Mode
 Frequency: 120 GHz
 Density at origin: $2.7 \times 10^{13} \text{ cm}^{-3}$
 B field at origin: $4 \times 10^4 \text{ Gauss}$
 Electron temperature: 120 keV
 Incidence: from -25° to -10°

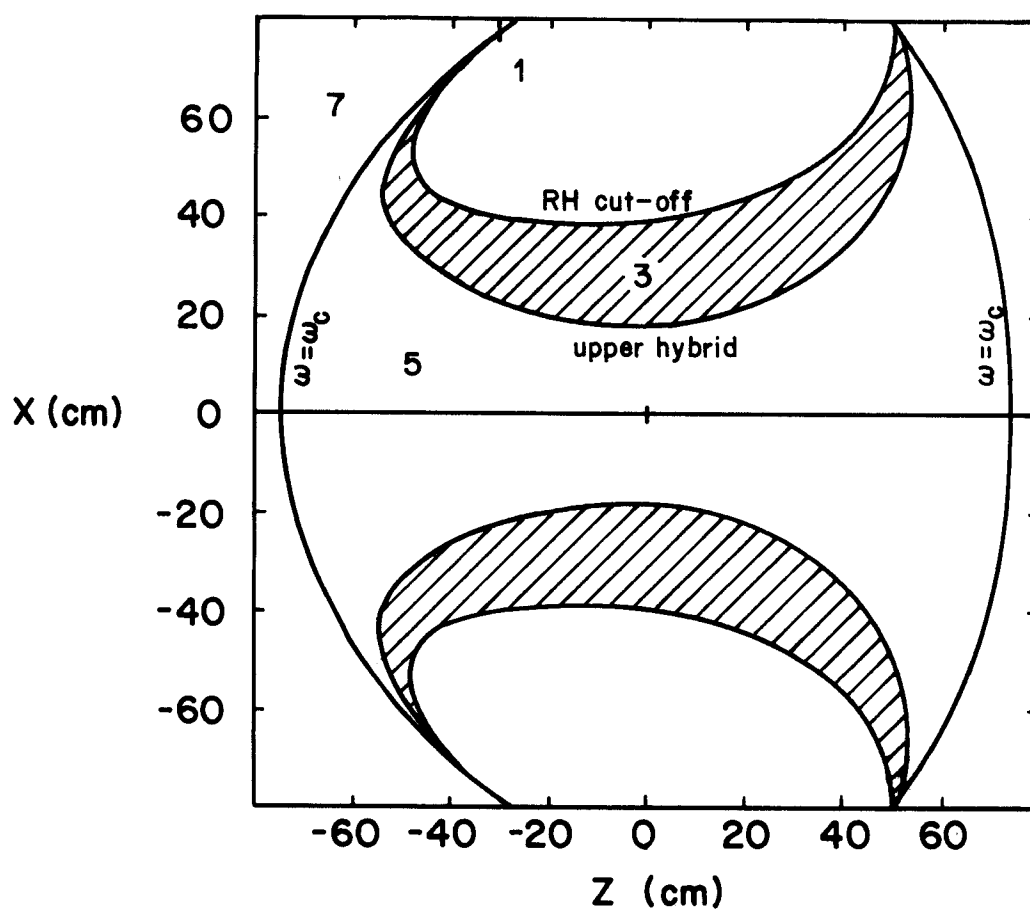


Fig. V-B-4

Ordinary Mode
Frequency: 108 GHz
Density at origin: $2.7 \times 10^{13} \text{ cm}^{-3}$
B field at origin: 4×10^4 Gauss
Electron temperature: 120 keV
Incidence: from -25° to -10°

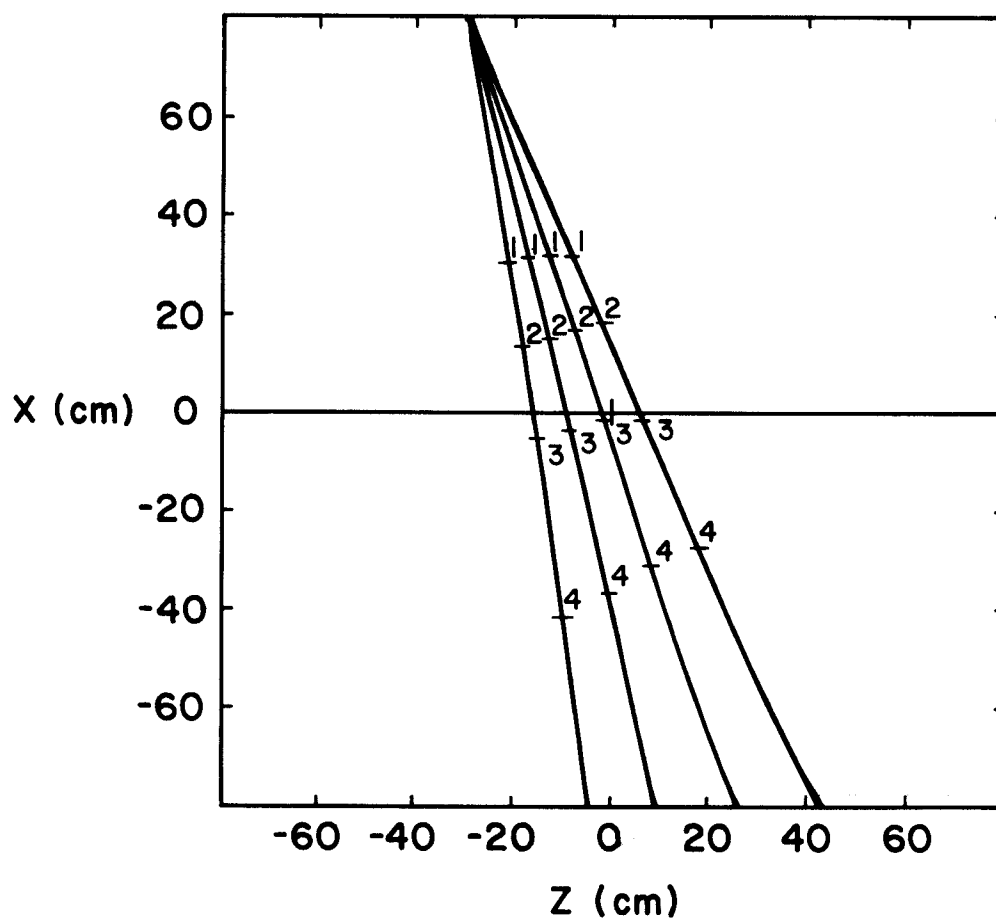


Fig. V-B-5

Ordinary Mode
Frequency: 112 GHz
Density at origin: $2.7 \times 10^{13} \text{ cm}^{-3}$
B field at origin: 4×10^4 Gauss
Electron temperature: 120 keV
Incidence: from -25° to -10°

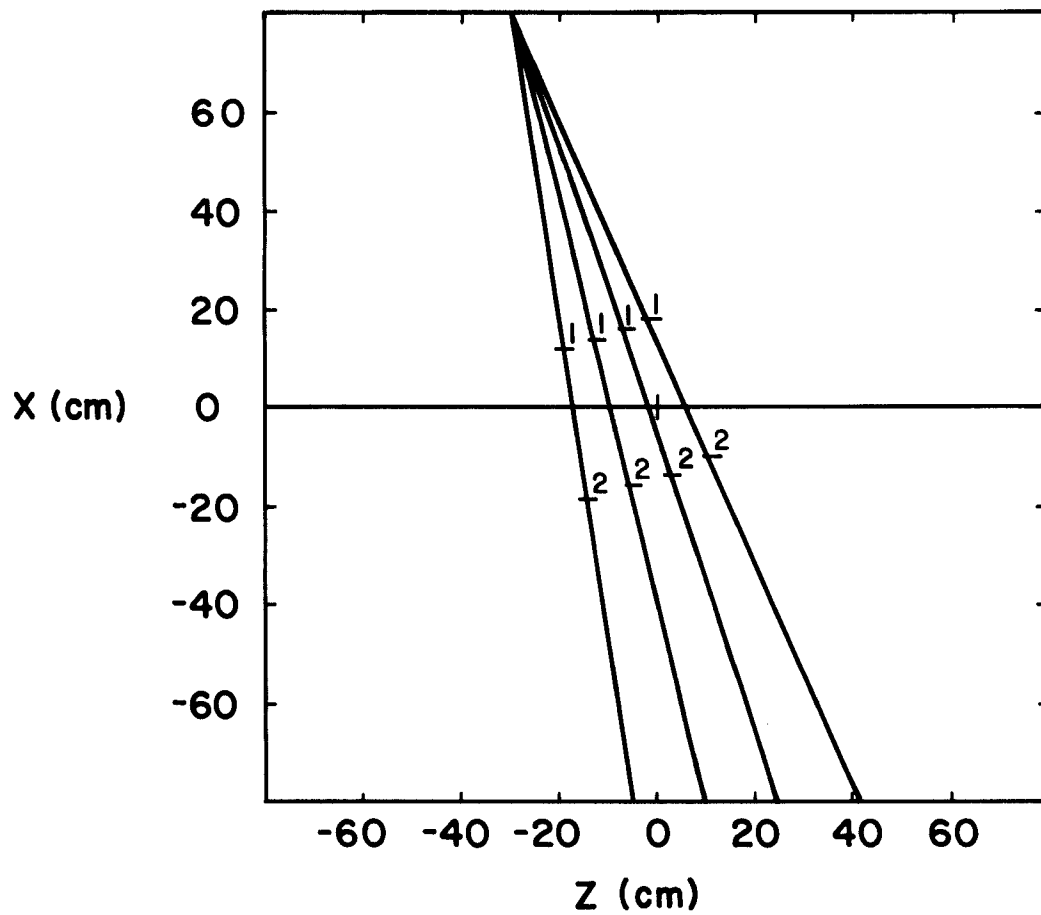


Fig. V-B-6

Ordinary Mode

Frequency: 120 GHz

Density at origin: $2.7 \times 10^{13} \text{ cm}^{-3}$

B field at origin: $4 \times 10^4 \text{ Gauss}$

Electron temperature: 120 keV

Incidence: from -25° to -10°

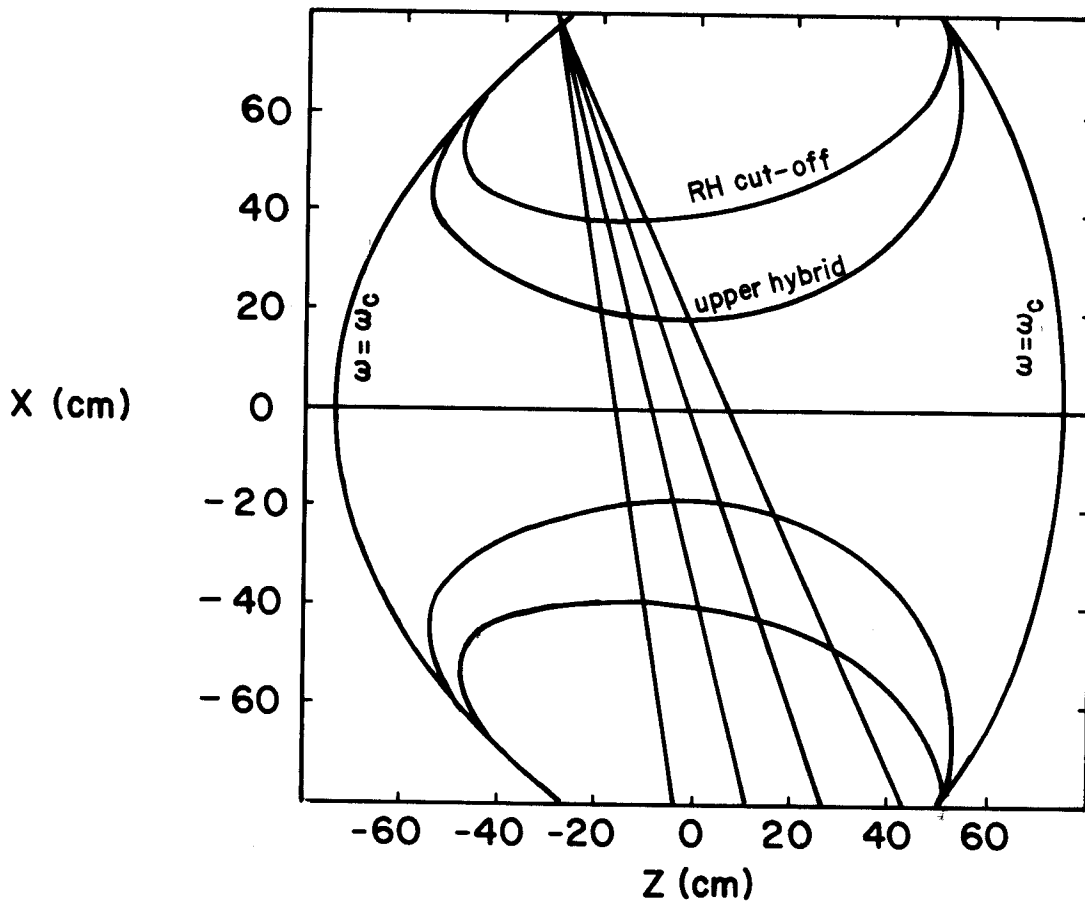


Fig. V-B-7

and 120 GHz. The wave energy is about 90% absorbed at the lowest frequency, but the plasma becomes optically thin very rapidly with increasing frequency.

V-B-4-b. Heating of the Barrier Electrons

In this case there are no serious problems related to launching ECRH power within magnet constraints. We launch from a position (± 80 , 0, -100) cm and examine directions at angles from 50° to 80° with respect to the normal plane. Figures V-B-8 and V-B-9 show that the extraordinary mode is well absorbed at frequencies in the 38-42 GHz range. At 46 GHz, however (Fig. V-B-10), the picture is complicated by the presence of the cyclotron resonance, plasma resonance, and cyclotron cut-off layers, and the bulk of the plasma becomes inaccessible for the extraordinary mode. The ordinary mode has no such propagation problems, but is poorly absorbed at the low electron densities within the barrier (Figs. V-B-11 through V-B-13).

In all cases the plasma exhibits a wide resonant area centered about the $\omega = \omega_c$ surface. For this reason it is possible to heat at a frequency somewhat below the resonance frequency at the position where the absorption is required. This technique also ensures the best possible absorption of the ordinary mode (although the extraordinary mode is still preferred) and allows avoidance of propagation problems related to the presence of resonance and cut-off surfaces.

V-B-5. ECRH Transport System

Heating requirements dictate that power levels of the order of 8 to 16 MW be delivered to the plasma at microwave frequencies in the 40 to 120 GHz range. The most probable source of such powers at this time is the gyrotron. The gyrotron tube uses cyclotron resonance as the coupling means between an electron beam and the electromagnetic fields. Various modes are in use or being considered in the microwave structure of such tubes. Circular electric mode resonators operating in the TE_{011} or TE_{021} modes coupling to hollow cylindrical electron beams are typically used, however higher order TE_{m11} or "whispering gallery" modes are now under investigation.⁽⁷⁾ To date the highest gain has been achieved with the fundamental circular waveguide mode, TE_{11} .⁽⁸⁾ Because circuit dimensions are typically ten times greater than those of conventional type tubes the power output of gyrotrons is at least two orders of magnitude greater.

Microwave output in any of the above higher TE modes presents a problem for transmission systems utilizing circular waveguides because the dominant

Extraordinary Mode
Frequency: 38 GHz
Density at origin: $6.7 \times 10^{12} \text{ cm}^{-3}$
B field at origin: $1.5 \times 10^4 \text{ Gauss}$
Electron temperature: 270 keV
Incidence: from 100. to 140.°

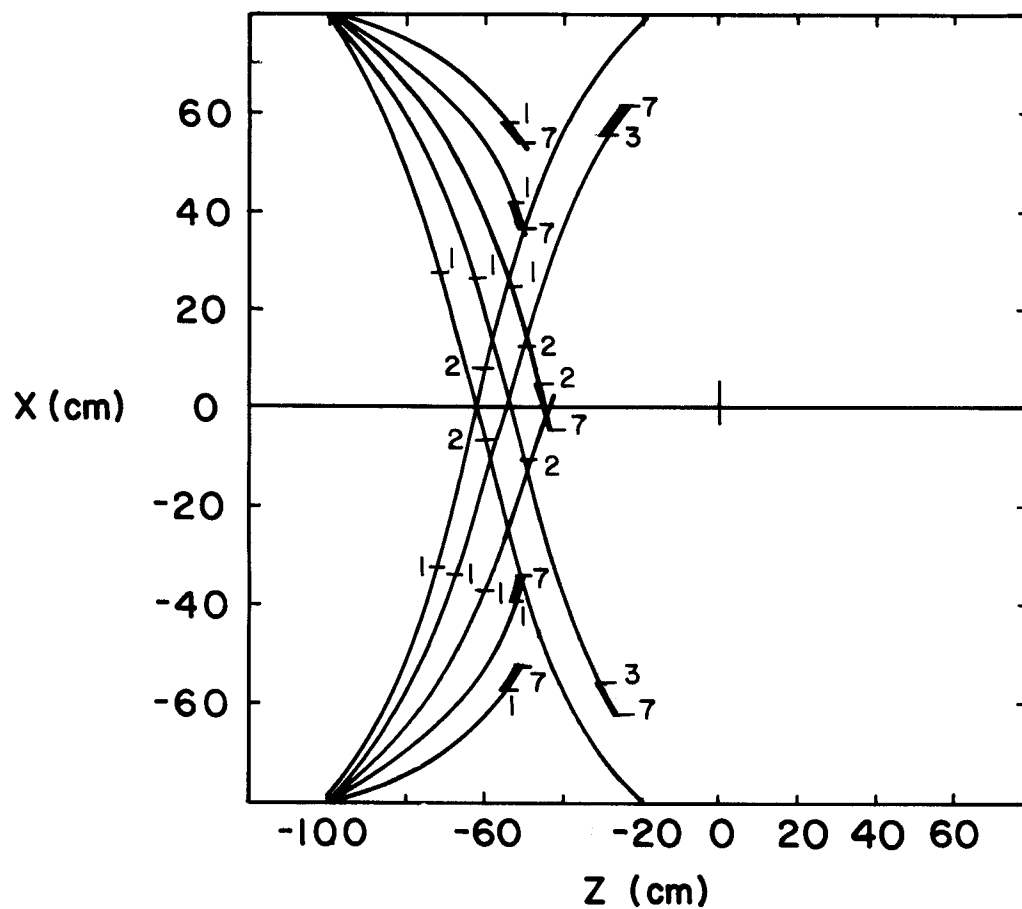


Fig. V-B-8

Extraordinary Mode

Frequency: 42 GHz

Density at origin: $6.7 \times 10^{12} \text{ cm}^{-3}$

B field at origin: $1.5 \times 10^4 \text{ Gauss}$

Electron temperature: 270 keV

Incidence: from 100° to 140°

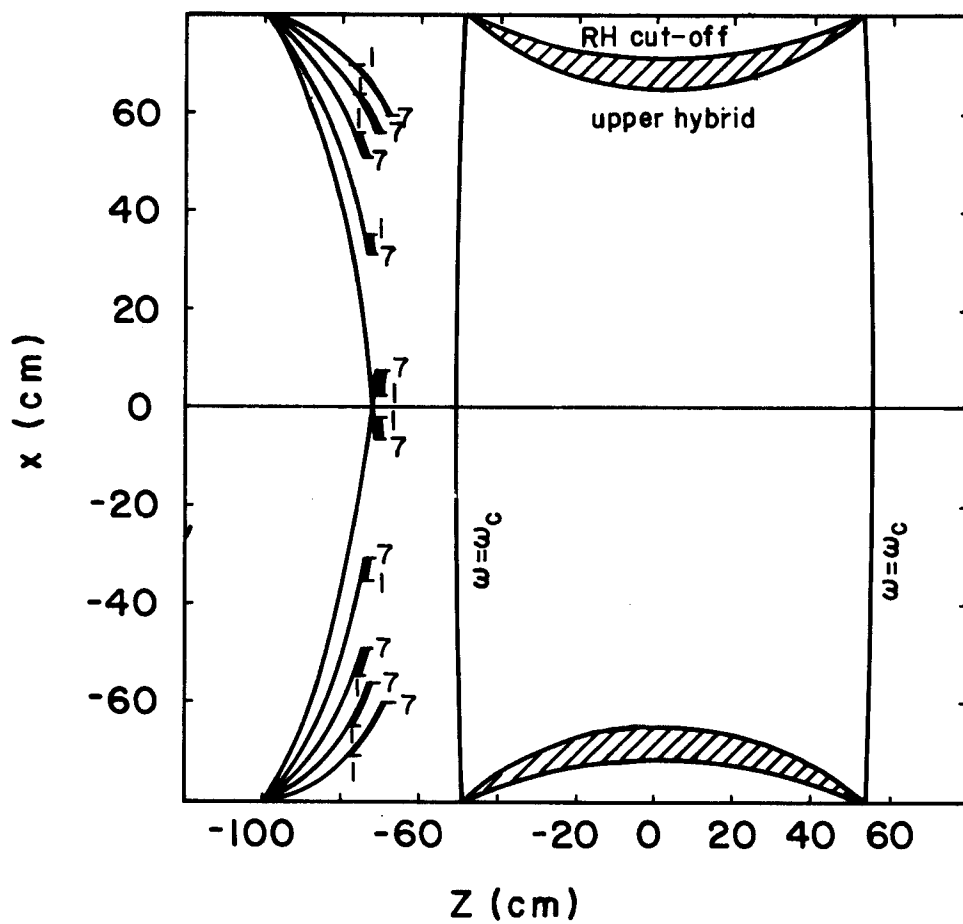


Fig. V-B-9

Extraordinary Mode

Frequency: 46 GHz

Density at origin: $6.7 \times 10^{12} \text{ cm}^{-3}$

B field at origin: $1.5 \times 10^4 \text{ Gauss}$

Electron temperature: 270 keV

Incidence: from 100. to 140.°

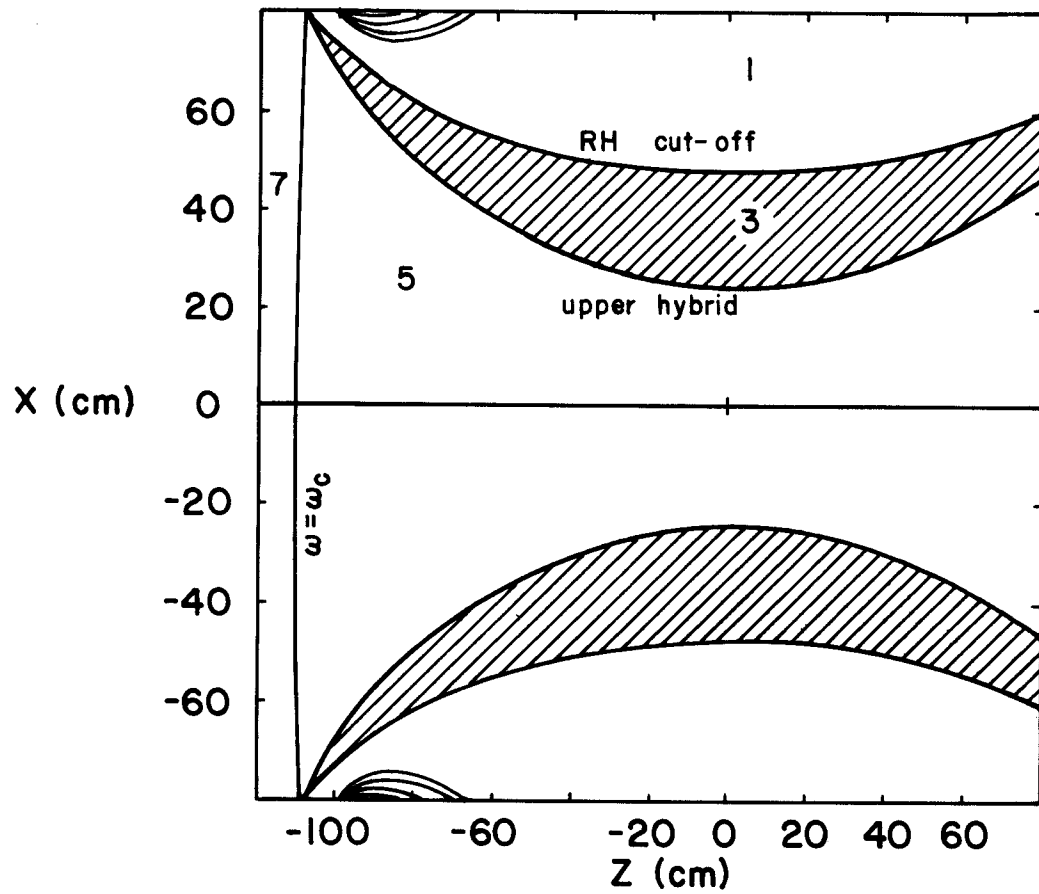


Fig. V-B-10

Ordinary Mode
Frequency: 38 GHz
Density at origin: $6.8 \times 10^{12} \text{ cm}^{-3}$
B field at origin: $1.5 \times 10^4 \text{ Gauss}$
Electron temperature: 270 keV
Incidence: from 100. to 140.°

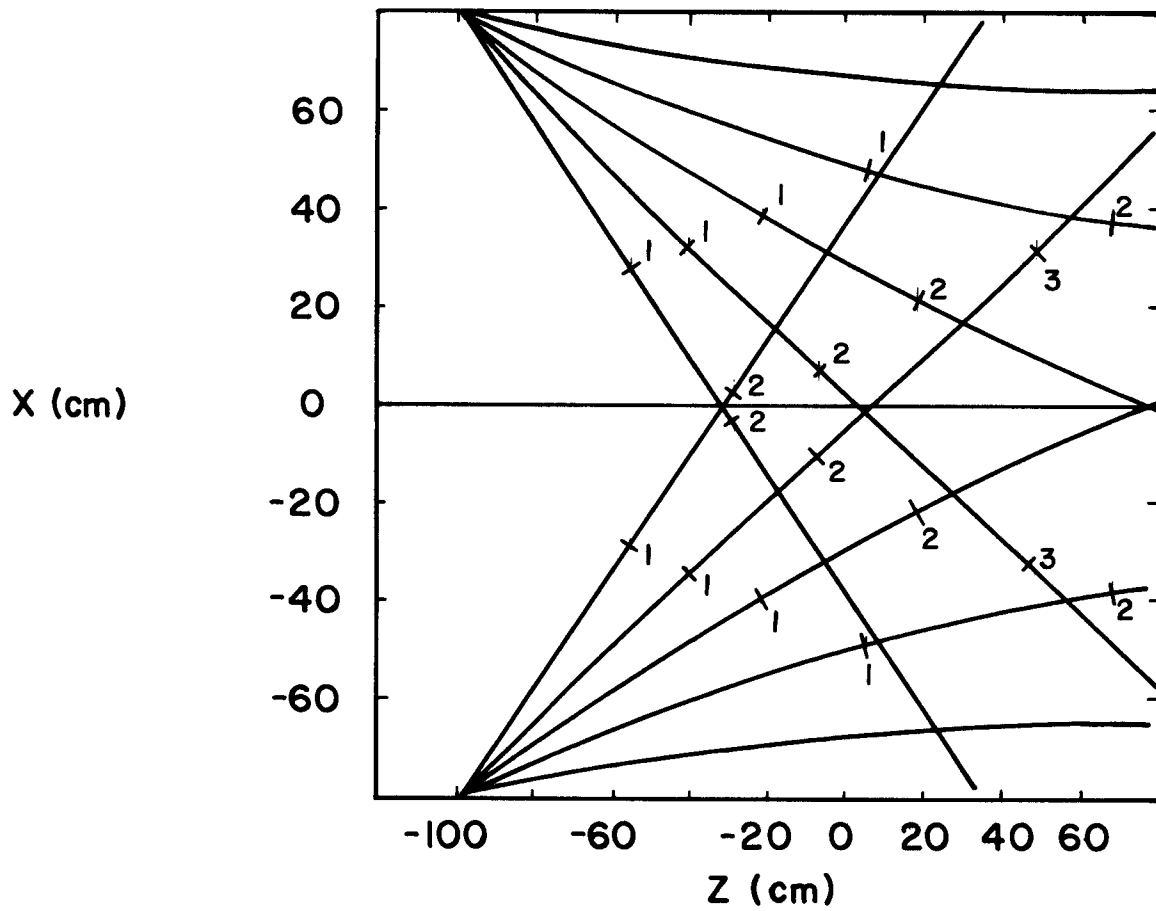


Fig. V-B-11

Ordinary Mode

Frequency: 42 GHz

Density at origin: $6.7 \times 10^{12} \text{ cm}^{-3}$

B field at origin: $1.5 \times 10^4 \text{ Gauss}$

Electron temperature: 270 keV

Incidence: from 100. to 140.°

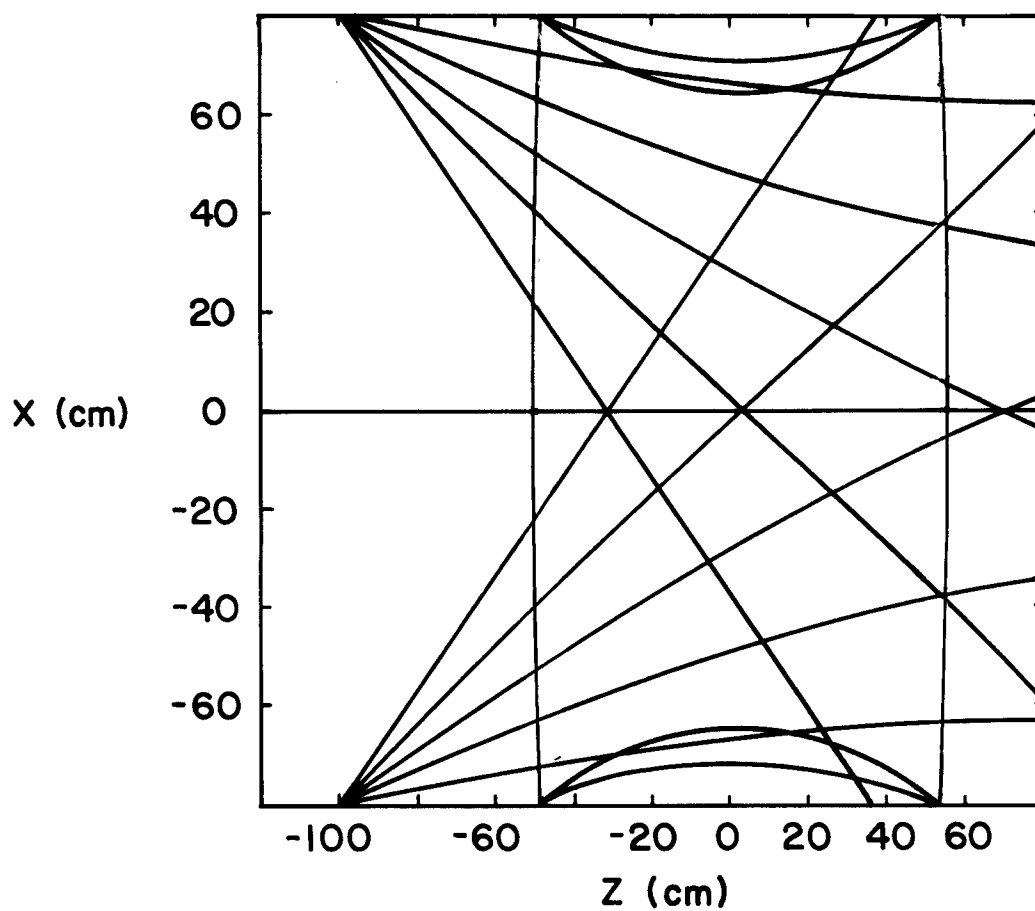


Fig. V-B-12

Ordinary Mode

Frequency: 46 GHz

Density at origin: $6.7 \times 10^{12} \text{ cm}^{-3}$ B field at origin: $1.5 \times 10^4 \text{ Gauss}$

Electron temperature: 270 keV

Incidence: from 100. to 140.°

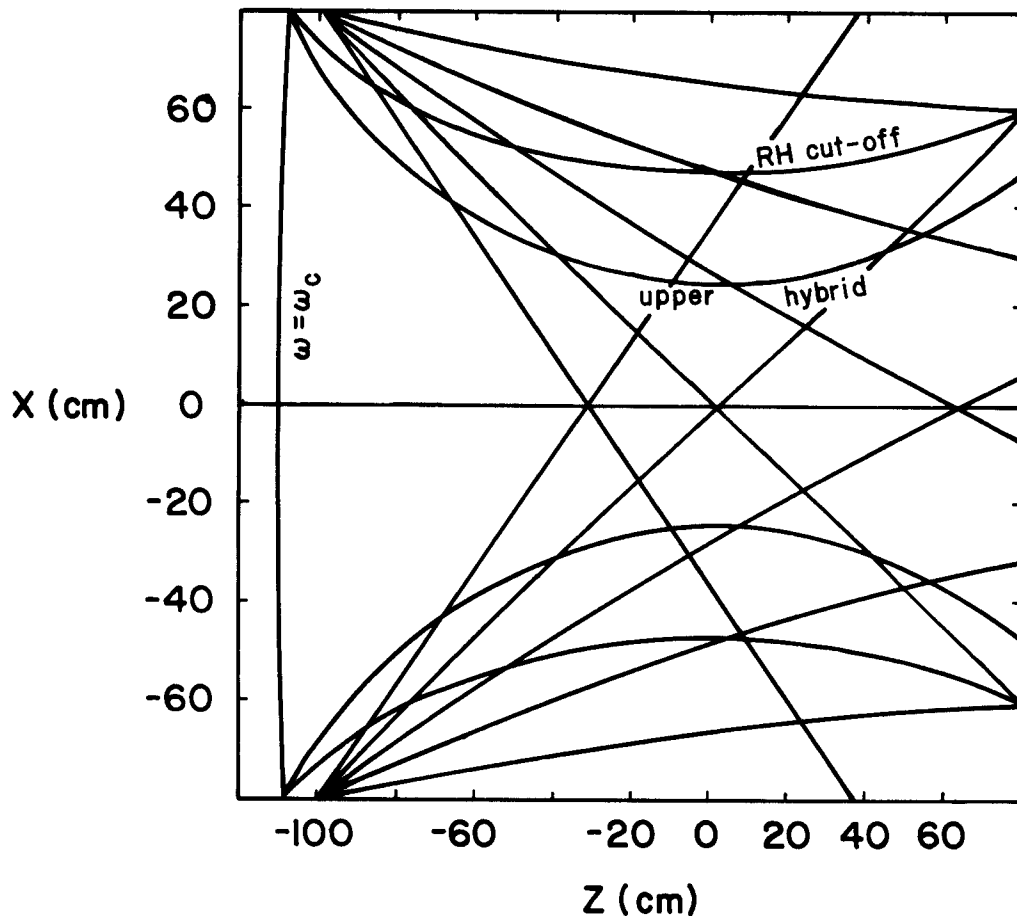


Fig. V-B-13

mode is TE_{11} . The use of the low loss TE_{01} mode, for example, requires expensive specially machined waveguides, and coupling structures which prevent mode conversion pose a very difficult problem.

V-B-5-a. The Beam Waveguide

A low loss open waveguide system has been proposed by Goubau and Schwering⁽⁹⁾ and appears to have application to this problem. Beam modes are characterized by a radial amplitude distribution which is the product of a Gaussian and an associated Laguerre polynomial. Such modes will be guided with extremely small attenuation when regular phase correction is provided either in the form of dielectric "lenses" or parabolic mirrors. The lowest loss mode distribution function is given by:

$$(E_{\text{trans}})_{00} = A_{00} L_0^0 \left(\frac{a^2 \rho^2}{R^2} \right) e^{-\frac{a^2 \rho^2}{R^2}} \begin{Bmatrix} \sin \phi \\ \cos \phi \end{Bmatrix} \quad (1)$$

and is shown in Fig. V-B-14(a), while the next higher loss mode is given by:

$$|E_{\phi}|_{01} = A_{01} L_0^1 \left(\frac{a^2 \rho^2}{R^2} \right) e^{-\frac{a^2 \rho^2}{2R^2}} \quad (2)$$

where

$$a = \sqrt{\frac{k_0}{2z_0}} R$$

(R being the guide radius and $(2z_0)$ the iteration distance) as depicted in Fig. V-B-14(b). The mode given by Eq. 1, designated TEM_{00} , is linearly polarized while the TEM_{01} of Eq. 2 is circular electric (i.e., ϕ independent, not circularly polarized). Iteration loss, i.e., loss per lens spacing is given in Fig. V-B-15 as a function of the parameter a , which is related to the Fresnel number. It is evident that for a given frequency it is possible to design the lens spacing and radius for any desired diffraction (iteration) loss.

Since wall losses are eliminated and the electromagnetic energy is concentrated near the axis such open waveguides are ideal for application to

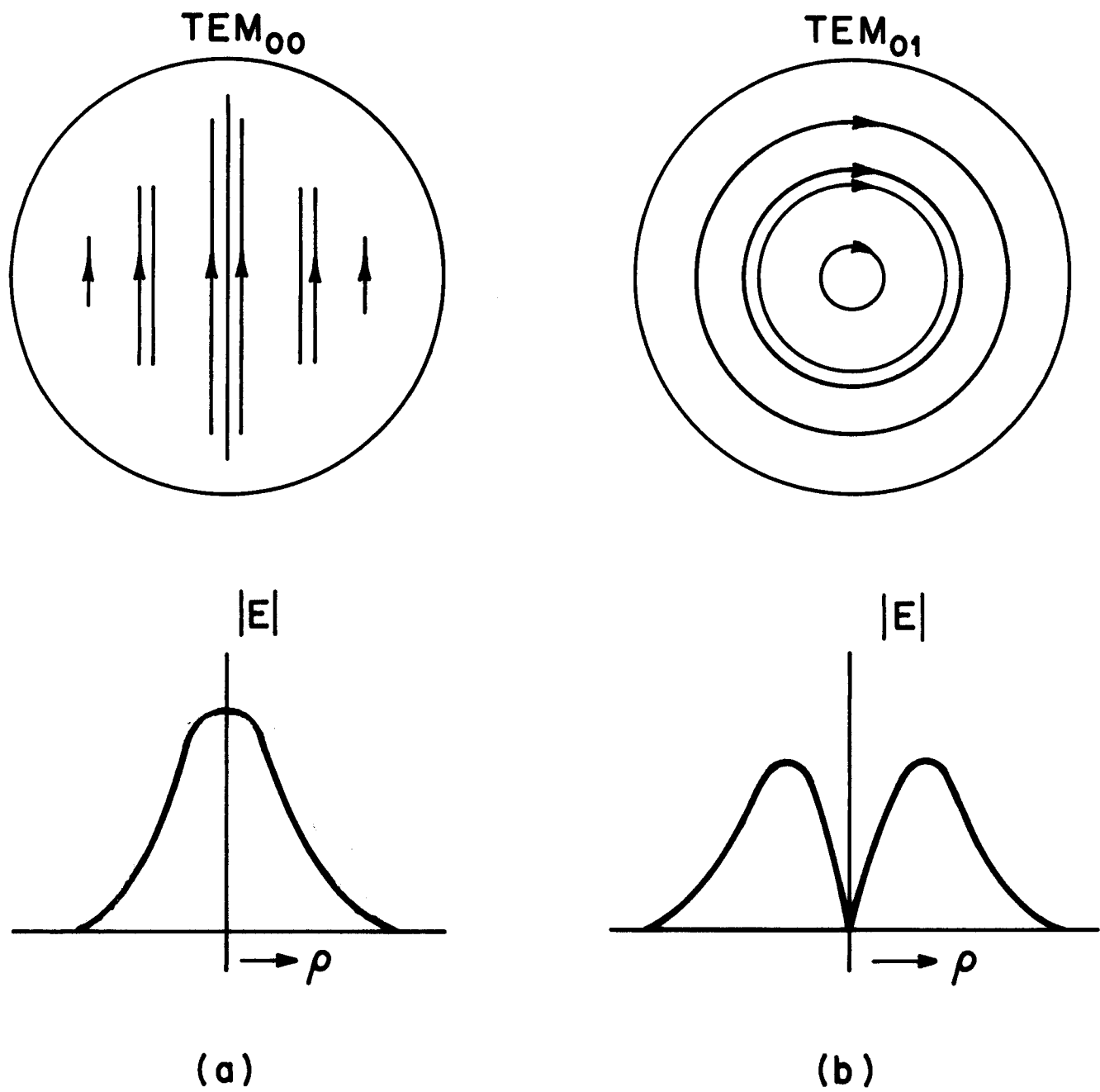


FIG. V-B-14

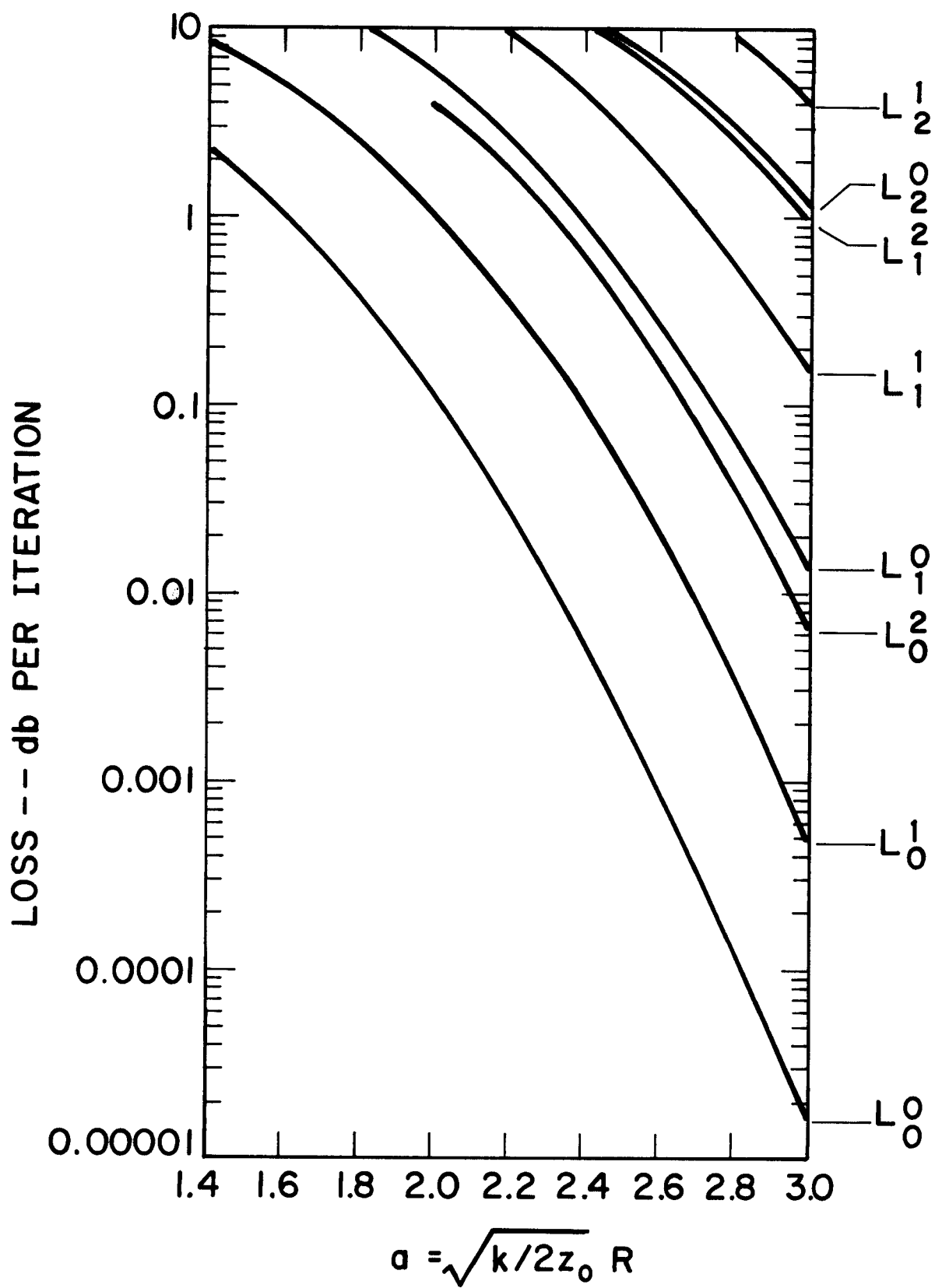


Fig. V-B-15. Iteration Loss for Confocal Modes

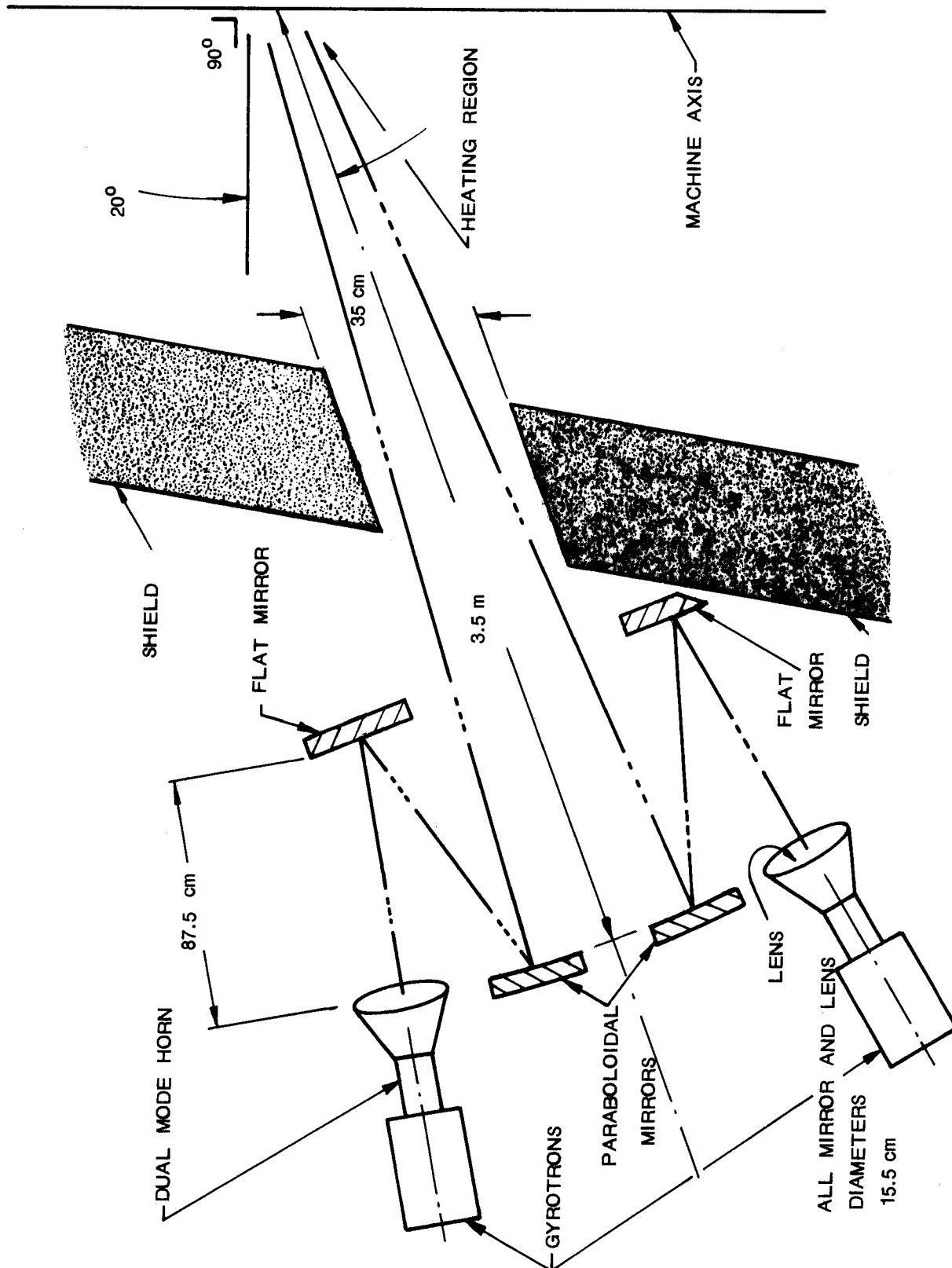
the high power transport system for plasma heating. In addition, energy propagating in the waveguide can be fed directly to the plasma without need for any launching device. Such a system is ideally suited for the tandem mirror reactor when the waveguide axis is oriented at the optimum angle to the axial magnetic field, and the fundamental or linearly polarized mode given by Eq. (1) is utilized. Local regions of excitation in the plasma see a plane wave and r.f. absorption is essentially complete.

While there is no need for a launcher from the waveguide into the plasma there is however a need for a transducer between the gyrotron and the waveguide. Since gyrotrons do not operate in the TEM_{00} beam waveguide mode, provision must be made to convert the gyrotron output to the TEM_{00} waveguide mode. This can be done with high efficiency when a gyrotron operating in the TE_{11} circular waveguide mode is used. Such a transducer takes the form of a dual mode horn and has been shown experimentally to be a highly effective beam waveguide launcher.⁽¹⁰⁾

When the gyrotron operates in other than the TE_{11} mode, mode transducers to the TE_{11} mode must first be employed followed by the dual mode horn. Another interesting possibility afforded by the beam waveguide system exists. Although it has not yet been studied in detail it appears feasible to heat the plasma directly with circular electric polarization (note: not circular polarization). In that case a gyrotron operating in the TE_{01} mode could have its output converted directly to the TEM_{01} beam waveguide mode of Fig. V-B-14(b) (Eq. 2) and fed directly to the plasma without mode conversion to linear polarization. The waveguide dimensions would have to be somewhat increased for the same loss as evidenced from Fig. V-B-15 wherein the L_0^1 curve must now be used instead of the L_0^0 curve.

V-B-5-b. System Design - Plug

The proposed system for plug heating, which requires 8 MW at 110 GHz, is shown in Fig. V-B-16. Since projected power levels for gyrotrons appear to be of the order of 1 MW, eight such gyrotron sources are employed. Each gyrotron feeds a dual mode horn which in turn has a dielectric phase correcting "lens" covering its mouth. The lens' focal length is chosen to be 87.5 cm at which point a flat reflecting mirror reflects the beam mode back towards a paraboloidal mirror. The total path just described constitutes the first iteration for the beam waveguide. The radius of the horn and lens is 7.8 cm resulting in, from Eq. (1), an "a" value of



GYROTRON TRANSPORT SYSTEM

Fig. V-B-16

$$a = \sqrt{\frac{k_0}{2z_0}} \quad R = \sqrt{\frac{2\pi(110 \times 10^9)}{3 \times 10^8 (1.75)}} \quad 0.078 = 2.83 \quad . \quad (3)$$

This yields (from Fig. V-B-15) a loss of 10^{-4} dB. (Here we assume no diffraction loss at the flat mirror placed at the focal point, since the beam narrows to a radius of 70% of $R^{(9)}$ and the mirror has an 8 cm radius.) Since the lens is shielded from neutron bombardment it can be cast from polystyrene or comparable material which has a dielectric constant of 2.5 and a loss tangent of 5×10^{-4} .

The second and final iteration of the beam mode results from reflection from the paraboloidal mirror. This mirror is placed so that it is at its focal length 1.75 m from the blanket. This results in maximum beam concentration, hence minimum hole diameter in the blanket. For this case the blanket hole will be 35 cm in diameter. The beam mode irradiates the plasma at another 1.75 m downstream where the phase front is again plane. At this point, as shown, the beams from all eight sources superimpose to a 15.6 cm diameter region in the plasma. The "a" value for the second iteration is given by

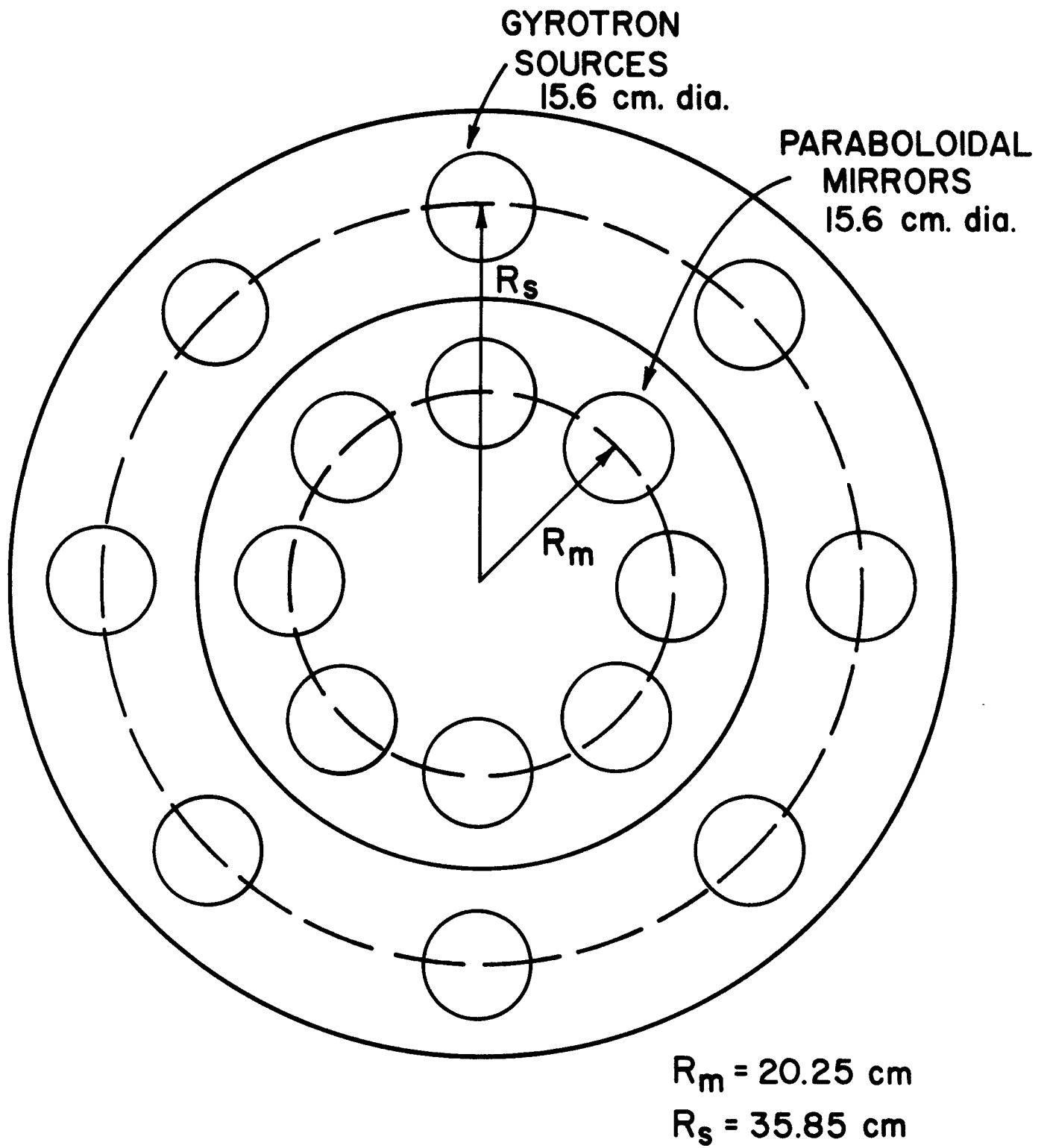
$$a = \sqrt{\frac{n_0}{2z_0}} \quad R = \sqrt{\frac{2\pi(100 \times 10^9)}{3 \times 10^8 (3.5)}} \times .078 = 2.0 \quad (4)$$

Again from Fig. V-B-15 the loss for this iteration is 0.1 dB.

A detailed drawing of the sources and paraboloidal mirrors as perceived looking down the axis of the delivery system, which makes an angle of 20° with the normal to the machine axis, is shown in Fig. V-B-17.

V-B-5-c. System Design - Barrier

Barrier heating requires 16 MW at 42 GHz and can be accomplished with two separate systems similar to the one just described. For this case, using the same equations as in the first case, we design for a paraboloidal mirror focal length of 1.5 m since the blanket is 1.5 m from the plasma at the barrier. For an "a" value of 2.0 (0.1 dB loss) we obtain a mirror radius of 11.6 cm at 42 GHz. The horn, to flat mirror, to paraboloidal mirror iteration path length is 1.5 m total and again the loss is 10^{-4} dB. The sources and

**FIG. V-B-17**

Gyrotron Beam Reflector System

paraboloidal mirrors are arranged as in Fig. V-B-17, however, in this case the mirror and source diameter are, of course, 23.2 cm and $R_m = 30.3$ cm while $R_s = 53.5$ cm. The axis of each delivery system makes an angle of 50° with the normal to the machine axis.

V-B-5-d. Power Considerations

Integrating the power density in the beam, using Eq. (1) over the beam cross section yields the maximum electric field intensity for a given power level. The result is

$$E_{\text{max peak}}^2 = \frac{2z_0 P_{\text{AVG}}}{\pi R^2} \left(\frac{4a^2}{1 - e^{-2a^2}} \right) \quad (5)$$

Using the highest power level of 16 MW needed in barrier heating yields a peak field intensity of 3.0×10^6 V/m. This is a worst case calculation assuming the fields of all sources combine coherently, a highly unlikely if not impossible situation. Since fields of the order of 10^7 V/m can be accommodated in vacuum the delivery system should have no inherent breakdown problem. The concept of 10 kW/cm^2 maximum power density does not apply to this scheme since these power levels are based on metallic waveguide systems wherein electric fields are oriented normal to conducting boundaries. Beam modes, on the other hand, have nearly zero electric field intensity at any boundary. Breakdown in the TE_{01} waveguide, horn or gyrotron will limit the power level and increase the number of individual sources needed to achieve the total power requirement.

Mirror reflection loss (I^2R loss) is given by

$$r^2 = 1 - 2\sqrt{\frac{\omega\epsilon}{\sigma}} \quad (6)$$

and will be less than 0.1% for good conductors. Dissipation in dielectric lenses or windows is found from

$$P_{\text{diss}} = \frac{1}{2} \iiint \sigma_{\text{eff}} |E|^2 dV \quad (7)$$

where

$$\sigma_{\text{eff}} = \omega\epsilon\delta$$

and $\delta = \text{loss tangent}$.

For lens material with a loss tangent of 0.0005 and $\epsilon_r = 2.5$ at 110 GHz, use of Eq. 1 for E in Eq. 7 yields,

$$P_{\text{diss}} = \frac{t \sigma_{\text{eff}} z_0 P_{\text{AVG}}}{4} \left(\frac{1 - e^{-2a^2}}{a^2} \right) \quad (8)$$

$$\frac{P_{\text{diss}}}{P_{\text{AVG}}} = 9 \times 10^{-4} t$$

where t is measured in cm, hence a lens loss of 0.09% per cm of thickness.

A spherical lens has a phase characteristic of

$$\psi = e^{j \frac{Rr^2}{2f}} \quad (9)$$

where f = focal length and r = radial distance. Hence the maximum phase shift necessary for a 7.8 cm lens radius and 87.5 cm focal length is

$$\phi = \frac{\omega}{c} 3.48 \times 10^{-3} \text{ radians} \quad (10)$$

If this phase shift is accomplished by using a material with a dielectric constant of 2.5, then

$$\frac{\omega}{c} 3.48 \times 10^{-3} = \frac{\omega}{v_{\phi}} t \quad (11)$$

where t is the lens thickness at the center (r = 0). Since $v_{\phi} = c/\sqrt{\epsilon_r}$, Eq. 9 yields

$$t = 2.2 \text{ mm} \quad (12)$$

If we assume at least 1 cm for mechanical strength, the lens dissipation loss from Eq. 6 is

$$P_{\text{loss}} = 9 \times 10^{-4} P_{\text{AVG}} \quad (13)$$

Equation 11 in turn yields for 1 MW sources

$$P_{\text{loss}} = 900 \text{ watts} \quad (14)$$

or

$$P_{\text{loss}} = 0.004 \text{ dB} \quad (15)$$

V-B-5-e. Summary of Delivery System Loss

Adding the path loss in each gyrotron yields 0.004 dB lens loss, 0.004 dB reflection loss at two surfaces or 0.008 dB, and 0.0001 dB diffraction loss in the first iteration of the beam waveguide for a total of 0.0013 dB. The second iteration loss of 0.1 dB doesn't really occur since the loss calculation is based on an iterative waveguide system. In this case the plasma replaces the next lens and although the spot size there will be slightly larger than the lens diameter no energy is lost since all the wave energy is incident on the plasma. Hence the total path loss is 0.0013 dB. Losses in the dual mode horn will constitute the major loss in the system. The loss in such horn launchers has not been calculated but experimental data at 35 GHz indicates a loss of 1 to 2% in non-optimized horns.

From the above considerations we see that the use of quasi-optical techniques such as the beam-waveguide mode provide an attractive alternative to conventional overmoded circular waveguides. At these power levels, cooling of lenses and metallized mirrors remains an important consideration for reliable steady-state operation. At these high frequencies, surface pitting and blistering of metallized mirrors even at these relatively remote locations from the plasma volume is an important consideration on which more laboratory data will have to be obtained.

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V-C. Refueling by Pellet Injection

It has been proposed in the plasma analysis section (Section III-B) to use pellet injection to refuel the central cell of WITAMIR-I. This avoids the energy loss due to charge exchange reactions associated with gas injection or neutral beam injection to refuel the central cell. In this section we discuss the required pellet size and velocity to achieve penetration of the pellet to the center of the central cell.

The use of solid pellets to refuel fusion devices was first suggested by Spitzer.⁽¹⁾ In recent years several authors have studied the ablation of a pellet in a plasma, including shielding mechanisms which increase the pellet's lifetime. These various theoretical analyses have been reviewed briefly by Turnbull.⁽²⁾ The advent of pellet injection experiments on ORMAK,⁽³⁾ ISX, Pulsator, and elsewhere have provided experimental data to test the various theories. The neutral shielding models of Parks and Turnbull,⁽⁴⁾ Vaslow,⁽⁵⁾ and Milora and Foster⁽⁶⁾ appear to be in reasonable agreement with experimental results. In this model, the pellet is ablated to form a dense neutral gas surrounding the pellet. Most of the energy incident on the pellet from the external plasma is absorbed in the gas. The gas is heated by the incident electrons and expands; near the pellet it is subsonic but becomes sonic and supersonic at larger radii from the pellet surface. The ablation rate (rate of change of the pellet radius) has been estimated to be⁽²⁾

$$\frac{dr_p}{dt} = - 1.58 \times 10^{-8} r_p^{-2/3} n_e^{1/3} T_e^{1.64} \text{ cm/sec} , \quad (1)$$

where r_p is the pellet radius in cm, n_e is the plasma electron density in cm^{-3} , and T_e is the electron temperature in eV. n_e and T_e are evaluated far from the pellet. The more detailed calculations of Milora and Foster⁽⁶⁾ give essentially the same result. Equation (1) is also in agreement with Vaslow's scaling law.⁽⁵⁾

The central cell of the tandem mirror is expected to have a relatively flat density and temperature profile with a sharp drop near the edge.⁽²⁾ (See Section III-F.) Consequently it is reasonable to assume that n_e and T_e are essentially constant over the trajectory of the pellet. Thus, we can integrate Eq. (1) to get the lifetime, τ , and penetration distance, d , of the pellet for a given pellet velocity, v_p , and initial pellet radius, r_0 :

$$\tau = \frac{3}{5} \frac{r_0^{5/3}}{c_0} , \quad (2)$$

$$d = v_p \tau = \frac{3}{5} \frac{v_p r_0^{5/3}}{c_0} , \quad (3)$$

where $c_0 = 1.58 \times 10^{-8} n_e^{1/3} T_e^{1.64}$. The dimensions are cm, sec, and eV.

Equation (3) indicates that it is desirable to have large pellets in order to reduce the required velocity, v_p , for a given penetration distance, d . The maximum pellet size is limited, however, by the requirement that the number of particles introduced into the plasma by a single pellet is a small fraction of the total number of particles in the plasma. Table V-C-1 shows the fuel charge versus pellet size and required velocity for penetration to the axis of the central cell. The pellet is injected just in front of the barrier coil where the radius is smallest. This reduces the required velocity by a factor of 2. We see that if a single pellet is to introduce less than a 10% fluctuation in the total number of particles in the plasma the pellet radius should not exceed ~ 3 mm and the required injection velocity is $\sim 1 \times 10^7$ cm/sec. This is somewhat higher than the estimates ($\sim 10^6 - 10^7$ cm/sec) for tokamak reactors, primarily because of the flatter profile and the higher electron temperature. If penetration to the center is unnecessary, then the velocity can be reduced accordingly. For a 3 mm radius pellet, the required repetition rate is 1.3 pellets/sec. Because of some deuterium re-fuelling by the beam pumping of the barrier, the pellet composition should be 70% T - 30% D.

These estimates are based upon a model in which neutral gas shielding of the pellet from the incident electron energy flux is the only shielding mechanism. It has been suggested⁽⁸⁾ that the magnetic field in the plasma provides an additional shielding mechanism - the effect of a "diamagnetic balloon" surrounding the pellet. This is due to the high pressure of the cold plasma created by ionization of the neutral gas cloud surrounding the pellet. This "diamagnetic balloon" provides additional shielding by the reduction of the heat flux to the pellet. A recent analysis by Lengyel⁽⁹⁾ indicates that this effect is not significant at the heat fluxes encountered in present experiments but may be significant in thermonuclear plasma where the heat flux is much higher, although there is some disagreement on this.⁽²⁾ If Lengyel is

Table V-C-1

pellet radius (cm)	$\left(\frac{N_{\text{pellet}}}{N_{\text{plasma}}}\right)$	v (cm/sec)
.1	.0037	7.1×10^7
.2	.030	2.24×10^7
.3	.10	1.14×10^7
.4	.24	7.06×10^6

correct, then the estimates from the neutral gas shielding model may give pessimistically large injection velocities. It is safer, however, to use these estimates until this question is resolved.

The injection velocities estimated above represent a considerable extrapolation from present technology. One mm diameter hydrogen pellets have been accelerated to 10^5 cm/sec using a pneumatic accelerator with helium as the propellant.⁽¹⁰⁾ The theoretical ultimate speed at room temperature with helium propellant is 3×10^5 cm/sec, but this limit will not be achieved in practice because of the retarding effects of friction. Two stage light gas guns have been used to accelerate pellets of other substances in 2 g sizes to 1.2×10^6 cm/sec.⁽¹¹⁾ A mechanical accelerator of the centrifugal type with the potential for achieving 10^5 cm/sec is under development at ORNL; the use of stronger materials may allow an injection velocity $\sim 5 \times 10^5$ cm/sec. More exotic acceleration schemes such as using a laser or particle beam to ablate one side of a pellet and thereby accelerate it have been proposed. These schemes are not subject to limitations such as material strengths in mechanical accelerators or the sound velocity in gas acceleration, and therefore may lead to higher ultimate speeds. There are, however, no working prototypes so that these schemes have to be considered to be rather speculative.

In summary, it appears that refueling the central cell of a tandem mirror reactor by pellet injection is outside the realm of foreseen extrapolations of present technology, but is only modestly more difficult than the same task in tokamak reactors. There is, on the one hand, sufficient uncertainty in the estimates of the required injection velocity and, on the other hand, the possibility of developing suitable pellet accelerators. Consequently, pellet refuelling cannot be accepted as a straightforward extrapolation of present technology, nor dismissed as totally impractical.

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V-D. Neutral Beam Sources

The three types of neutral beam systems required for WITAMIR-I will be discussed in this section. A short survey of the history of neutral beam technology, current experimental programs, and anticipated future developments are presented in Ref. 1, and will be reproduced here only where relevant to the WITAMIR-I design. Table V-D-1 lists the design parameters.

All the WITAMIR-I neutral beam injectors have an aperture of 10 cm by 40 cm at the source. The beams are assumed to have a divergence of 0.5° in the short dimension and of 2.5° in the long dimension. Present devices have corresponding divergences of 0.7° and 2.8° . One central cell coil on each end is enlarged to provide access for the barrier pumping beams at the angles listed in Table V-D-1. The beam paths are shown in Fig. II-C-2. The barrier pumping beams are focused near the central cell edge of the barrier coil. The plug beam focal point is not critical and is assumed to be near the axis of the plug. The barrier pumping beams are essentially completely trapped, while the plug beam has a trapping fraction of 0.128, necessitating a thermal dump for 16 MW of power. A reasonable efficiency of 50% was used for the high energy barrier beams and the plug beams, in harmony with a conceptual study reported in Ref. 2. An efficiency of 80% is expected to be attained for the low energy barrier beams.

On 2XIIB, twelve neutral beam sources capable of 50-A-equivalent deuterium at 20 keV for 10 msec have operated, and Ref. 1 quotes "state-of-the-art" at 30-A-equivalent deuterium at 120 keV for 1 sec. The necessary extensions of this technology are described below.

The low energy barrier pumping neutral beams, at 9.6 keV, should present few problems beyond extending pulse lengths of approximately one second to continuous operation, since 20 keV sources have operated successfully on 2XIIB and TMX.⁽¹⁾ The high energy barrier pumping neutral beams, at 190 keV, will require considerable development. In particular, it may not be possible to extend the presently available technology of accelerating positive ions in the first stage beyond about 150 keV, entailing the use of less developed negative ion technology. The plug neutral beams, at 500 keV, will definitely require negative ion technology. Protons, rather than deuterons, are involved in the plug, so a factor of $\sqrt{2}$ is gained in ease of reaching equal energy, and similar development to the high energy pumping beams will be needed. All of the

Table V-D-1

<u>Beam</u>	<u>Power (both plugs) (MW)</u>	<u>Energy (keV)</u>	<u>Current (both plugs) (A)</u>	<u>Injection Angle</u>	<u>Species</u>
low energy barrier pump	12.7	9.6	1320	10°	d
high energy barrier pump	42.5	190	224	10°	d
plug	18.4	500	36.8	90°	p

beams will require extrapolation from pulse lengths of about 1 sec to continuous operation.

A conceptual system which approaches all of the desired features for the high energy beams was presented in Ref. 2. The design provided 50% or greater efficiency for 2.1 MW of 400 keV neutral hydrogen with continuous operation.

References for Section V-D

1. B. Cohen, editor, Status of Mirror Fusion Research 1980, Lawrence Livermore Laboratory Report UCAR 10049-80.
2. G. Carlson et al., Lawrence Livermore Laboratory Report UCRL-52836 (1979).

VI. Magnet System of the Tandem Mirror Reactor WITAMIR-I

VI-A. General Features

The tandem mirror reactor WITAMIR-I with a thermal barrier has a set of 34 central cell solenoidal magnets and end plug magnets of more complex configuration (c-shape, yin-yang, solenoids). 32 of the central cell magnets are identical while two magnets are oversized to provide space for neutral beam injection into the barrier region. The central cell solenoids are dc coils of 6.6 m inner diameter, and 9 m for the two oversized magnets. The design of these magnets is relatively straightforward. The on-axis magnetic field in the central cell is 3.6 T with a field ripple of less than 5% at the plasma radius. The peak conductor field is less than 6.1 T (5.6 T in the oversized magnets).

The barrier coil of WITAMIR-I is an assembly of six solenoids with an on-axis field of 14 T and a peak conductor field of less than 15.1 T. In addition each end plug of WITAMIR-I contains two transition coils, one between the barrier coil and the yin-yang plug and one behind the plug. The outside coil is the recircularizing coil. Figure VI-A-1 shows the magnet arrangement in one end plug region while Fig. VI-A-2 shows the on-axis magnetic field variation in this region. The central cell on-axis field of 3.6 T is followed by a steep rise in the barrier coil to 14 T. The field then declines in the thermal barrier region to 10% of the maximum value. In the yin-yang plug, the field reaches a maximum of 6 T. The field is less than 4 T in the center of the yin-yang, providing a plug mirror ratio of better than 1.5.

In Table VI-A-1 the field characteristics of one half of WITAMIR-I are given, while in Table VI-A-2 the magnet positions are given. The end plugs are rotated with respect to each other by 90 degrees.

The central cell magnets have been designed using conventional cryogenic stability criteria (see for example references 2 and 3). The detailed features of our design have been previously given.⁽⁷⁾ The code EFFI has been used for the calculations of fields, forces and inductances.⁽⁵⁾

The field requirements on axis, shown in Fig. VI-A-2, are fulfilled by keeping the field at the conductor below 8.1 T in all magnets, with the exception of the barrier coil (see Table VI-A-3). Only for the barrier coil is Nb₃Sn needed. All other coils can use NbTi conductor at 4.2 K. The critical heat flux of 0.3 W/cm² is used for the central cell coils. The overall

WITAMIR-I MAGNET ARRANGEMENT

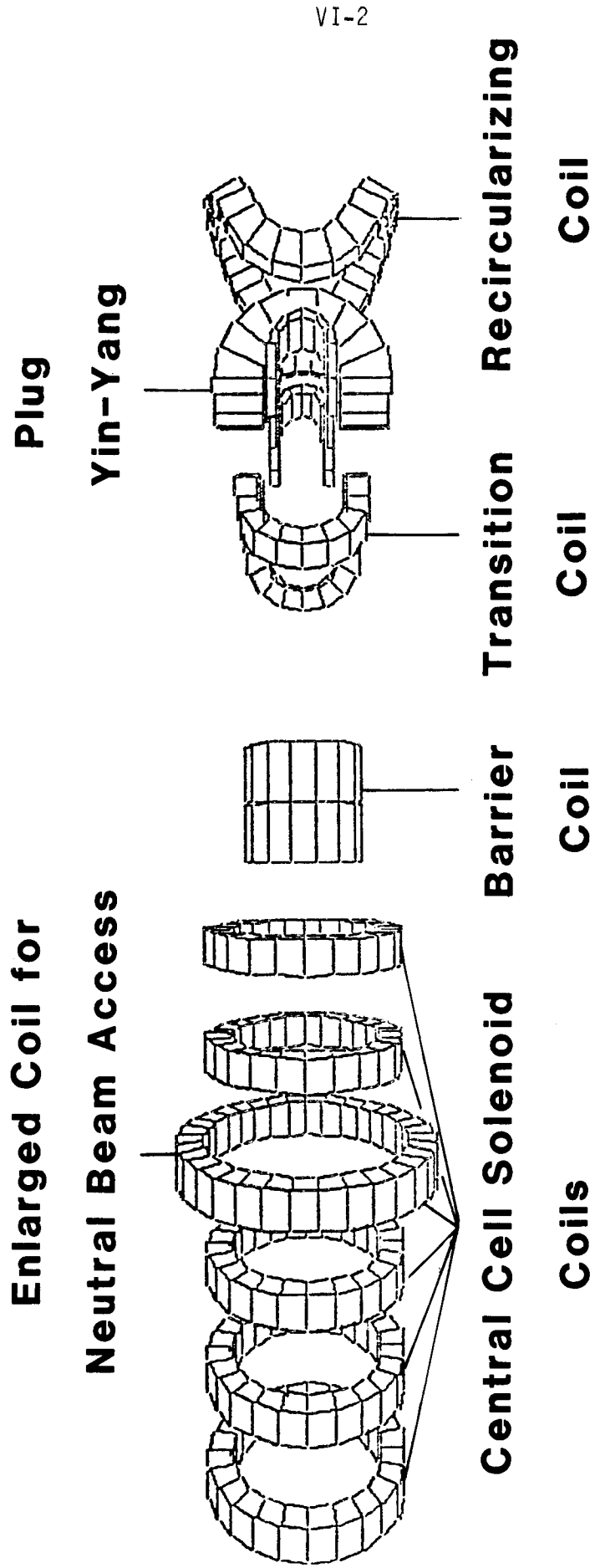


Fig. VI-A-1. Magnet arrangement. One end plug and one end of the central cell are shown.

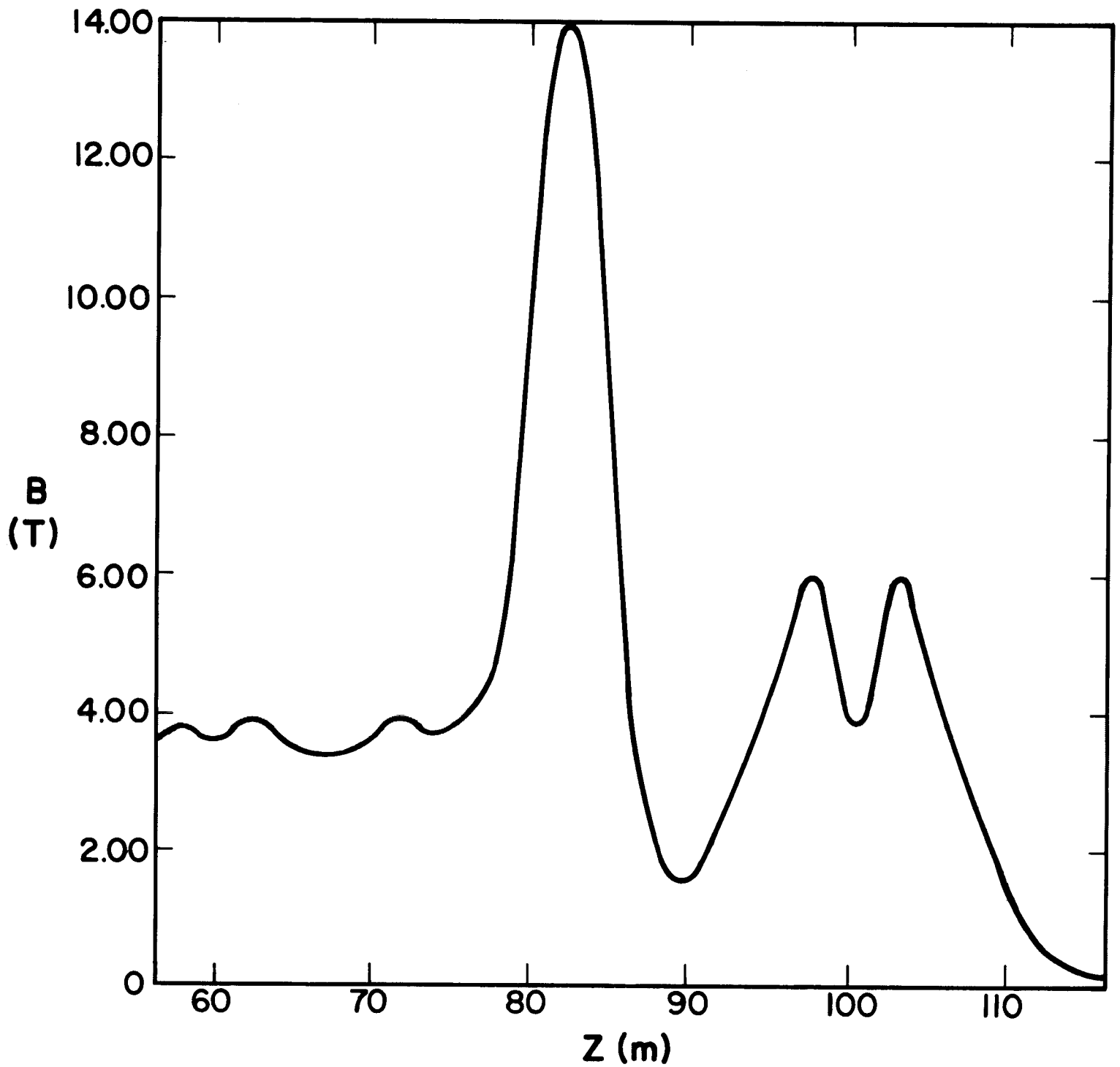


Fig. VI-A-2. On axis magnetic field of WITAMIR-I. The zero of the z axis is at the center of the reactor.

Table VI-A-1

Field Characteristics of WITAMIR-I (One Half)

Magnetic Field on Axis	At z (in m)	B (T)
Central Cell	0. to 76.395	3.6
Barrier Coil (Maximum Field Position)	82.555	14.0
Minimum Barrier Field	89.395	1.4
First Maximum in the Plug	97.795	6.0
Minimum Plug Field	100.595	3.84
Second Maximum in the Plug	103.395	6.0

Table VI-A-2

Magnet Position in WITAMIR-I (Midplane or Center)

Central Cell: 34 solenoids, S1 to S34, separated by 4.63 m from midplane to midplane. The z-position of S1 is at -76.395 m; the z-position of S34 is at +76.395. The origin is between S17 and S18. S3 and S32 are the oversized coils.

End Plug:

Negative Side of z-Axis (m)		Positive Side of z-Axis (m)	
RCN1	-107.815	RCN2	93.495
YYN1	-100.655	YYN2	100.655
YYP1	-100.655	YYP2	100.655
RCP1	-93.495	RCP2	107.815

Barrier Coil:

BC1	-82.555	BC2	82.555
Left Half At	-83.825	and	81.285
Right Half At	-81.285	and	83.825

Table VI-A-3

Maximum Magnetic Fields at Conductor in WITAMIR-I

Central Cell Magnets	Maximum Field is Less Than 6.1 T (5.6 T in oversize coils)
Barrier Coil	15.1 T
Transition Coil RCN2	
zx-plane	8.1 T
zy-plane	7.9 T
Yin-Yang Coils	
YYN2	
zx-plane	8.1 T
zy-plane	7.7 T
YYP2	
zx-plane	7.7 T
zy-plane	7.8 T
Recircularizer RCP2	
zx-plane	7.8 T
zy-plane	7.9 T

current density in the NbTi coils does not exceed 1900 A/cm^2 . In Table VI-A-4 the main features of the WITAMIR-I magnets are given.

The recircularizing coil (RCP) in the end plug is an option. The use of this coil will be influenced by the construction of the plasma direct converter. With a recircularizing coil, a spherical construction is possible; without a recircularizer, the construction is elliptical.

The barrier coil is a five meter long solenoid, built up of six subcoils. There is an 8 cm wide space in the midplane of the coil for pellet injection for fueling. The winding thickness is 1.01 m. Three conductor types are used in this coil: Nb_3Sn for conductor fields higher than 12 T, Nb-Ti-Ta for conductor fields less than 12 T (to about 8-9 T) and Nb-Ti for fields of 8 T or less. The coil is cooled with He II. This solenoid can be designed in a manner similar to our earlier design of a classical yin yang.⁽⁸⁾

The magnetic forces on the coils have been calculated for the case when all coils are in operation with the designed current densities. The most important result of the calculation is that in all C-shaped coils the absolute value of any in-plane (i.e. parallel to the broad face) or out-of-plane (i.e. perpendicular to the broad face) force is less than $0.5 \times 10^6 \text{ N/cm}$. These forces are well below the high levels which so complicated our earlier design of a "classical" (i.e. without thermal barrier) end plug yin-yang.⁽⁸⁾ The forces are well distributed over the four C-coils in the sense that no high peak forces occur in any coil. The line forces in the barrier coil are less than $1.0 \times 10^6 \text{ N/cm}$ in each part of the coil, while the line forces in the central cell solenoids are less than $0.3 \times 10^6 \text{ N/cm}$, including the oversized coils. The maximum magnetic self forces in any coil, if the coil is operated alone, are lower than the operational maximum forces. For example, the maximum self force in the yin-yang is about a factor of two lower than the standard operational force.

The stored magnetic energy of WITAMIR-I is 78 GJ, with about 43 GJ stored in the central cell. In Table VI-A-4 the stored self energies of the coils are shown.

VI-B. Central Cell Magnets

The central cell magnetic field of 3.6 T will be achieved with a set of solenoids spaced at regular intervals to provide space for fueling and diagnostic devices and for the blanket and shielding modules. The central cell magnets are superconducting solenoids, operating in a steady-state dc mode at

Table VI-A-4: Magnet Characteristics of WITAMIR-I

Coil Purpose and Type	Unit	Central Cell Solenoid	Barrier Coil Solenoid	Transition Coils Reversed Cee-Coils	Yin-Yang Coils
Number of Coils		32 + 2	2	2 + 2	2 + 2
Function		C.C. Field	Mirror	Change of Flux Bundle Cross Section	Mirror, Magnetic Well
Major Radius	m	4.3	5.7	2.71	1.66
Minor Radius	m	3.3	4.5	1.7	1.0
Sweep Angle	degree			65	90
Bundle Cross Section	m x m	1.4x1.0	1.6x1.2	1.4 x 0.9	2.1 x 0.36
Mean Turn Length	m	23.88	32.05	26.4	24.45
Volume of Winding	m ³	33.427	61.525	33.251	18.478
Weight of Winding	Tonnes	88.1	162.5	283	157
Overall Current Density	A/cm ²	950	800	1000/1500/2000	1900
Ampere Turns	10 ⁶ A-Turns	13.3	15.36	18.65	14.4
Self-Inductance Per N ²	10 ⁻⁶ H/N ²	9.807	14.04	9.367	6.223
Stored Self-Energy	GJ	0.87	1.66	1.63	0.642
Operating Temperature	K	4.2	1.8	4.2	4.2
Field on Axis	T	3.6	14	N/A	6
Max. Field on Conductor	T	6.1	5.6	8.1	8.1
Superconductor		NbTi	Nb ₃ Sn/NbTiTa/NbTi	NbTi	NbTi
Stabilizing Material		Al	Cu	Cu	Cu
Structure Material		Al	CuNb	SS	SS

a temperature of 4.2 K. The solenoids are designed for cryostable operation to provide safe long-term operation.⁽⁷⁾

The characteristics of the 32 normal sized and the two oversized central cell magnets are shown in detail in Table VI-B-1. The number of the central cell magnets results from the central cell length and from the accessibility and field ripple requirements. The coil windings cover about 30% of the central cell surface so that adequate space is available for the exchange of blanket and shielding parts. The mass of the winding of one standard sized coil is about 90 tonnes and 163 tonnes for an oversized coil. The NbTi superconductor represents about 1.5% of the total coil weight, making about 48 tonnes in total for the central cell.

The field requirements of 3.6 T on axis and less than 5% field ripple at the plasma radius allow a fairly straightforward design of the magnets. The maximum field at the conductor is less than 6.1 T (5.6 T for the oversize coil), with a conductor current density of 4000 A/cm² and an inner coil radius of 3.3 m (4.5 m for the oversize coil).

The NUWMAK design for the conductor arrangement (disk configuration) is chosen for the central cell magnets⁽¹⁾ (see Fig. VI-B-1). It is an aluminum-stabilized NbTi conductor with an aluminum-alloy structure operating at 4.2 K. The characteristics of this design are:

- Aluminum minimizes weight and cost;
- A working strain of 0.003 is chosen to keep the resistivity of the aluminum stabilizer below $10^{-10} \Omega\text{m}$ ⁽⁴⁾;
- 2219-T87 aluminum alloy is used for the structure to ensure a good thermal contraction match between the structure and conductor.⁽⁶⁾

The 12000 A conductor, 3 cm wide and 1 cm thick, is embedded with fiberglass epoxy into the spiral grooves of an aluminum alloy structural disk (see Fig. VI-B-1). The conductor itself is encapsulated in 0.08 cm of high strength aluminum alloy to prevent plastic flow of the high purity aluminum due to the high magnetic forces.

Alternative designs considered were:

- Al-stabilized conductor with stainless steel structure;
- Cu-stabilized conductor with stainless steel structure.

The first assembly was rejected due to the different thermal contraction coefficients between aluminum and stainless steel. The second one was rejected because the weight of the central cell magnets was approximately three

Table VI-B-1

Characteristics of the Central Cell Magnets

	<u>Unit</u>	<u>Normal Size</u>	<u>Oversize</u>
Number of Solenoids	32	2	
Coil Separation (Winding/Winding)	m 3.23	3.23	
Coil Separation (Midplane/Midplane)	m 4.63	4.63	
Axial Coil Length	m 1.4	1.6	
Radial Coil Thickness	m 1.0	1.2	
Inner Coil Radius	m 3.3	4.5	
Outer Coil Radius	m 4.3	5.7	
Mean Coil Radius	m 3.8	5.1	
Volume of One Coil	m ³	33.427	61.525
Average Density of a Coil	t/m ³	2.64	2.64
Mass of One Coil	t 88.087		162.5
Operating Temperature	K 4.2	4.2	
Conductor Current Density	A/cm ²	4000.	4000.
Overall Current Density	A/cm ²	950.	800.
Maximum Field on Conductor	T < 6.1	< 5.6	
Field Ripple on Axis	% < 3.5	N/A	
Field Ripple at Plasma Radius	% < 4.1	N/A	
Operating Current	A 12090.	12000.	
Number of Turns	1100.	1280.	
Ampere-Turns	10 ⁶ A-Turns	13.3	15.36
Self Energy	GJ	0.87	1.17

Total Stored Energy in the Central Cell 43 GJ

Table VI-B-1 (continued)

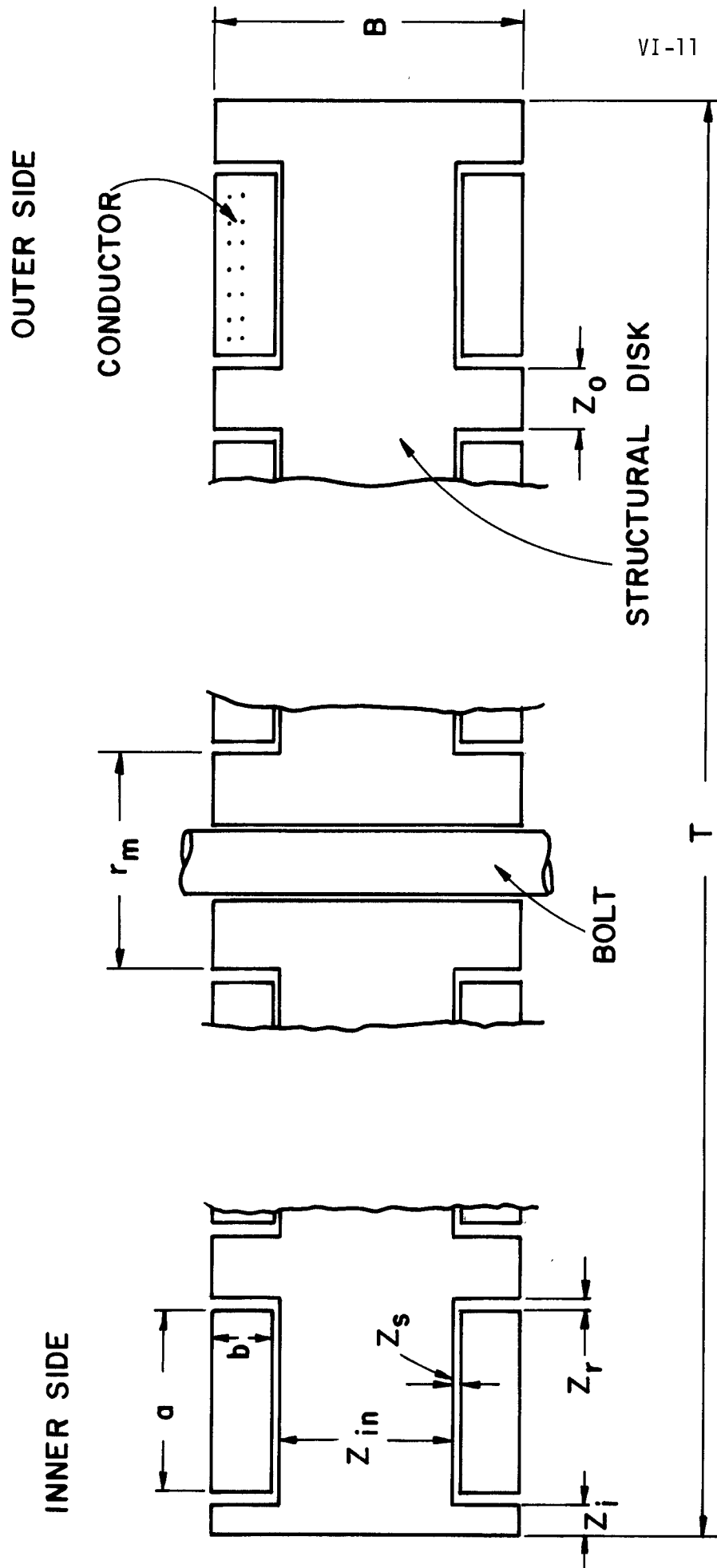
Conductor of Central Cell Magnets

Dimensions (Radial x Axial)	3.0 cm x 1.0 cm
Superconductor	NbTi
Fraction of SC in Conductor	3%
Stabilizer	High Purity Aluminum
Fraction of Stabilizer in Conductor	75%
Skin	High Strength Aluminum Al-2219 T87
Fraction of Skin in Conductor	22%
Percentage of Wetted Perimeter	> 24%
Conductor Assembly	Disk Structure
Structural Material	High Strength Aluminum
Working Strain in Structure	0.003
Working Stress in Structure	246 MPa
Yield Stress at 4.2 K	505 MPa
Number of Conductors per Disk	2 x 22
Number of Disks per Magnet	25

Disk Dimensions for Normal Size Magnets

Cross Section (Radial x Axial)	100. cm x 5.52 cm
Z_o	1.2 cm
Z_i	0.5 cm
Z_s	0.1 cm
Z_r	0.2 cm
Z_{in}	3.32 cm
r_m	2.3 cm

(see Fig. VI-B-1)



DISK CONFIGURATION

Fig. VI-B-1

times higher than in the chosen conductor design and was therefore more expensive.

The stored self energy in one central cell magnet is about 0.87 GJ (1.66 GJ for the oversized coil), and the total stored energy in the central cell is about 43 GJ.

The protection integral for the normal sized magnets for a discharge with 2 kV gives a value of $0.58 \times 10^{17} \text{ A}^2\text{s/m}^4$ for discharge into a fixed resistor and a value of $0.39 \times 10^{17} \text{ A}^2\text{s/m}^4$ for a constant voltage discharge. The maximum temperatures are about 60 K or 40 K respectively. The time constant of a normal sized central cell magnet is 72 s and 138 s for the oversized coils. The values of the protection integral for the oversized coils are about twice those of the normal sized coils, and so the temperature rise is approximately twice the rise of the normal sized coils under the same discharge conditions.

A detailed description of the design of the normal sized central cell solenoids for the TMR WITAMIR-I is given in ref. (7).

VI-C. The Barrier Coil

The barrier coil is designed for a peak field of 14 T on the machine axis. The operating temperature is 1.8 K. The overall length of the coil winding is 5 m and the overall winding thickness is 1.01 m including radial slots. The characteristics of the barrier coil are given in Table VI-C-1.

In the design proposed here, only the general features have been investigated and further optimizations are undoubtedly possible. For example, the coil design presented here uses a subdivision into 6 blocks of constant current density. The coil is built up by two winding halves, separated by an axial slot of 8 cm to provide space for pellet injection for fueling. Each half coil consists of three nested solenoids, separated by radial slots of 3 cm. Figure VI-C-1 shows the cross section of the winding and the conductor fields for the whole cross section. The maximum conductor field at the inside is ~ 15.1 T. Therefore a Nb_3Sn -conductor is used in the inner part of the coil. The center coils have a conductor field of less than 12 T permitting a NbTiTa -conductor to be used while NbTi is used in the outer parts. As can be seen from Fig. VI-C-1, the peak field in the outer section at the ends actually reaches 9 T. However, this can be reduced by spreading the end pancakes with little effect on the side of the coil or the central on-axis field. The total stored energy of the barrier coil is 6.38 GJ.

Table VI-C-1
 Characteristics of the Barrier Coil

	<u>Unit</u>	<u>Inner Coils</u>	<u>Middle Coils</u>	<u>Outer Coils</u>	<u>Total Coil</u>
Inner Radius	m	1.7	2.03	2.31	1.7
Outer Radius	m	2.00	2.28	2.71	2.71
Radial Gap	m	0.03	0.03		2 x 0.03
Axial Gap	m				0.08
Mean Radius	m	1.85	2.155	2.51	2.205
Axial Width	m	2.46	2.46	2.46	5.00
Thickness	m	0.3	0.25	0.4	1.01
Winding Volume per coil	m ³	8.578	8.327	15.52	64.85
Total Volume of Slots	m ³				4.852
Total Coil Volume	m ³				69.70
Weight	tonnes				558

Superconductor		Nb ₃ Sn	NbTiTa	NbTi	
Current Density in sc	kA/cm ²	27.5	40	120	
Stabilizer/ sc-Ratio		18.3	13.3	45	
Conductor Current Density	kA/cm ²	1.5	3.0	2.0	
Overall Current Density	kA/cm ²	1.0	1.5	2.0	

Magnetic Field on Axis	T				14.0
Maximum Field on Conductor	T	< 15.1	< 12.0	< 9.0 Resp. 8.0	
Ampere-Turns	10 ⁶ A-Turns	2 x 7.38	2 x 9.225	2 x 19.68	72.57
Self Energy	GJ				6.38
Equivalent Inductance	10 ⁻⁶ H/N ²				2.425

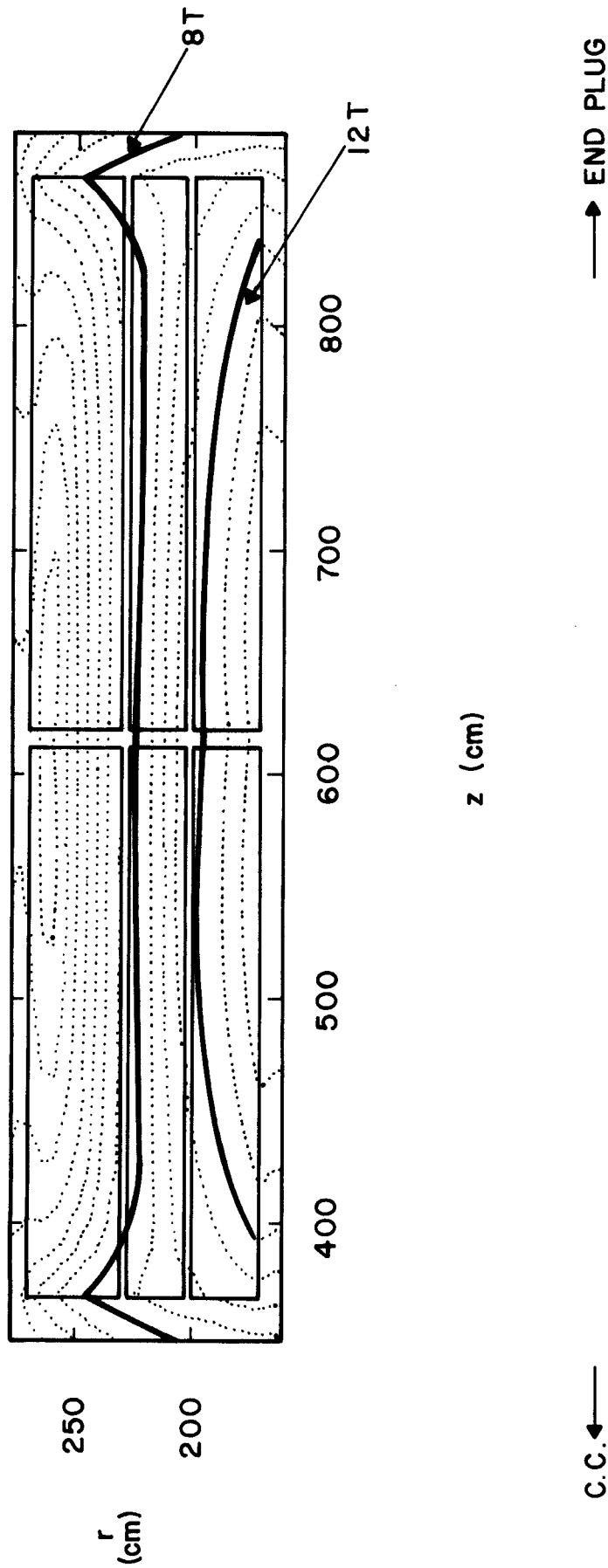


FIGURE VI - C - 1:
WINDING CROSS SECTION OF THE BARRIER COIL.

Due to the high mutual inductances, the magnetic coupling between the barrier coil parts is very strong. So in the case of failure of one coil a considerable amount of energy is transferred to the neighboring coils, leading to current enhancement. Consequently the critical current of the superconductor can be exceeded and most of the energy is dumped in the coil. To avoid the energy dumping in the coil a discharge circuit has to handle most of the energy and dump it outside the coil. This consideration is valid for the whole magnet system, but due to the enormous stored energy in the barrier coil a safety discharge circuit is of special importance here.

Due to the high field requirements and thus the high requirements on magnet technology, the consequences of a barrier field decrease were studied. In comparison with the WITAMIR-I reference case with 14 T on axis, a 12 T case was investigated. In this case the reactor Q dropped by 3 from 28 to 25 and the input power in the barrier region increased by about 8 MW from 42 MW to 50 MW for the high energy neutral beams.

VI-D. Transition Coils

The plasma coming out of the barrier coil is of circular cross section. If the plasma were to directly enter the plug yin-yang, then its shape would be changed to an elliptical one. The transition coil between the barrier coil and the plug yin-yang elongates the plasma in the opposite direction, so that the plasma shape in the center of the yin-yang is approximately circular. This behavior is shown in Fig. VI-D-1, where the center of the yin-yang is actually at $z=2426$ cm. In this figure, field lines are shown in two perpendicular planes in the end plug region. These field lines start at 0.8 m (plasma radius) in the central cell.

The transition coil must be close to the plug yin-yang and has to produce a significant axial field to reasonably limit the field line excursion. In the design case the maximum field contribution of the transition coil is 3.1 T on axis. On the other hand, the transition coil must be separated sufficiently from the barrier coil to give the required minimum barrier field of 1.4 T.

The last coil in the end plug, the recircularizing coil, converts the elliptical magnetic flux bundle leaving the yin-yang plug to an approximately circular shape. This allows a spherical construction of the plasma direct convertor. The transition and recircularizing coils of our design are

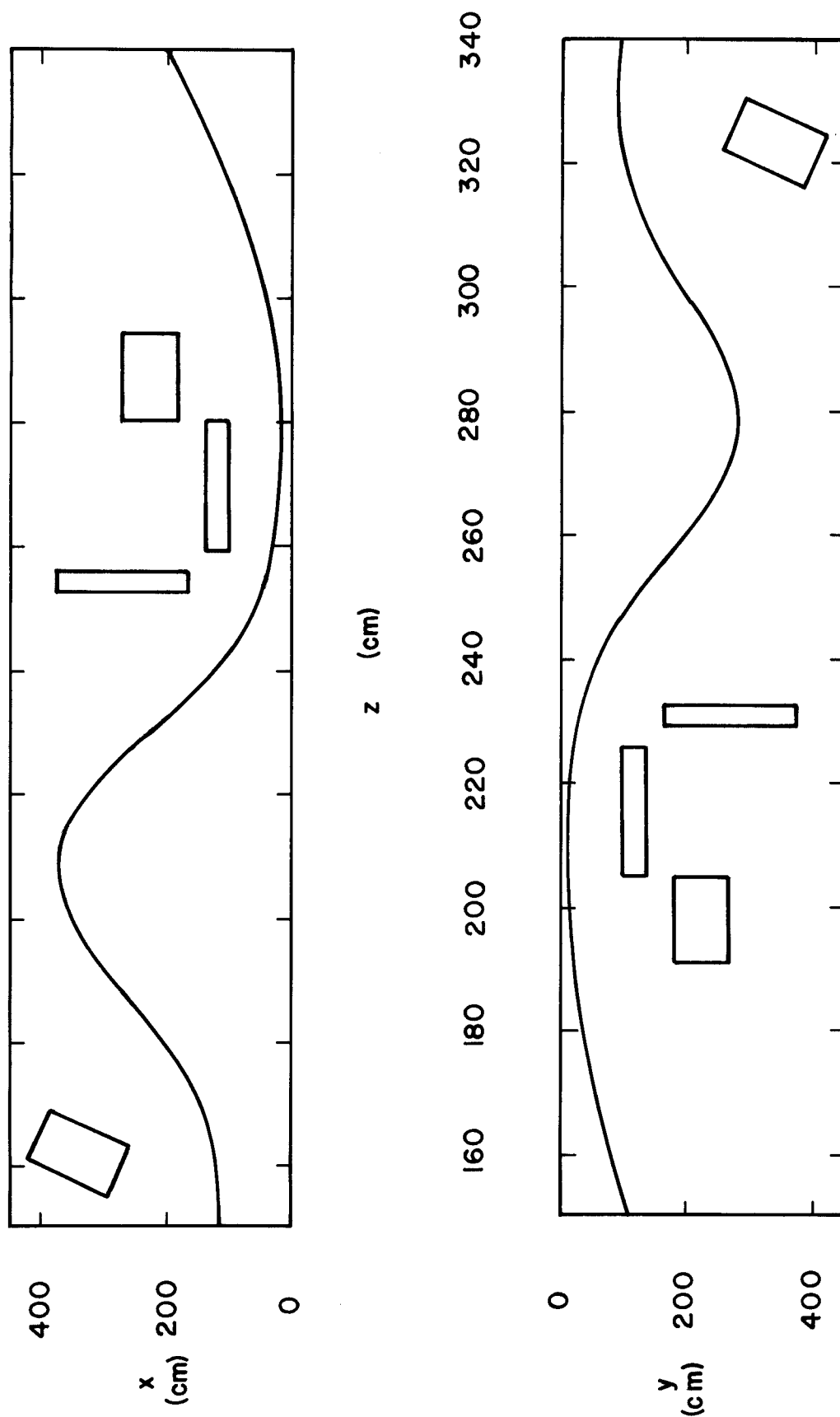


Fig. VI-D-1. Field lines in the end plug region with recircularizing coil.

the z axis units are to be multiplied by 10, i.e., 160, 180, 200, etc. should be 1600, 1800, 2000 etc.

identical, only the orientation of the coils being different. Their main features are given in Table VI-D-1.

Omitting the recircularizer has the consequence that the flux bundle cross section leaving the plug yin-yang is elliptical, as shown in Fig. VI-D-2, so that the plasma direct convertor has to be of elliptical construction.

Omitting the recircularizer has another consequence. The field in the plug region drops from 6 T to 5.9 T for the first maximum, from 3.84 T to 3.46 T in the center of the plug and from 6 T to 4.6 T in the second (outer) maximum. The reestablishment of the previous values of 6 T in the maxima and 3.84 T in the center of the plug would require an increase in current density from 1900 A/cm^2 to 2100 A/cm^2 in the yin coil and to about 3000 A/cm^2 in the yang coil (outer coil) together with a movement of the yang coil by about 2 m towards the end of the plug to provide the required plug mirror ratio of 1.5. In this case the yin-yang has to be operated in an asymmetrical current mode. Moreover the plug volume and the number of particles in the plug are increased, leading to an ECRH power rise for the plug. In addition, the force distribution in this case is asymmetrical in the plug.

The safety integral for the transition coils with a stored self energy of 1.63 GJ, a conductor current density of 4000 A/cm^2 , an operating current of 15000 A and a discharge voltage of 2 kV is $0.87 \times 10^{17} \text{ A}^2\text{s/m}^4$ for the discharge in a constant resistor and $0.58 \times 10^{17} \text{ A}^2\text{s/m}^4$ for a constant voltage discharge. This leads to temperature rises of about 80 K and 60 K, respectively, for copper stabilized conductors. The time constant of the coils is 110 s.

The conductor used in the transition coils is a copper stabilized NbTi conductor with a square cross section.

Winding C-shaped coils such as needed for the yin-yang set and the transition regions is more difficult than winding simple solenoids, because the winding machine must have two degrees of freedom. The degrees of freedom are: (1) 360° rotation about one axis and (2) 180° rotation about an axis perpendicular to the first one. In this respect, a naval gun turret makes an ideal C-shaped coil winding machine and, indeed, has been used for winding the MFTF coils.

The first step in making a C-shaped coil is the construction of the coil case. The parts of the coil case which are used during winding define the

Table VI-D-1

Main Features of the Transition Coils

Number of Coils	4
Major Radius	2.0 m
Minor Radius	1.0 m
Sweep Angle	65 degrees
Bundle Cross Section	1.4 m x 0.9 m
Mean Turn Length	26.4 m
Volume per Coil	33.251 m ³
Weight per Coil	283 tonnes

Superconductor	NbTi
Stabilizer	Cu
Structure	SS

Conductor Current Density	4000 A/cm ²
Overall Current Density	1480 A/cm ²
Ampere-Turns	18.65 x 10 ⁶ A-Turns
Self Inductance Per N ²	9.367 x 10 ⁻⁶ H/N ²
Self Energy	1.63 GJ
Maximum Field Contribution on Axis	3.1 T
Maximum Field on Conductor During Normal Operation	< 8.1 T

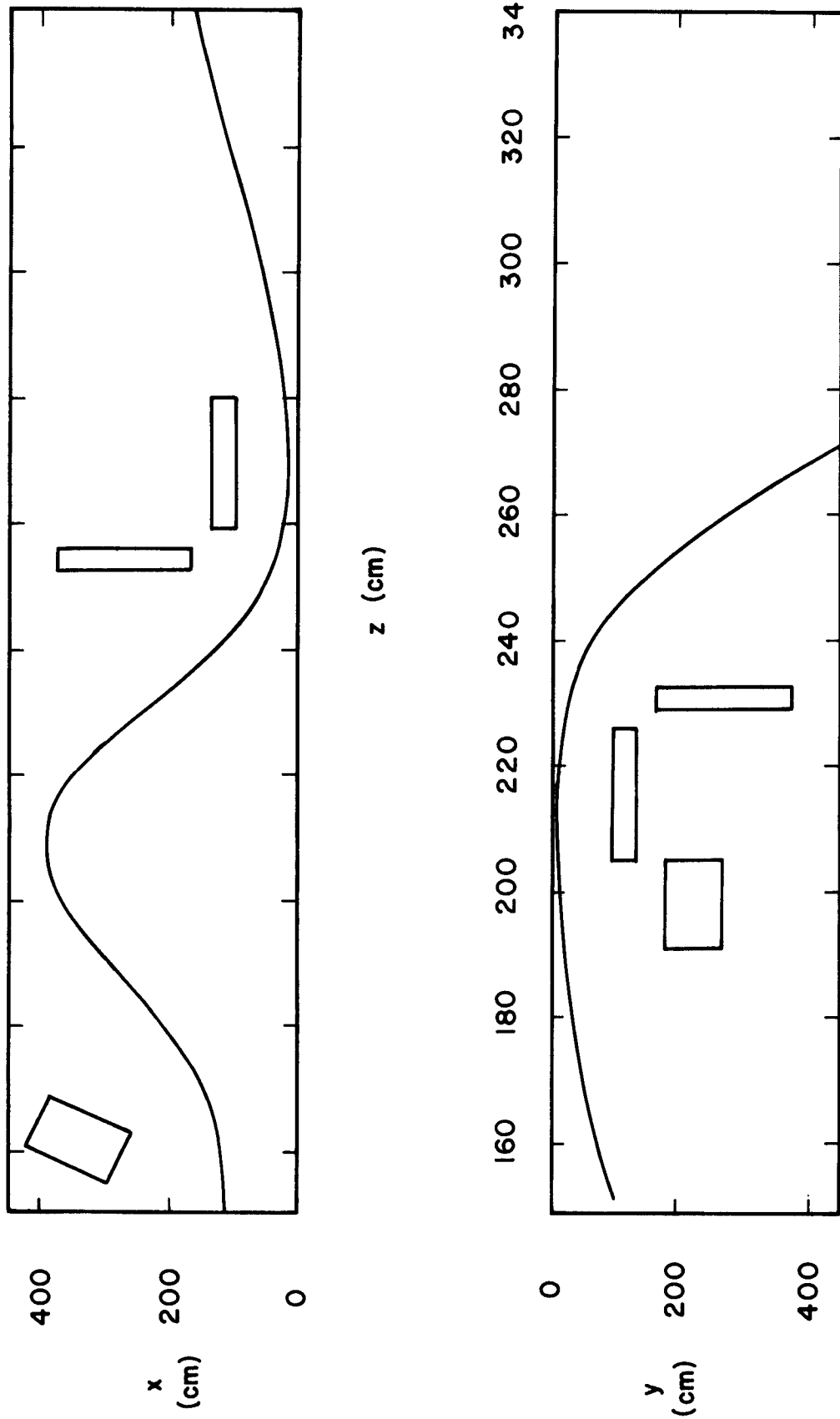


Fig. VI-D-2. Field lines in the end plug region without recircularizing coil.

the z axis units are to be multiplied by 10, i.e., 160, 180, 200, etc. should be 1600, 1800, 2000 etc.

inner boundaries of the conductor bundle. The rest of the case, which defines the outer boundaries of the conductor bundle, is usually added on after the coil is wound. Winding proceeds by layers, starting at an inner boundary against the coil case and ending at the outer boundary. As each layer is added on, a joint is made in the preceeding layer. The joints alternate on the inner and the outer boundaries, depending on the electrical continuity within the coil.

One of the hardest problems in winding such coils is maintaining the tension on the conductor and subsequently preventing the conductor from popping out as the winding proceeds from rotation about one axis to the other. This is especially true when the conductors are rigid as in the MFTF. Specially designed shoes are used to clamp the conductors and keep them in place. Removing the shoes as subsequent layers are added, and then putting them back, necessarily complicates and slows down the winding process.

VI-E. The Plug Yin-Yang

The plug yin-yang is designed with a mirror ratio of better than 1.5. The central field on axis is less than 4 T and the mirror field is 6 T together with the field contributions of the transition coils. Without transition coils the maximum field is 4.4 T and the central on axis field is 3.0 T. The general features of the yin-yang coils are given in Table VI-E-1.

The yin-yang plug provides, as explained in Chapter III on plasma physics, a mirror ratio of better than 1.5 and a radial magnetic well necessary for MHD stability. This is shown in Fig. VI-E-1.

In the WITAMIR-I design the maximum field on conductor in the yin-yang coils is less than 8.1 T. In Fig. VI-E-2 and Fig. VI-E-3 a section of the yin-yang in the zx- and zy-plane is shown with the contours of the magnetic field magnitude. These figures show the values for the operation of all coils with the designed current density.

The maximum magnetic line forces during operation are less than 0.5×10^6 N/cm. The self forces are only about 60% of these.

The safety integral for the yin-yang coils for a stored energy of 0.642 GJ, a conductor current density of 4000 A/cm^2 and an operating current of 15000 A and a discharge voltage of 2 kV is $0.43 \times 10^{17} \text{ A}^2\text{s/m}^4$ for a constant resistor discharge and $0.29 \times 10^{17} \text{ A}^2\text{s/m}^4$ for a constant voltage discharge. This gives a temperature rise of about 40 K for copper stabilized conductors. The time constant of a coil is 53 s.

Table VI-E-1
Characteristics of the Plug Yin-Yang

Number of Coils	4
Major Radius	1.66 m
Minor Radius	1.0 m
Sweep Angle	90 degrees
Bundle Cross Section	2.1 m x 0.36 m
Mean Turn Length	24.45 m
Volume per Coil	18.478 m ³
Weight per Coil	157 tonnes

Superconductor	NbTi
Stabilizer	Cu
Structure	SS

Conductor Current Density	4000 A/cm ²
Overall Current Density	1900 A/cm ²
Ampere-Turns	14.4 x 10 ⁶ A-Turns
Self Inductance per Coil	6.223 x 10 ⁻⁶ H/N ²
Self Energy	0.642 GJ
Maximum Field Contribution on Axis	4.4 T
Central Field Contribution on Axis	3.0 T
Peak Field on Axis with Transition Coils	6.0 T
Central Field on Axis with Transition Coils	3.84 T
Maximum Field on Conductor During Normal Operation	< 8.1 T

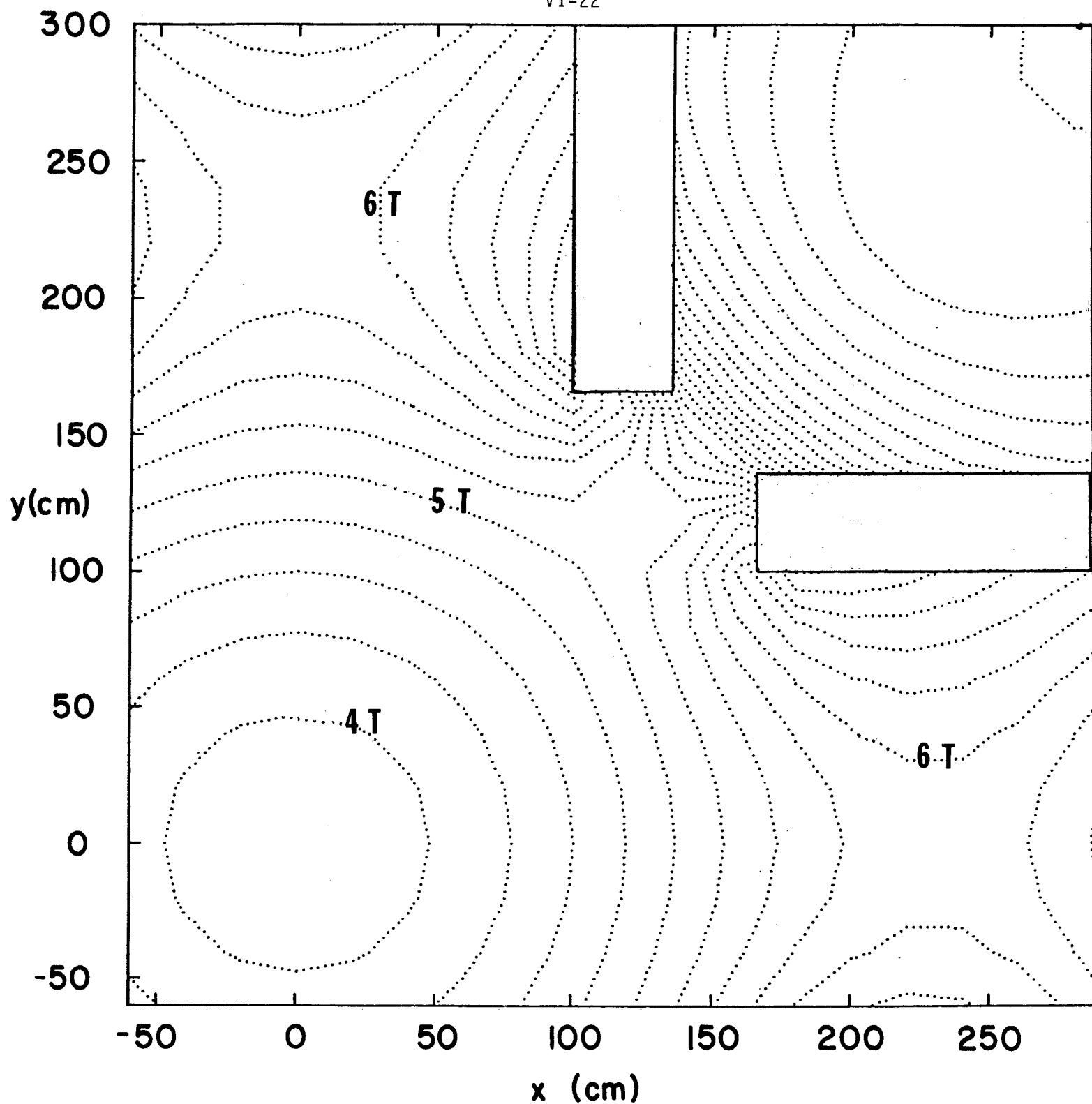


FIGURE VI - E - 1. XY - PLANE SECTION OF THE YIN - YANG COILS THROUGH THE CENTER OF THE PLUG WITH FIELD CONTOURS OF CONSTANT MAGNITUDE SHOWN.

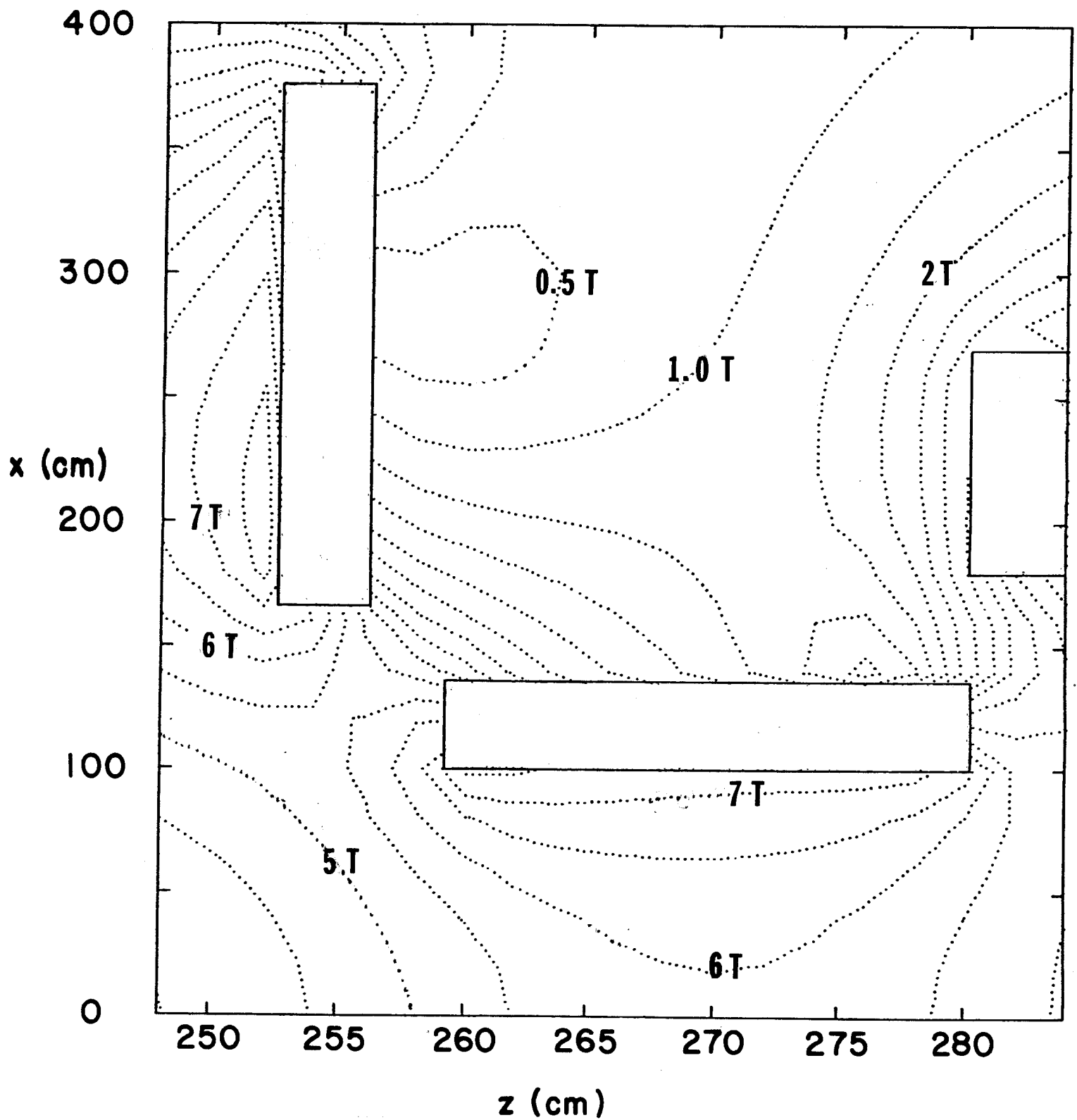


Fig. VI-E-2. Section of the yin-yang in the zx -plane showing the field magnitude. A part of the recircularizing coil is shown at right.

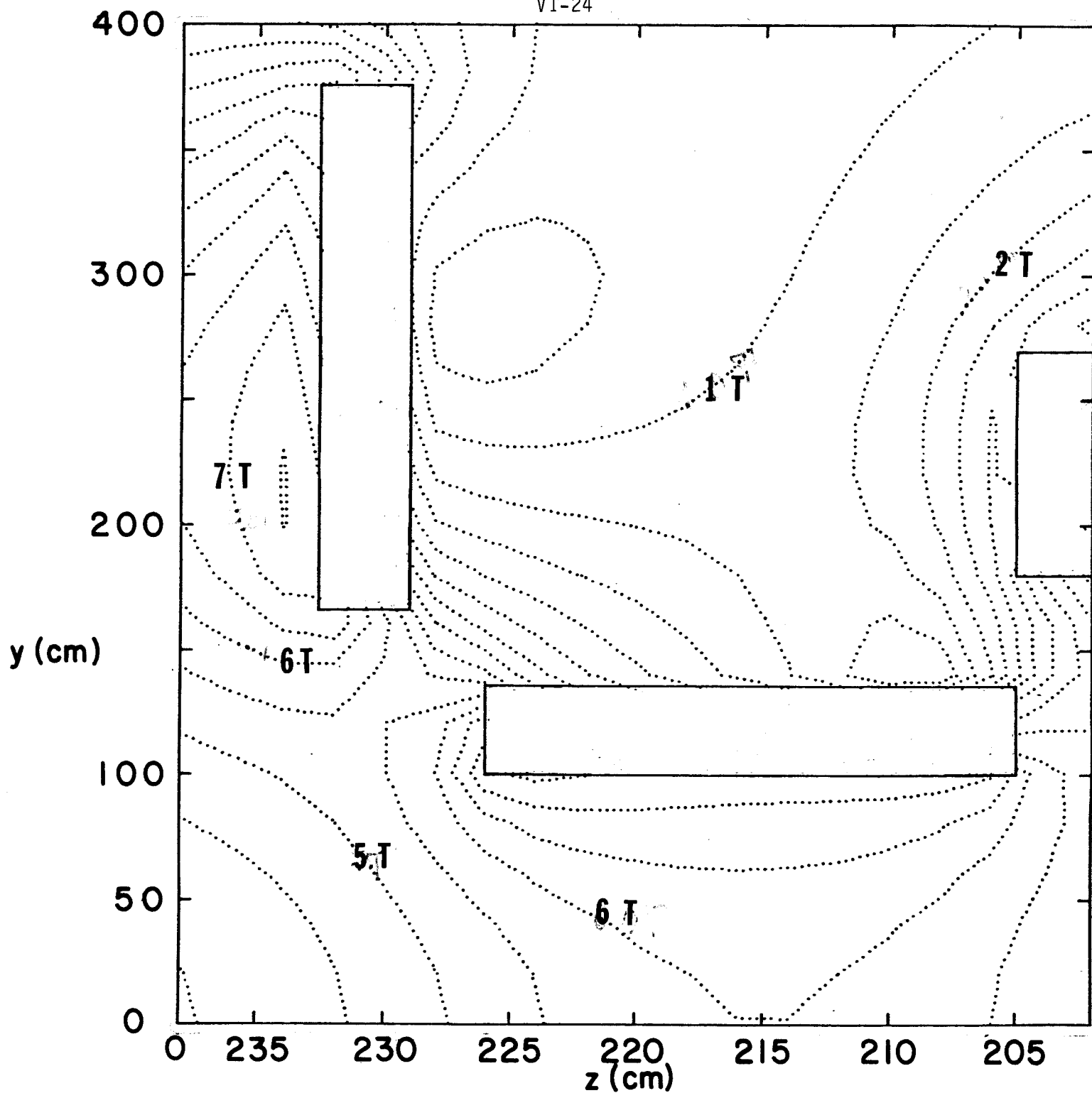


Fig. VI-E-3. Section of the yin-yang in the zy -plane showing the field magnitude. A part of the recircularizing coil is shown at right.

VI-F. Conclusions

The magnet configuration of the tandem mirror fusion reactor WITAMIR-I allows the following conclusions:

- All magnets are d.c. magnets. No pulsed magnets are needed.
- The central cell magnets are relatively simple magnets. Mass production of the disks is possible; 800 identical disks for the 32 normal sized central cell magnets are necessary.
- The end plug yin-yang and the transition coils are indeed big coils, but relatively conservative in their design. The fields on axis are moderate, and the maximum conductor field is less than 8.1 T. This fact allows the use of NbTi conductors with Cu-stabilizer. The overall current density is less than 2000 A/cm² in the yin-yang coils and less than 1500 A/cm² for the transition coils. The magnetic load is less than 0.5×10^6 N/cm. The C-shaped magnets in the end plug are buildable with today's technology.
- The barrier coil is challenging because of the high field, large radius and the consequent high stresses. We have taken an innovative approach, utilizing He II cooling and a high strength in situ stabilizer to ease the design problems of this coil.

The results of our 12 T investigation encourage us to pursue this case for a demonstration reactor or for materials test reactors based on the principle of the tandem mirror with thermal barrier.

- The stored energy of about 78 GJ in the whole magnet system compares to the stored energy in the toroidal field magnet system of NUWMAK (30 GJ) and UWMAK-III (108 GJ).
- Another point has to be mentioned: the fringe fields are very high. In the end plug region about 5 m outside of the machine axis they are about 1 T. The end tube of the neutralizer of the neutral beam injector for the barrier region is placed in a field of 0.5 to 1 T. This fact requires magnetic screening of the neutral beam injector.

References for Chapter VI

1. B. Badger et al., "NUWMAK, A Tokamak Reactor Design Study," University of Wisconsin Fusion Engineering Program Report UWFDM-330, (1979).
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4. H. R. Segal, IEEE Trans. on Mag., Vol. MAG-13, No. 1, p. 109 (1977).
5. S. J. Sackett, Lawrence Livermore Laboratory Report UCRL-52402 (1978).
6. Handbook of Materials for Superconducting Machinery, MCIC-HB-04. Metals and Ceramics Information Center, Battelle Columbus Laboratories, Columbus, Ohio.
7. W. Maurer, D. C. Larbalestier, I. N. Sviatoslavsky, "Design Procedure of the Central Cell Solenoids for the Tandem Mirror Reactor WITAMIR-I," University of Wisconsin Fusion Engineering Program Report UWFD-361 (1980).
8. S. O. Hong, D. C. Larbalestier, L. P. Mai, I. N. Sviatoslavsky, I. Ojalvo, "A Preliminary Design of the End Plug Magnet System for a Tandem Mirror Reactor," Proc. of the 8th Symp. on Eng. Problems of Fusion Research, IEEE Pub. No. 79CH1441-5NPS, p. 1683 (1980).

VII. Direct Convertor

VII-A. Introduction

A single-stage plasma direct convertor has been incorporated into the reactor design to provide direct conversion of the ion kinetic energy lost out the ends to electricity. A direct convertor is required only at one end since the ion end loss can be directed out one end. This is accomplished by lowering the potential of one plug slightly relative to the other plug. As central cell ions upscatter in energy, they escape out the lower potential plug before acquiring enough energy to escape out the other plug. The electron end loss can be directed out the end opposite to the direct convertor by biasing that end wall slightly positive relative to the ground potential at the entrance of the direct convertor. The main benefit of directing the electrons out the end opposite to the direct convertor is that the heat load on the entrance grid of the direct convertor is drastically reduced; it only has to handle the power carried by the small fraction of ions intercepted by the grid and not the entire electron power.

The obvious way to design a tandem mirror reactor with a direct convertor is then to put the direct convertor at one end and to use a simple thermal dump for the electron heat load at the other end. This configuration was used in the Livermore tandem mirror reactor design⁽¹⁾ and is also used in WITAMIR-I. A unique feature of the tandem mirror is the narrow energy distribution of the ions entering the direct convertor. This is because they have fallen through the potential $\phi_c + \phi_e$ (see Chapter III) which is many times the ion temperature in the central cell. Consequently, a single stage plasma direct convertor (PDC) provides adequate efficiency.

A contributor to energy loss in the PDC are the high energy alpha particles. About 10% of the alpha particles are born inside the loss-cone of the central cell and immediately escape out the ends. Because their energy is much greater than the potential, they go equally out both ends. Approximately 15% of the fusion-born alphas scatter into the loss-cone while downscattering in energy; these also go equally out both ends unless one makes the peak field of the barrier at one end higher than that at the other end. Of the alpha particles entering the direct convertor, only a small amount of their energy is converted to electric power; they enter the PDC with a kinetic energy substantially greater than twice the PDC voltage.

The WITAMIR-I direct convertor is described in the following sections. The composition of the power incident on the thermal dump and entering the direct convertor is given in Section VII-B. Section VII-C describes the magnetic configuration of the direct convertor including the possibility of shielding the direct convertor against neutrons streaming through the end plugs. The details of the various grids and collector plates in the direct convertor are described in Section VII-D. The vacuum pumping system is presented in Section VII-E. Finally, the efficiency of the direct convertor is calculated in Section VII-F.

Reference for Section VII-A

1. G. A. Carlson, et al., "Tandem Mirror Reactor with Thermal Barriers," UCRL-52836, Lawrence Livermore National Laboratory (1979), Chapt. 7.

VII-B. Particle and Energy Flow to the Direct Convertor and Thermal Dump

The basic parameters describing the central cell plasma, the end loss rates, and the electrostatic potentials are given in Section III-C. These parameters determine the species mix and energy spectrum of the particles entering the direct convertor at one end and incident on the thermal dump at the other end.

The central cell ions escaping through the end plug have essentially zero parallel kinetic energy as they pass over the plug potential and are accelerated by the potential ($\phi_c + \phi_e$) before entering the direct convertor. They also carry with them a certain amount of perpendicular kinetic energy which is roughly T_{ic} , the central cell ion temperature. Consequently the mean energy of the central cell ions entering the PDC is 358 keV, while the minimum energy is 326 keV. This relatively narrow energy spectrum is advantageous for the direct convertor. The mean energy coupled with the ion loss rate gives a power of 444 MW entering the PDC. In addition, the plug ions (protons) leave the plug with an energy of about 375 keV. This contributes another 3 MW to the power entering the PDC.

The remaining ionic species are the fusion born alpha particles. These see a mirror ratio (determined by the barrier peak field and the beta-corrected central cell field) of 5. Since the alphas are born isotropically in velocity space, 10% are in the loss-cone and leave the central cell immediately with an energy of 3.5 MeV. They are then accelerated by the electric field and enter the PDC with an energy of 3.95 MeV, which corresponds to 71 MW of power. The remaining 90% of the alphas are magnetically trapped in the central cell and slow down, first by electron drag and then by scattering off ions when their energy is lower. Approximately 15% are estimated to enter the loss-cone with an energy of approximately 600 keV. These alpha particles contribute 27 MW. One half of the alpha particles are assumed to enter the PDC and the other half to go to the thermal dump. The remaining 75% of the alphas are electrostatically as well as magnetically contained in the central cell. For true steady-state operation they have to be removed by some other means, such as radial diffusion.

The central cell electrons are confined axially by the potential ϕ_e ; those electrons reaching the dump have little parallel kinetic energy and a perpendicular kinetic energy of about T_{ec} . This represents a power of 44 MW incident on the thermal dump. The plug electrons enter the central cell

electron population upon upscattering in energy to above their confining potential of $\phi_c + \phi_b$; this is not an additional loss from the system. The remaining electron species are the hot magnetically confined electrons in the thermal barrier. Since the mirror ratio is lower on the plug side of the barrier, these electrons do not enter the central cell but leave through the neighboring plug after scattering into the loss-cone. Hence one half go to the PDC and one half to the thermal dump. They leave the barrier with an energy of 135 keV and are then decelerated by the potential difference $(\phi_e - \phi_b)$ before reaching the thermal dump or PDC. They carry a total power of 12 MW.

In addition to the energy carried by charged particles out the ends, energy leaves the plasma by energetic neutral atom transport, mostly in the barrier, and by synchrotron and bremsstrahlung radiation. This is estimated to contribute 92 MW. This is relevant for the blanket and shield design, but not for the direct convertor or thermal dump, since they would receive only a minute portion of that power. In addition, in order to have a complete power balance for the combined central cell, barrier, and plug plasma, the radial diffusion of thermal alpha particles has to carry out 14 MW. Once the alphas diffuse radially beyond the plug, they are lost parallel to the field so this power appears at annular thermal dumps at each end.

The power entering the direct convertor is given by the sum of the central cell ion power, the plug ion power, half the alpha power, and half the barrier electron power, which adds to 502 MW. The thermal dumps receive the central cell electron power, the other half of the energetic alpha and barrier electron power, and all of the thermal alpha power. Secondary electron emission at the electron repeller grids in the PDC will contribute an additional 7 MW to the power on the thermal dumps. This is discussed in Section VII-F. The total power incident on the thermal dumps is then 120 MW.

The electrical current entering the direct convertor is given by the particle loss rate and charge for each species entering the PDC. The barrier electrons entering the PDC reduce the current by about 10% because of their negative charge and hence reduce the electrical power output correspondingly. The total current is 1170 amps. The efficiency of the direct convertor is calculated in Section VII-F.

These results are summarized in Tables VII-B-1 and VII-B-2.

Table VII-B-1
Input Power and Current to Plasma Direct Convertor

	<u>Power, MW</u>	<u>Average Energy, keV</u>	<u>Current, A</u>
Central Cell Ions			
(50% D-T)	444	358	1,238
Plug Ions	3	960	5
Prompt Alphas	35	3,950	18
Scattered Alphas	14	1,000	26
Barrier Electrons	<u>6</u>	52	<u>-117</u>
Total	502		1,170

Table VII-B-2
Input Power and Current to Thermal Dump

	<u>Power, MW</u>	<u>Average Energy, keV</u>	<u>Current, A</u>
Central Cell			
Electrons	44	33	-1,352
Barrier Electrons	6	52	-117
Prompt Alphas	35	4,000	18
Scattered Alphas	14	1,000	26
Thermal Alphas	14	?	257
Secondary Electrons	<u>7</u>	35	<u>-181</u>
Total	120		-1,349

VII-C. The Magnet System

The WITAMIR-I design incorporates a recircularizing magnet between the yin-yang plug magnets and the direct convertor, as shown in Fig. VII-C-1. The function of this magnet is primarily to reduce the forces on the yin-yang magnet system as discussed in Section VI-D. The flux surface at the PDC is not fully recircularized but is roughly a 2:1 ellipse. It could be fully recircularized, but that would unbalance the plug fields.

We have also investigated a method to shield the direct convertor from neutrons streaming down the axis and out the end plug. This method is to add a coil (or set of coils) on the axis, as shown in Fig. VII-C-1, to divert the magnetic flux. Shielding material is then placed in the private flux area inside the separatrix. Figure VII-C-2 shows the magnetic field lines in the horizontal and vertical planes. These two views are not identical since the flux tube is elliptical in cross-section at this point. The current carried in each coil is 5 MA-T, with the center coil having the reverse polarity of the inner and outer coils. The configuration shown here has not yet been optimized; it is result of an effort to determine the basic feasibility of this type of neutron plug. Supporting the center coil has not yet been investigated, but can probably be accomplished by supporting it from the outer coils; the support structure can be magnetically guarded so that it is not bombarded by the incident ion flux.

VII-D. Grid and Collector System

The minimum ion energy entering the direct convertor is 326 keV; hence the collector voltage relative to ground (the thermal dump) is set just below this value to collect all ions. We choose 319 kV in order to allow some loss of energy between the end plug and the collector; this could be due to scattering off neutrals or deflection by the non-planar electric fields near the grids. In front of the collector plate are the entrance grid and electron repeller grid, as shown in Fig. VII-D-1. The entrance grid is set slightly negative to keep electrons from entering the PDC; if allowed to reach the collector plate, the electrons would hit it with high energy. This would generate heat and reduce the collected current. The second grid (electron repeller grid) is set more negative (-35 kV) to correct for the non-planar electrostatic potential surfaces near the grids. The system is analogous to a triode vacuum tube, as noted by Barr.⁽¹⁾ The repeller grid voltage is set so

FIG. VII - C - I
MAGNET SYSTEM

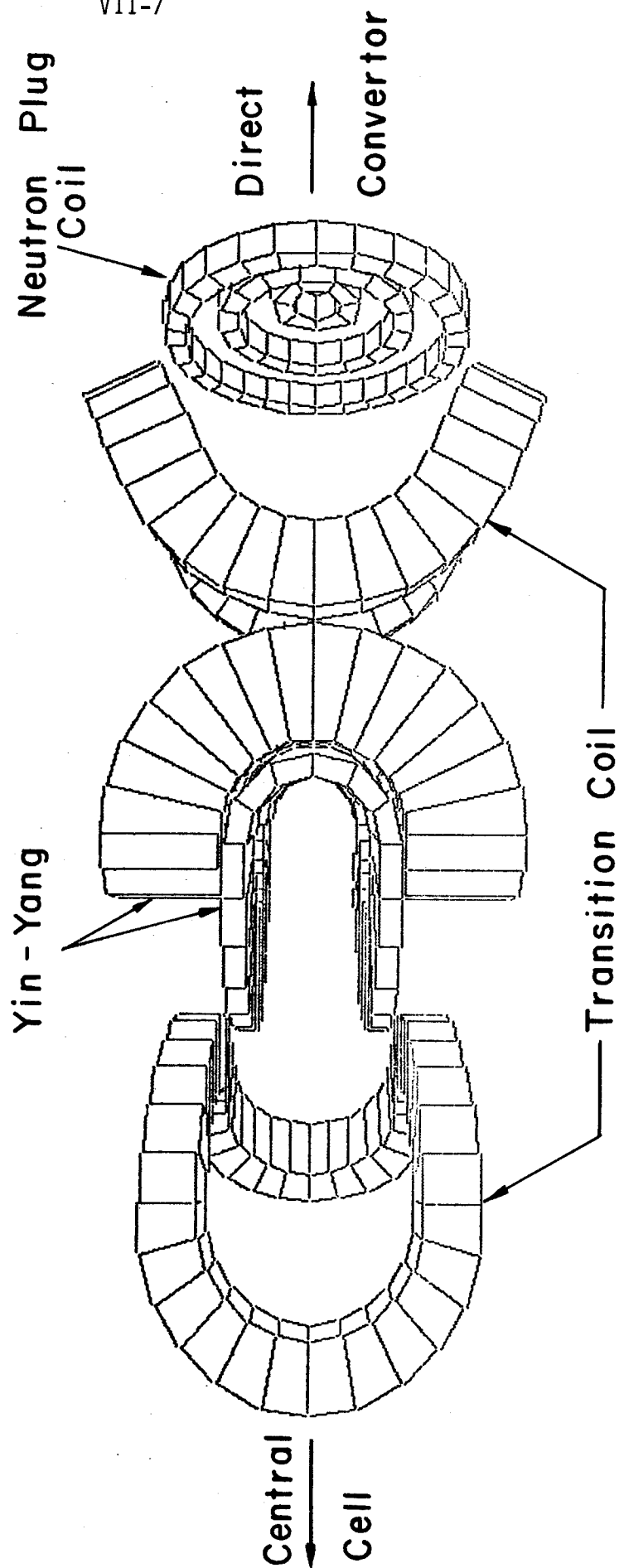


FIG. VII - C-2

MAGNETIC FLUX THROUGH NEUTRON PLUG

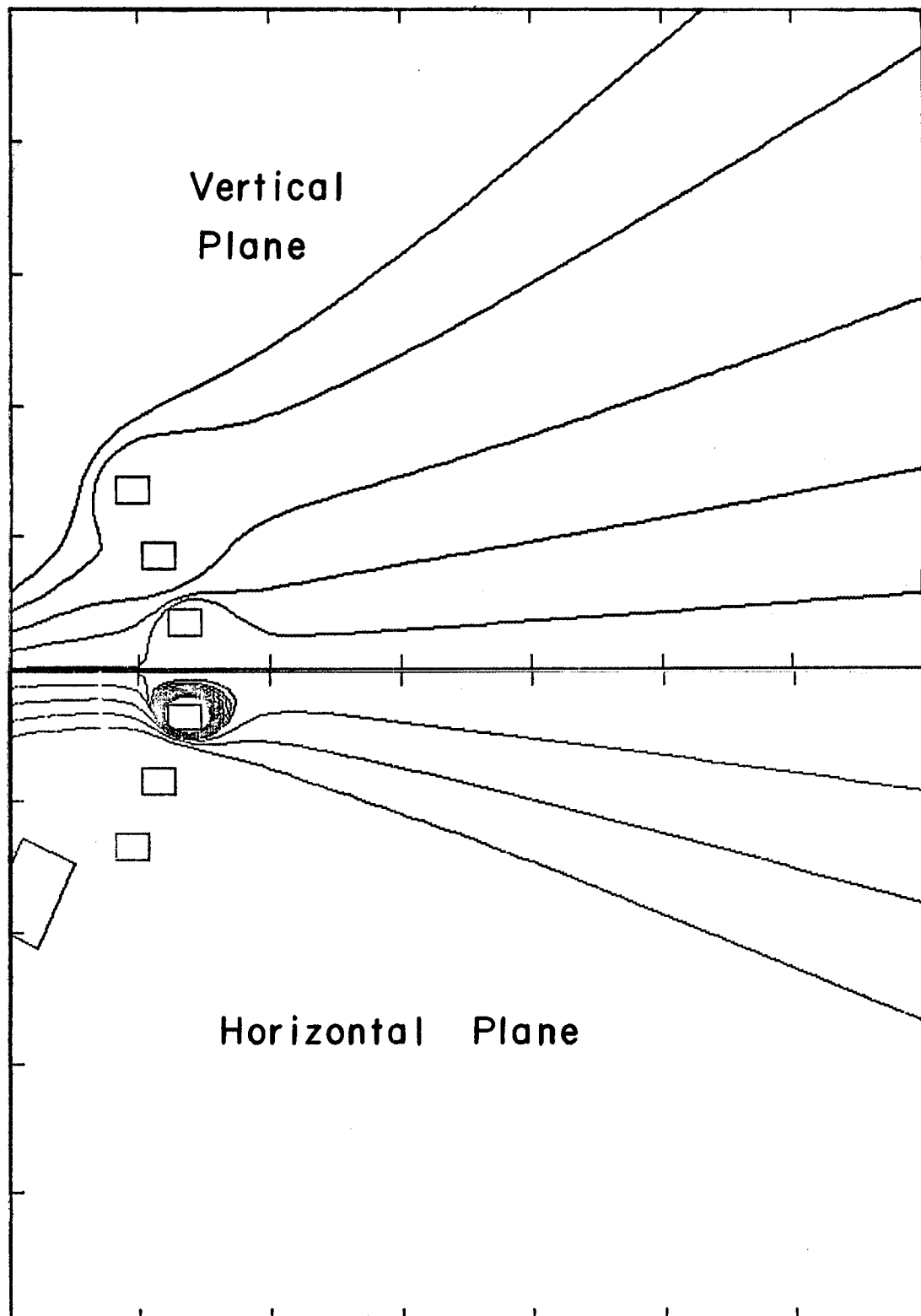
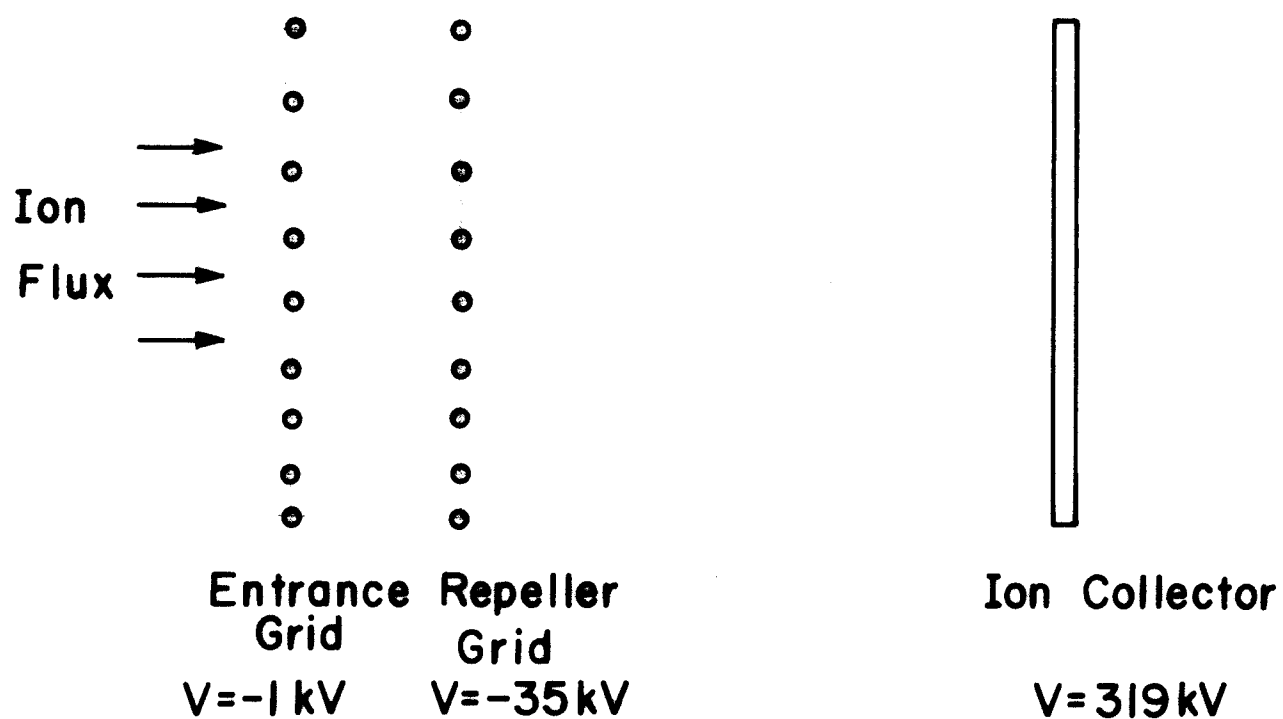


Fig. VII - D - I

GRIDS AND COLLECTOR IN THE DIRECT
CONVERTOR

that the system operates in the cutoff condition; electrons reaching the entrance grid are prevented by the repeller grid from reaching the plate.

The grids intercept a fraction of the ion current entering the PDC. This produces a heat load which must be removed by cooling. The cross-sectional area of the PDC is set by the requirement that the peak power density not exceed 300 W/cm^2 . Hence the area is 167 m^2 which is a radius of 7.3 meters. The ion current density in the PDC is then 7.2 amps/m^2 . The grid-collector spacing is set by the requirement that the current density be less than the space-charge-limited current density J_ℓ in a plane diode,⁽²⁾

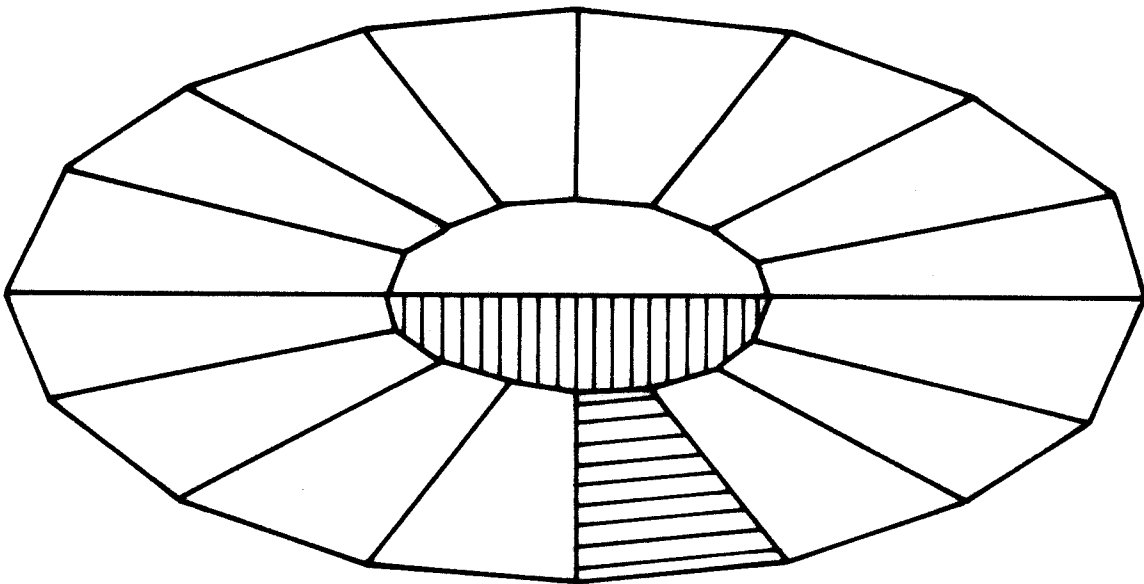
$$J_\ell = \frac{4\epsilon_0}{9} \sqrt{\frac{2q}{M}} \frac{V^{3/2}}{d^2}, \quad (1)$$

where V is the voltage across the diode of spacing d , q is the charge, M is the mass, and $\epsilon_0 = 8.85 \times 10^{-12}$ farads/meter. For the repeller grid-collector voltage, this gives a maximum d of 1 m. We choose this spacing to be 75 cm. Since the ions approach the collector with finite kinetic energy (average = 38 keV), using Equation (1) to set an upper limit on d is actually conservative.

The grids and collector are essentially a capacitor with stored energy. This energy can potentially be released in a spark; in order to prevent damage to the grids and collector plate if a spark were to occur, the capacitively stored energy should be kept below 10 J.⁽¹⁾ This is accomplished by segmenting the grids and collector, as shown in Fig. VII-D-2, with each segment acting as a separate electrical system. The total stored electrical energy is 123 Joules. With 18 segments as shown in Fig. VII-D-2, the energy per segment is 6.8 J, which provides some safety margin for the 10 J criterion.

Each grid, and its support structure, is assumed to be 95% transparent. Hence a total of 10% of the ion current entering the PDC is intercepted by the grids. In addition, secondary electron emission at the electron repeller grid produces a beam of electrons which travel back through the plasma and strike the thermal dump at the other end of the reactor. Assuming three secondary electrons per incident ion, the -35 kV power supply for the electron repeller grid needs to supply 242 amps, or 8.5 MW. Of this power, 7 MW is deposited as heat at the thermal dump. A total of 52 MW of heat is generated at the grids and 105 MW at the collector plate because the ions are collected with a finite amount of kinetic energy.

FIG. VII - D-2
SEGMENTED GRID SYSTEM
(The grid tubes are only shown
in 2 segments)



The engineering problems associated with the grid and plate are numerous. The problems arise from high energy particles which impinge on the structure. The input to the grid and plate are summarized on Table VII-D-1. The potential problems caused by the energy input are:

1. Material damage, especially blistering.
2. T inventory in the structure.
3. D-T neutron source and its shielding.
4. T lost to the coolant.
5. Cooling of 300 W/cm^2 .

It is clearly beyond the scope of this study to evaluate the consequences of all those problems. It is useful, however, to list the potential problems. The design of the direct convertor and the selection of the structural material and the coolant can only be done through an iteration process involving considerations of all those problems for an optimum design. For this study, we have decided, as a first choice, to use Ta-10 W as the structural material and water as the coolant.

The grid system receives a high heat flux of 300 W/cm^2 . A forced water stream with a velocity of 10 m/sec is required for the cooling. This velocity is high but not excessive. The resulting heat transfer coefficient is $5 \text{ W/cm}^2\text{-}^\circ\text{C}$. The heat flux on the plate is only 63 W/cm^2 . The large thermal input to the plate and its relatively low thermal loading warrants high temperature operation to recover the thermal energy. Boiling water cooling is therefore used for the plate to generate steam at 16.5 MPa and 330°C . This steam is fed to the superheater section of the steam generator to be heated to the desired steam temperature of 482°C . The preliminary summary of the cooling system for the direct convertor is listed in Table VII-D-2.

References for Section VII-D

1. G. A. Carlson et al., "Tandem Mirror Reactor with Thermal Barriers," UCRL-52836, Lawrence Livermore National Laboratory (1979), Chapt. 7.
2. J. D. Cobine, "Gaseous Conductors," Dover Press (1958), 123.

Table VII-D-1

Summary of Energy Flux on Direct Convertor Components

	Total Energy, MW	Energy Flux, W/cm ²	Form of Energy	Particle Energy, keV	Particle Flux #/cm ² -yr
Grid	52	300	D, T	350	2×10^{23}
			He	1000, 4000	3×10^{21}
			H	960	5×10^{20}
Plate	105	63	D, T	35	2×10^{23}
			He	3300	3×10^{21}
			H	640	5×10^{20}

Table VII-D-2

Thermal-Hydraulic Parameters for the Direct Convertor

Grids

Total thermal energy	52 MW
Peak heat flux	300 W/cm ²
Structural material	Ta-10 W
Coolant	H ₂ O
Coolant temperature	80 ~ 150°C
Coolant flow rate	6.4 x 10 ⁵ kg/hr
Coolant velocity	10 m/sec
Coolant heat transfer coefficient	5 W/cm ² -°C

Plate

Total thermal energy	105 MW
Peak heat flux	63 W/cm ²
Structural material	Ta-10 W
Coolant	Boiling H ₂ O
Coolant temperature	300 ~ 330°C
Coolant flow rate	2.9 x 10 ⁵ kg/hr
Coolant velocity	1 m/sec
Coolant heat transfer coefficient	4 W/cm ² -°C

VII-E. Vacuum System Design

The direct convertor will contain the vacuum pumps needed for removing the neutralized D, T, and He, and maintain the D & T partial pressure at $\sim 3 \times 10^{-5}$ torr. The same system will be used for evacuating the plasma chamber prior to an initiation of a burn cycle.

During operation, the direct convertor will receive the following influx of particles:

D & T	7.74×10^{21} atoms/sec
α	0.43×10^{21} atoms/sec .

At 300 K this is a throughput of:

D ₂ , T ₂ & DT	~ 120 torr·l/sec
He	~ 13.3 torr·l/sec .

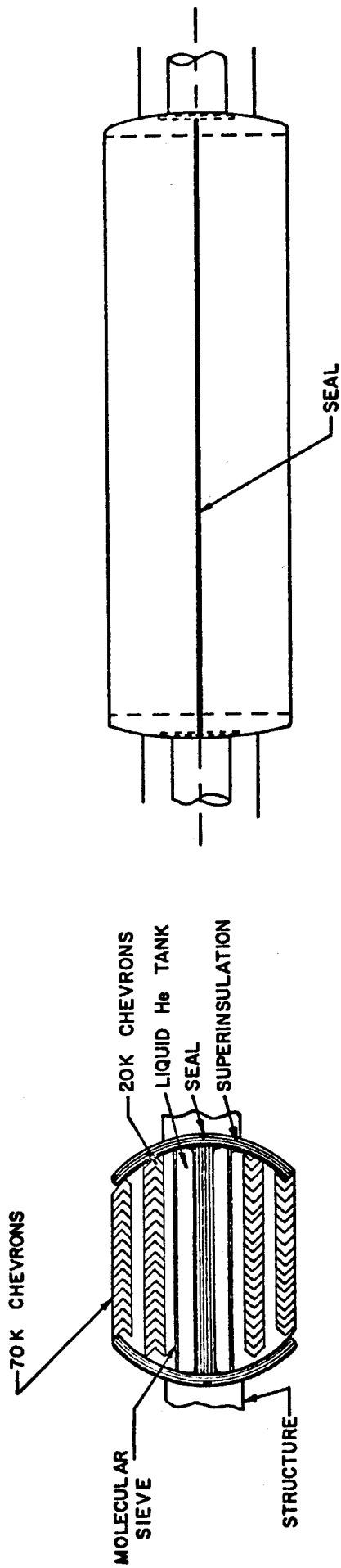
Assuming a pumping speed of a compound cryopump for D₂ and T₂ of ~ 5 l/cm² sec, that cryopump surface needed to maintain a D & T partial pressure of 3×10^{-5} torr in the direct convertor is 80 m². A typical compound cryopump will have a pumping speed of ~ 2 l/cm² sec for He. The helium pressure will therefore be

$$P_{\text{He}} = \frac{13.3 \text{ torr} \cdot \cancel{\text{l}} \cancel{\text{sec}} \cancel{\text{cm}^2}}{\cancel{\text{sec}} \cdot 2 \cancel{\text{l}} \cdot 80 \times 10^4 \cancel{\text{cm}^2}} = 8.3 \times 10^{-6} \text{ torr}$$

and the total pressure in the direct convertor

$$P_{\text{tot}} = 3.8 \times 10^{-5} \text{ torr} .$$

We envision that the compound cryopumps used in the direct convertor will be of the reversible kind, first proposed in TETR⁽¹⁾ and subsequently in NUWMAK⁽²⁾. The advantage of this pump, shown in Fig. VII-E-1, is that it requires half as much space as does a conventional cryopump. As can be seen from the figure, this pump has a front and a back cryopanel surface. While the front surface is actively pumping, the back surface is being regenerated.



SHAFT FOR ROTATING PUMP

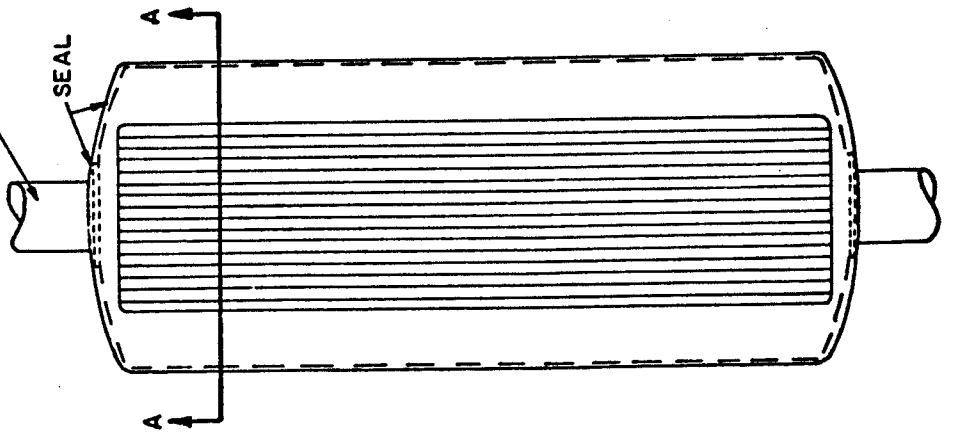


Fig. VII-E-1
Sketch of Proposed Rotatable Cryosorption Pump

After four hours of pumping, the pump is rotated 180° and a freshly regenerated cryosurface is ready to resume pumping.

Each pump has a surface area of 2 m^2 on each side. Thus, there will be 40 such pumps, 20 on either side of the direct convertor enclosure. The total surface area needed to house the forty pumps is $\sim 100 \text{ m}^2$.

References for Section VII-E

1. B. Badger et al., "TETR, A Tokamak Engineering Test Reactor," University of Wisconsin Fusion Engineering Report UWFDM-191 (Dec. 1977).
2. B. Badger et al., "NUWMAK, A Tokamak Reactor Design Study," University of Wisconsin Fusion Engineering Report UWFDM-330 (March 1979).

VII-F. Efficiency of the Direct Convertor

The total power input to the direct convertor is 502 MW. The output current is 1053 amps at 319 kV, which gives an output power of 338 MW. However, 8.5 MW is drawn by the electron repeller grid power supply, so the net electrical output is 330 MW, which yields an efficiency of 65%. In addition, the heat generated at the collector plate and at the thermal dump is utilized in the thermal cycle and therefore contributes to the electrical output of the plant.

The various losses in the direct convertor are listed in Table VII-F-1. The major loss is the heat generated by the central cell ions striking the collector plate, but this is still only 10% of the ion power entering the direct convertor. The next major contributor are the hot electrons from the barrier; they are not stopped by the electron repeller grid. Instead they are accelerated by the positive potential of the plate and hence generate additional heat in the plate. Those alpha particles born in the loss cone of the central cell and therefore leave the plasma with their full energy are the next major contributor to the losses. The last major contributor is the interception loss at the two grids.

There are a number of improvements that could be made to increase the efficiency. Smaller grid tubing would increase the transparency of the grids and reduce the interception loss. A two stage collector, perhaps based on the venetian blind concept,⁽¹⁾ set to better collect the central cell ions, could increase the fraction of their energy recovered. Since the prompt alphas are monoenergetic at 3.95 MeV, it is tempting to consider a two stage collector with the second stage set at 1.92 MV to recover almost all the prompt alpha energy. A potential difficulty with this is that the hot electrons will fall through to the 1.92 MV potential and hence their contribution to the losses will be greatly increased. Perhaps they can be prevented from doing so by their drastically different charge/mass ratio. If this problem can be solved, then the net electrical output of the PDC might be increased by about 80 MW, if all the above improvements were utilized. The reduced heat to the thermal cycle however would cause the net output of the plant to be increased by about 50 MW. Further study is required to determine the feasibility of these possible improvements.

Table VII-F-1
Losses in the Direct Converter

	<u>Power, MW</u>	<u>Mean Energy at Impact, keV</u>
Entrance grid		
- central cell ion interception	22	359
- prompt alpha interception	3	3,950
Electron repeller grid		
- central cell ion interception	24	393
- prompt alpha interception	3	3,950
Collector plate heat generation		
- central cell ions (50% D-T)	42	39
- plug ions	2	382
- prompt alphas	25	3,320
- scattered alphas	5	410
- hot electrons	40	371

Reference for Section VII-F

1. R. Moir, W. L. Barr, Nuclear Fusion 13, 35 (1973).

VIII. Blanket Design

VIII-A. Design Philosophy

The TMR design philosophy is to use the experience obtained from the recent studies on magnetically confined D-T tokamak reactor system studies, and combine it with the unique features of the tandem mirror to obtain an attractive design of a tandem mirror power reactor. The study is aimed at maximizing the strengths of the tandem mirror while mitigating its weaknesses. The end product should be a safe, reliable, maintainable and a relatively economic power reactor. The blanket design described here is consistent with this philosophy.

Perhaps the most attractive feature of the TMR is the simplicity of the central cell. The design philosophy has been to take full advantage of this basic cylindrical geometry to come up with a blanket which is simple to fabricate, lends itself to mass production and can be realistically maintained by remote control.

Early design studies have utilized high temperatures and advanced technology to obtain higher efficiency thermal cycles and thus minimize environmental impact. However, it was soon realized, partly based on our experience with the UWMAK-III study, that high temperature systems present severe problems, especially in the areas of tritium confinement, radiation damage and power cycle technology. It also became apparent that a higher efficiency does not necessarily translate into better economics. For electricity from a fusion reactor, the capital cost portion of the total power cost will dominate, while the fuel cost is minimal. This argues strongly in favor of high plant availability. Assurance against a low availability must come not only from careful design, but also from margins of safety against deleterious effects yet unknown. Design conservatism is the best approach most likely to result in a fusion reactor that stays on line. For this reason, we have chosen a moderate temperature for the blanket and a high pressure steam cycle for the power conversion system. To minimize the temperature difference between the blanket structure and the power cycle, we use a liquid metal as the coolant and the intermediate loop is eliminated.

VIII-B. General Description and Mechanical Design

The blanket in WITAMIR-I consists of two distinct zones:

- 1 - The front zone which is composed of four rows of close packed tubes.

- 2 - The back zone consisting of a single row of hollow rectangular beams which provide the structural support for the blanket.

Figure VIII-B-1, which is a cross section of the central cell, shows that both zones are manifolded at the top and the bottom. The molten breeding material $\text{Li}_{17}\text{Pb}_{83}$ comes in through a single header feeding a blanket module, is distributed axially in the top manifold, then flows through both zones of the blanket, collecting in the bottom manifold and exiting through a single return header.

The width of a blanket module is 463 cm and there are 33 modules in the central cell. The first row in each module consists of 45 tubes, 10.25 cm in outer diameter and 0.2 cm wall thickness. Because of the close packed triangular pitch configuration, the end tubes in the second and fourth rows are specially shaped as shown in Fig. VIII-B-2 to fit in the space available. Except for these special tubes, all the other tubes in the second, third and fourth rows are 10.25 cm in outer diameter and have a wall thickness of 0.25 cm. The tubes are curved to follow the general circular contour of the plasma in the central cell. They are also bent on the ends such that they connect to the tube sheets at 90°. This is deemed important both from assembly considerations and from the consideration of removing the welded zones from direct line of sight of the plasma. At the point of attachment to the tube sheets, the tubes are swaged to ~ 92% of the original diameter. They are then welded to the tube sheets from the back side.

The rectangular beams which follow the tube banks are 28 cm deep and 10.25 cm wide. Thus, there are 45 beams on each side of a blanket module. Each beam has three square passages 8.5 cm x 8.5 cm running clear through. To avoid bending such structural beams over a sharp bend radius it was decided to attach them to the tube sheets at an angle as shown in Figure VIII-B-1. When the beams are welded to each other on the ends and then welded to the tube sheets, they become part of the distribution manifolds.

The whole blanket is made of the martensitic steel alloy HT-9 which is under development in the fast breeder program. Preliminary tests indicate that this alloy has the potential for significantly increasing the first wall/blanket lifetime relative to 20 CW 316 stainless steel, particularly up to temperatures of ~ 520°C.⁽¹⁾ Although HT-9 is not commonly or extensively used in industry, no difficulties can be foreseen in its fabrication. Wrought seamless tubes and hollow beams such as those needed for the WITAMIR-I blanket can

CROSS SECTION OF WITAMIR I
CENTRAL CELL

VIII-3

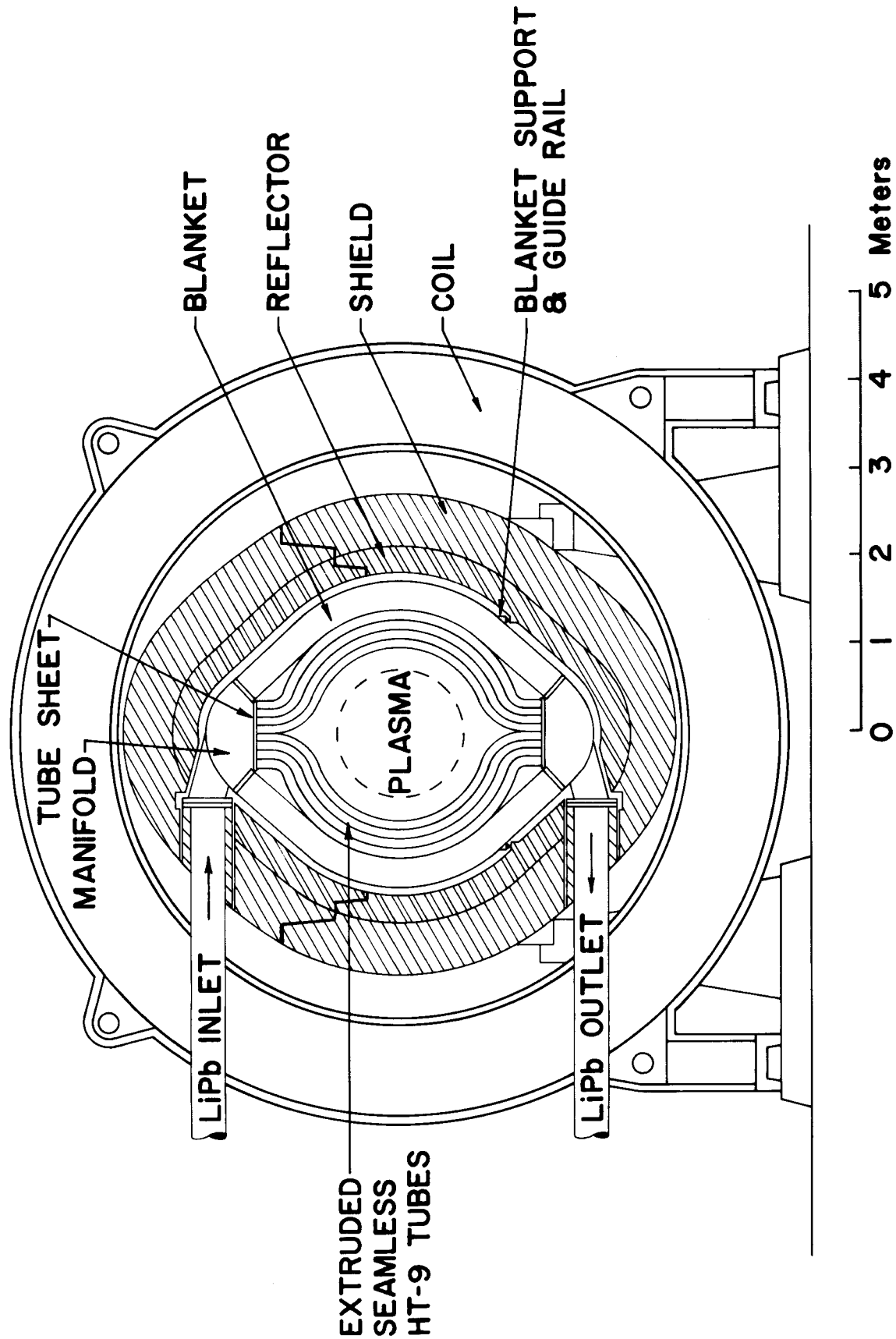
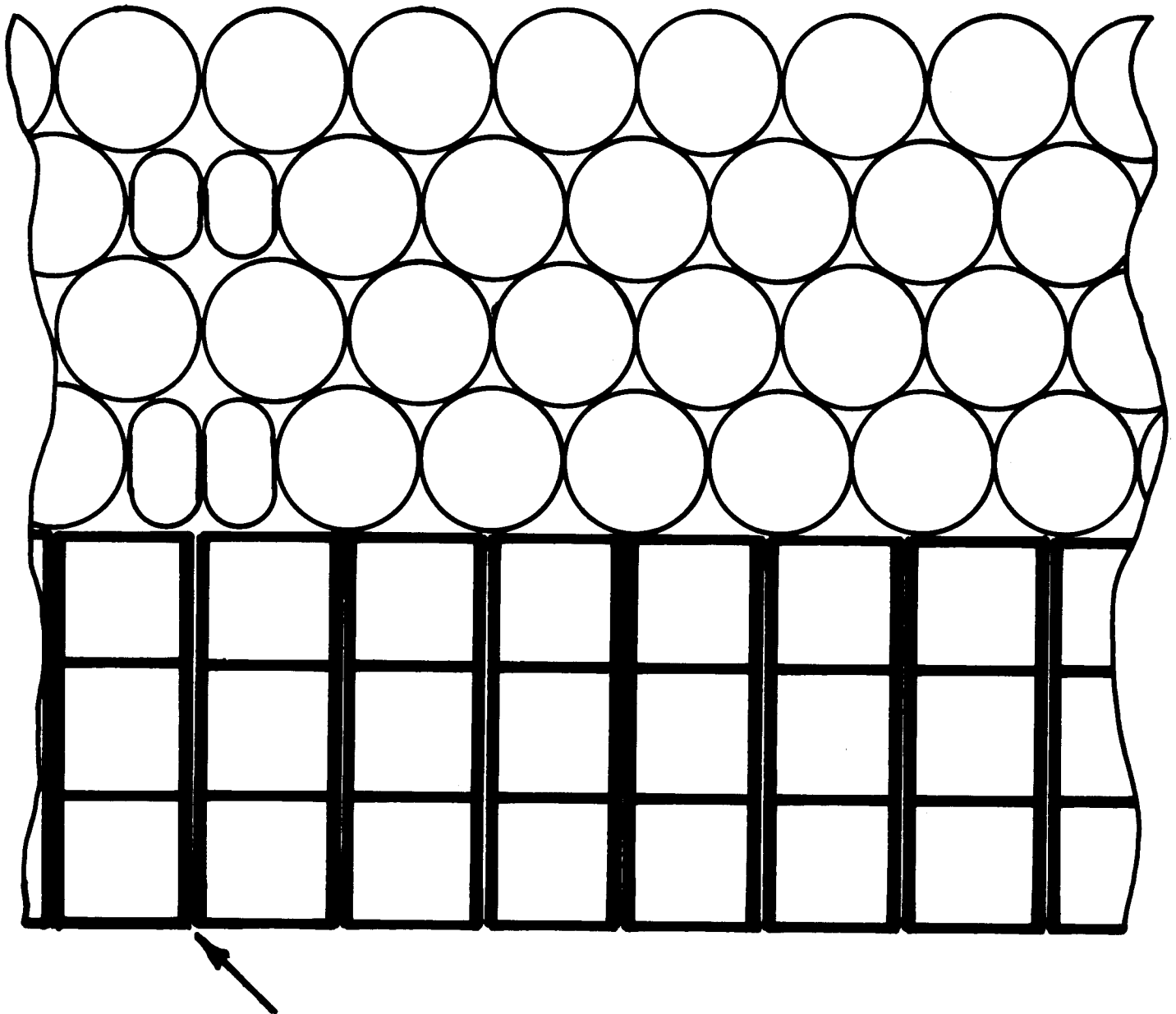


Fig. VIII-B-1

P L A S M A



JUNCTION BETWEEN ADJACENT
BLANKET MODULES

Fig. VIII-B-2 Cross section of the WITAMIR-I blanket showing
a junction between adjacent blanket modules.

be fabricated today with no extrapolation of present technology. Martensitic steels with carbon content greater than 0.1% will require preheating before welding and post weld heat treatment. Since the blanket described here will require a minimum amount of welding, this does not appear to be a major impediment.

The blanket module is supported on rails which are attached to the reflector structure. The reflector is part of the shield which in turn is supported on pedestals located between central cell coils. The support elements are welded to the back side of the blanket as shown in Fig. VIII-B-1. In this way the bearing stresses are distributed over a large area and are carried by the structural elements of the blanket.

Perhaps the most outstanding feature of the WITAMIR-I blanket is its simplicity. Some of the other features which make this blanket attractive are listed below:

- 1 - Because of the seamless tubes, no welded parts are exposed to the plasma. Furthermore, any welding done on the blanket is out of direct line of sight of the plasma.
- 2 - Simple straightfoward geometry makes it easy to fabricate. All the elements needed can be fabricated today with no extrapolation of technology.
- 3 - Only two low pressure breeding/cooling material connections have to be made for each module.
- 4 - The relatively low surface wall heating combined with the higher than stainless steel thermal conductivity of the HT-9 alloy result in negligible thermal stresses. Furthermore, the tubular geometry and the low operating pressure result in very low overall stresses.
- 5 - The blanket support scheme is simple and practical. The large scrape-off zone makes allowances for small blanket misalignments.
- 6 - The whole blanket module can be factory assembled and completely tested prior to shipment to the reactor site.

VIII-C. Fabrication

Some aspects of the blanket fabrication have already been discussed in the previous section. In this section we will outline the sequence of steps needed in building up a complete blanket module.

- The seamless tubes are cut to size.
- The tube ends are swaged.

- The tubes are bent to the appropriate shape depending on which row they fit in.
- At this point the tubes will undergo a thorough cleaning to remove any hydrocarbons used in the swaging and bending process.
- A special fixture is used to assemble the tubes in the same way they will be in a module.
- Tubesheets are added on the ends and the tubes welded to the tubesheets from the back side.
- In a similar way, the wrought seamless beams are cut to length, bent into shape and cleaned.
- The beams are assembled in a separate fixture and welded to each other on the open ends.
- The beam assemblies are then fitted to the tube assembly and welded to the tubesheets at the top and bottom.
- Prefabricated manifold covers are then fitted to the tubesheets and welded.
- Supply and return header attachment stubs are welded to the manifolds.
- Blanket support elements are aligned and welded to the beam assemblies on both sides of the blanket module.
- Post weld heat treatment can now take place.
- The blanket module is cleaned thoroughly and leak checked on a mass spectrometer.
- The last step in the module fabrication is a test under simulated operating conditions with $\text{Li}_{17}\text{Pb}_{83}$ circulated in it.
- The blanket module is then packaged and shipped to the reactor site with no additional machining or adjusting needed for installation.

VIII-D. Hydraulics

The dominant force on a conducting fluid across magnetic field lines in a magnetically confined fusion reactor is the MHD force. The effect of the MHD force is to increase the pressure drop and retard heat transfer by suppressing turbulence. A conducting fluid is usually a good heat transfer medium and conducting heat transfer is sufficient. The MHD pressure drop will increase the stresses in the blanket and will increase the pumping power. Therefore, the MHD effects have to be evaluated, both on heat transfer and pressure drop.

The Hartman pressure gradient arises in fully developed laminar flow across a uniform transverse magnetic field. For such a flow in a cylindrical

tube, in a uniform magnetic field normal to the tube, the pressure gradient is given by the following equation:

$$-\frac{dP}{dx} \approx \frac{\mu v}{a^2} \left[\frac{H^2 \tanh H}{H - \tanh H} + \frac{H^2 C}{1 + C} \right] \quad (1)$$

in which $C = \text{wall conductance} = \sigma_w t_w / a \sigma$

$a = \text{radius of the tube}$

$H = a B_{\perp} \sqrt{\sigma/\mu}$

$v = \text{bulk velocity}$

$\mu = \text{viscosity}$

$B_{\perp} = \text{component of the magnetic field perpendicular to the bulk velocity}$

$\sigma = \text{electrical conductivity of the coolant}$

$\sigma_w = \text{electrical conductivity of the wall material}$

$t_w = \text{wall thickness.}$

When the fluid is in a channel with a conducting wall and under large magnetic fields ($H \gg 1 + 1/C$), the second term of the equation dominates, giving

$$-\frac{dP}{dx} \approx v B_{\perp}^2 \sigma_w t_w / [a(1 + C)] .$$

For a thin walled tube, C is on the order of 10^{-2} , or

$$-\frac{dP}{dx} \approx \frac{v B_{\perp}^2 \sigma_w t_w}{a} .$$

This contains an interesting conclusion. The MHD pressure drop for fully developed laminar flow across a uniform magnetic field line is independent of the properties of the flowing material. For the first bank of tubes in this study, where the velocity is highest, the pressure drop $\Delta P = 0.69$ MPa, as calculated for the following conditions:

$v = 12.5 \text{ cm/s}$

$\sigma_w = .95 \times 10^6 \text{ mho/m}$

$a = 5 \text{ cm}$

$B_{\perp} = 3.6 \text{ Tesla}$

$t_w = .3 \text{ cm}$

$x = 7 \text{ m}$

This is a very moderate pressure drop and can be easily accommodated.

This design provides the flexibility of running the coolant from the top to the bottom or vice-versa in order to mitigate the problems of radiation damage. Therefore, the maximum pressure in the blanket is the sum of the MHD pressure drop and the pressure head of the LiPb in the blanket. The distance between the coolant inlet header and the outlet header is 5 m which has a pressure head of 0.46 MPa. The maximum blanket pressure is, therefore, 1.15 MPa.

VIII-E. Heat Transfer

The flow in a strong magnetic field is characterized as Hartman flow, namely, the flow is laminar with a flat profile and a very thin boundary layer. The heat transfer in such a system is dominated by conduction. The surface heat load on a TMR in the central cell is very small, such that over 95% of the heat is deposited in the coolant. Therefore, the function of the coolant is not to transfer heat from a solid surface; rather, it is to transport the heat from the blanket to the power cycle. For this reason, a conductive heat transfer mode is sufficient.

An exact solution for conductive heat transfer in a coolant channel with both a surface heating and non-uniform volumetric heating cannot be obtained. However, a numerical solution is easily available by solving the following set of finite difference equations:

$$V_i \rho C_p (T_i' - T_i) = [\sum_j \text{COND}_{ji} (T_j - T_i) + V_i Q_i''' + v A \rho C_p (T_{i-1} - T_i)] \Delta \theta$$

with the time step, $\Delta \theta$, defined as

$$\Delta \theta \leq [V_i \rho C_p / \sum_j \text{COND}_{ji}]_{\min}$$

in which

- V_i is the volume of zone i
- ρ is the density of the material
- C_p is the specific heat
- T_i is the temperature of zone i before iteration
- T_i' is the temperature of zone i after iteration
- COND_{ij} is the conductance between zones i and j
- Q_i''' is the volumetric heat rate at zone i
- v is the coolant velocity

A is the coolant channel cross section area in zone i

T_{i-1} is the temperature of the zone upstream of zone i .

By assuming an initial temperature distribution, all the new temperatures can be calculated as a function of the time at each time step, $\Delta\theta$. The temperature distribution will thus converge to a steady state which is the desired blanket temperature distribution.

The heat transfer calculations are based on input provided by neutronics calculations. The spatial nuclear heating rate is shown in Fig. VIII-E-1. The maximum nuclear heating rate in the blanket is 13.5 watts/cm^3 for a 2.4 MW/m^2 neutron wall loading. The coolant inlet and outlet temperatures are 329 and 500°C , respectively, as a compromise between blanket design and power cycle efficiency. The heat transfer calculations are summarized in Table VIII-E-1. The temperature distribution in the first row of the tubes is shown in Figure VIII-E-2.

The most important feature of this blanket is the small temperature difference between the structure and the coolant. This is due to the small surface heat load and the use of liquid metal as both breeding and cooling material. This small temperature difference results in a high thermal conversion efficiency while maintaining a conservative blanket design.

VIII-F. Stress Effects

Although a rigorous analysis of the stresses in the blanket has not been performed, calculations indicate that the stresses are not excessive. The following assumptions have been made to determine the stresses:

- 1 - The vertical temperature distribution in the various blanket components is uniform. Such a condition can be achieved by modulating the coolant flow in such a way that the exit temperature from all the passages in the blanket is the same.
- 2 - Sufficient contact exists between the banks of tubes as to distribute contact stresses over a large area.
- 3 - A fairly uniform load distribution exists over the entire support area between the blanket support element and the reflector on which it bears.
- 4 - The assumed surface wall loading is 2 W/cm^2 and the nuclear heating is 13.5 W/cm^3 . The coolant pressure is 1.15 MPa or $\sim 160 \text{ psi}$.
- 5 - The properties of HT-9 at 500°C are:

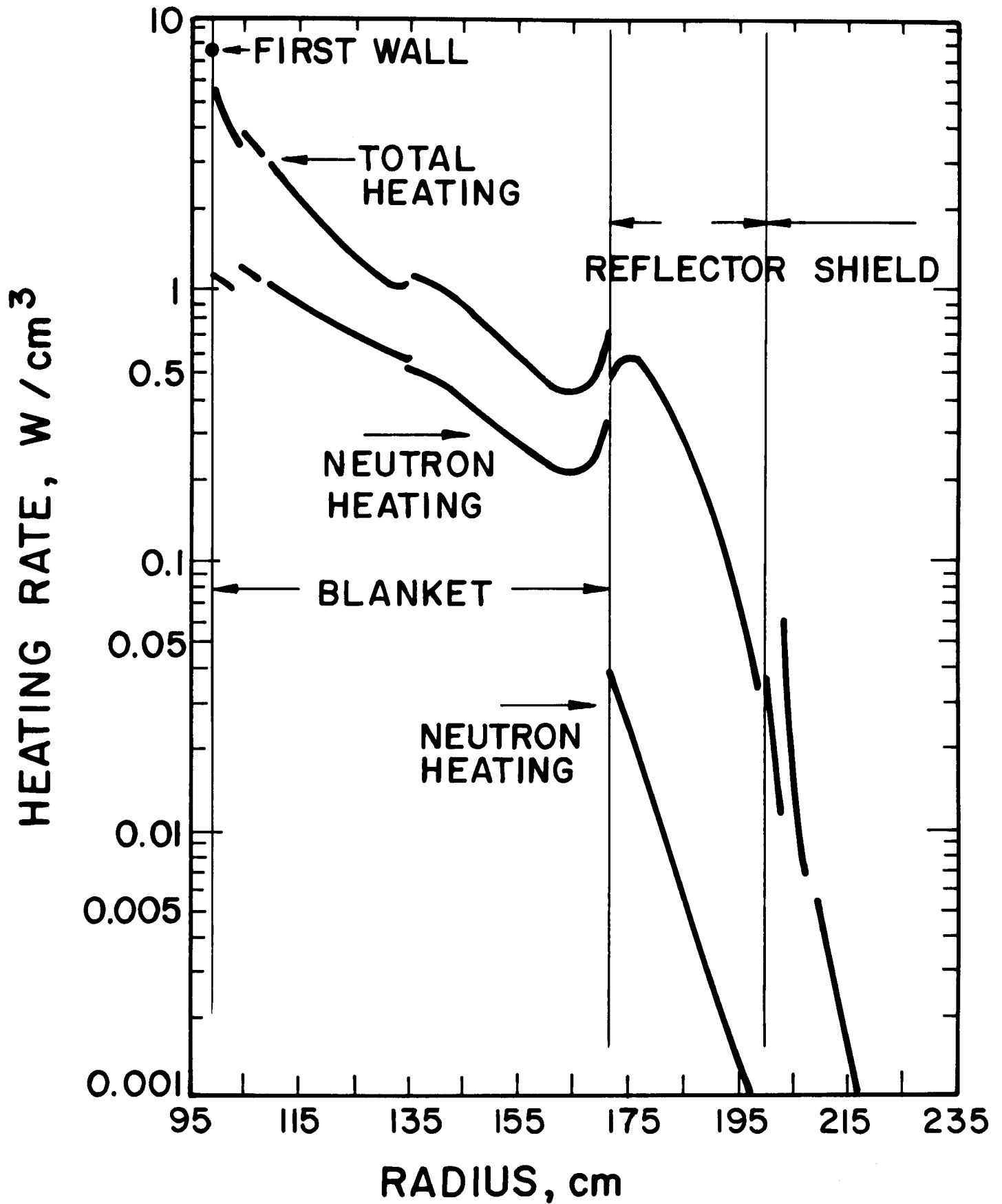


Fig. VIII-E-1

Table VIII-E-1. Major Thermal Hydraulic Parameters

Total Central Cell Power	3317 MW
Neutron Wall Loading	2.4 MW/m ²
First Wall Heating Load	2 W/cm ²
Coolant Temperature	
Inlet	329 C
Outlet	500 C
Maximum Structure Temperature	530 C
Maximum Coolant Velocity	12.5 cm/s
MHD Pressure Drop	.69 MPa
Maximum Blanket Pressure	1.15 MPa
Total Coolant Flow Rate	4.4 x 10 ⁸ kg/h
Steam Conditions	
Temperature	482 C (900 F)
Pressure	16.5 MPa (2400 psi)
Reheat Temperature	482 C (900 F)
Gross Thermal Efficiency (steam cycle)	42%
Estimated Net Efficiency (including direct cycle)	39.4%

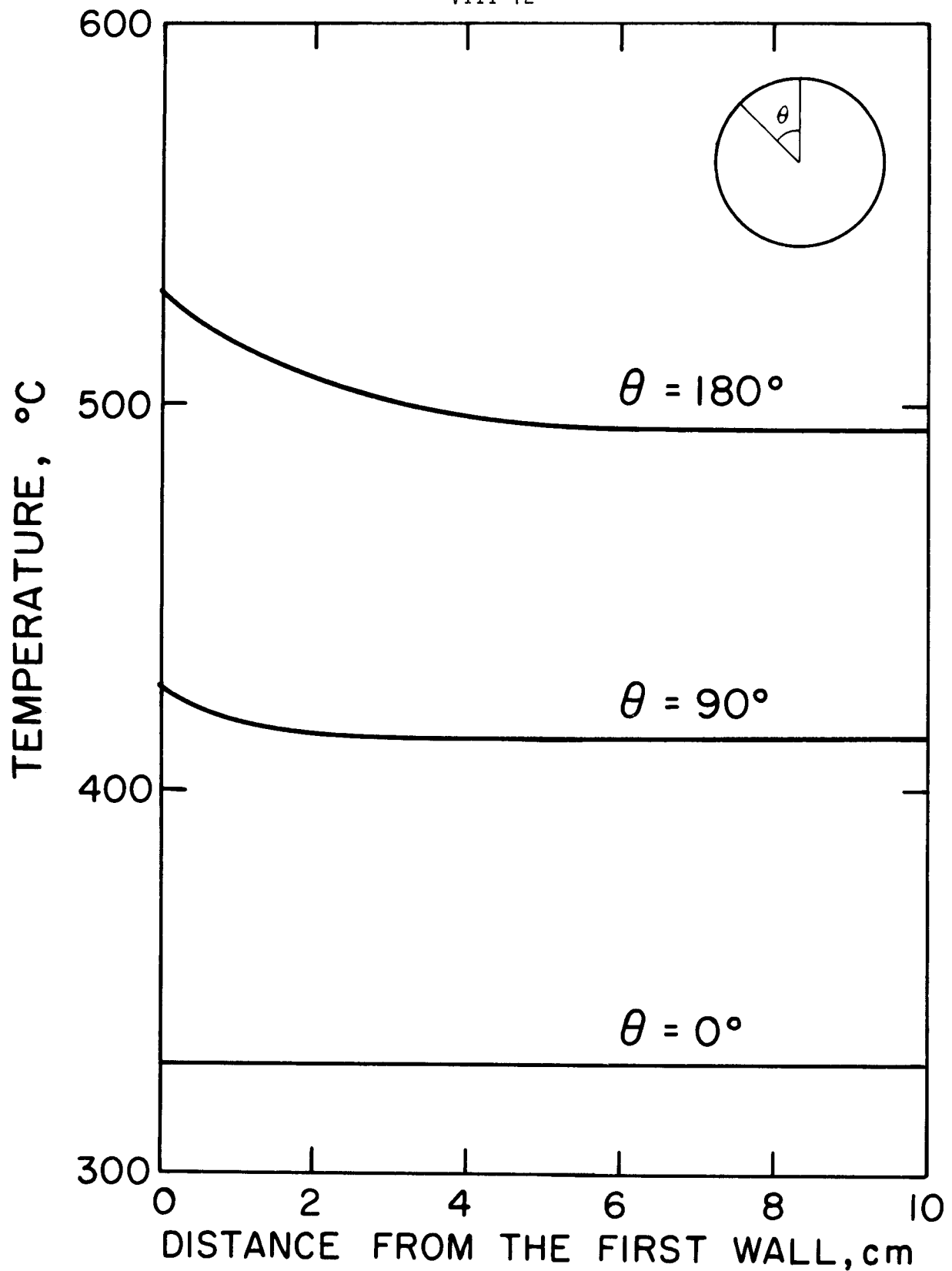


FIGURE VIII -E -2 TEMPERATURE DISTRIBUTION OF THE FIRST ROW OF TUBES AT DIFFERENT AZIMUTH DIRECTIONS

Thermal conductivity	29 W/m°C
Coefficient of expansion	$12.2 \times 10^{-6}/^{\circ}\text{C}$
Modulus of elasticity	1.62×10^5 MPa

Table VIII-F-1 gives the stresses in the blanket.

The maximum stress is in the rectangular beam at the support where the bending moment on the support element adds to the pressure stresses in the beam. This stress is 96 MPa, 25% lower than the maximum allowable stress intensity for HT-9 at 500°C as defined by the ASME Boiler and Pressure Vessel Code for Class I nuclear components (N47).

Table VIII-F-1. Stresses in the WITAMIR-I Blanket

	MPa	psi
Thermal stress at first wall tubes	± 7.9	$\pm 1,146$
Pressure stress in first wall tubes	-29.5	- 4,279
Max. stress in the first wall tubes	-37.4	- 5,425
Max. pressure stress in the rear beams	+78.8	+11,429
Stresses in the beams due to bending at support	± 17.24	$\pm 2,500$
Max. stress in the beam at support	+96.0	+13,929

(negative stresses are tensile, positive are compressive)

VIII-G. MHD Calculations for Feed and Discharge Tubes

The Hartman and end of the loop MHD pressure drops can be calculated by the following equations:

$$-\frac{dp}{dx} = \frac{VB^2\sigma_w t_w}{a}$$

and

$$\Delta P = k_{PIE}\sigma a V^2 \Delta B^2$$

in which: $-dp/dx$ is the pressure drop due to a uniform magnetic field

ΔP is entrance effect pressure drop

V = bulk velocity = .43 m/sec

B = magnetic field = 3.6 Tesla

σ_w = electrical conductivity of the wall = $.95 \times 10^6$ mho/m

t_w = wall thickness = 1 mm

a = channel half width = .5 cm

k_{PIE} = pressure drop coefficient as defined by Fig. VIII-G-1

= .1 at $LG/b = 3$ and $c \approx 0$

Δx = total length = 5 m

σ = electrical conductivity of $Li_{17}Pb_{83}$ = $.6 \times 10^6$ mho/m

t_w is kept small by using a 1 mm thick insert inside the structural table.

The Hartman and end of the loop pressure drops are .17 MPa and .05 MPa respectively. This will add .4 MPa to the total pressure drop and .2 MPa to the blanket pressure.

Reference for Chapter VIII

1. S. N. Rosenwasser, P. Miller, J. Dalessandro, J. Rawls, W. E. Toffolo, W. Chea, "The Application of Martensitic Stainless Steels in Long Lifetime Fusion First Wall/Blankets", General Atomic Co., San Diego, CA.

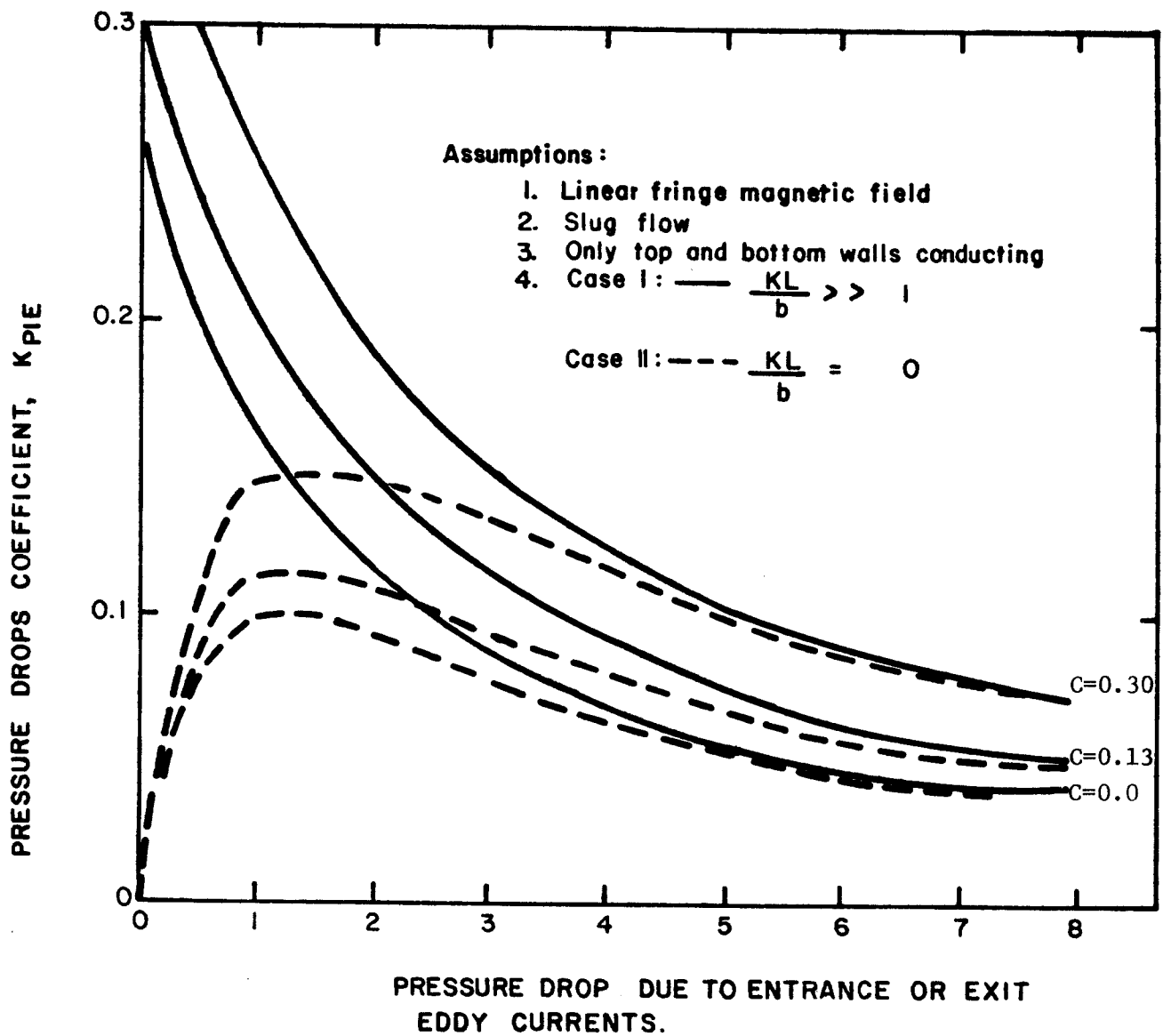


Fig. VIII-G-1

IX. Neutronics Analysis and Radioactivity

The nuclear analysis of WITAMIR-I is presented in three parts. First, a cylindrical model of the central cell is analyzed to establish the blanket and central cell shield design. The activation and inventory based on these results are detailed in the second section. Finally, preliminary assessment of the leakage of radiation from the central cell into the barrier, end plug, and direct convertor regions is given.

IX-A. Central Cell Neutronics Analysis

IX-A-1. Introduction

In this section, we present the neutronic design analysis for the TMR central cell blanket and shield. In essence, the blanket is simply an energy convertor which is coupled to the energy producer, the plasma, while the shield is a device to protect the magnets from radiation and heat generated from the energy producer and blanket. The primary neutronic design requirements for the blanket and shield unit are:

- . It must breed tritium.
- . It must convert the kinetic energy of the fusion reaction into heat.
- . It must attenuate the flux and provide adequate protection for the magnets and other equipment against radiation and heat.
- . It must be resistant to radiation damage.

These requirements are absolutely essential for the design of a viable blanket and shield and are also subject to economic and environmental constraints. There are many secondary features, which are not absolutely essential, but would be desirable. These features would include:

- . Low blanket and shield activity.
- . High blanket energy multiplication.

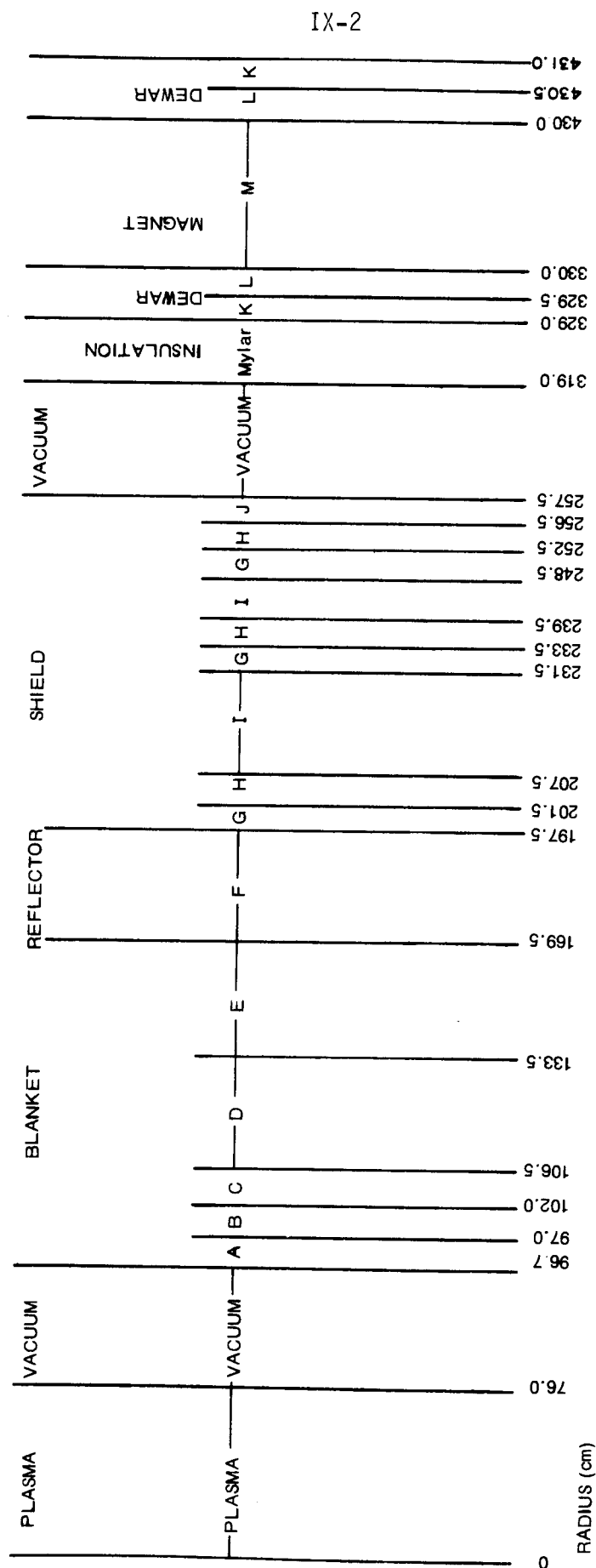
The TMR blanket and shield, discussed elsewhere in this report in more detail, will consist of a series of tube banks made of the ferritic steel HT-9, running circumferentially around the plasma reaction chamber. A eutectic mixture of $\text{Li}_{17}\text{Pb}_{83}$ flows through the tubes to provide cooling and tritium production. The tube bank is followed by a water cooled reflector, and energy deposited is sufficient to warrant utilization. A sixty centimeter shield is followed by insulation and then the magnets.

A schematic of the TMR blanket and shield is shown in Figure IX-A-1. This particular design was the result of a series of parameter studies, and it meets our basic requirements. In addition we have achieved a high blanket

Fig. IX-A-1

BLANKET - SHIELD - MAGNET

Schematic



COMPOSITION TABLE

A - 100 % FERRITIC STEEL (HT - 9)	H - 87 % B ₄ C
B - 79 % Li ₁₇ Pb ₈₃	I - 95 % F.S., 5 % H ₂ O
C - 84 % Li ₁₇ Pb ₈₃ , 7 % FERRITIC STEEL (F.S.)	J - 100 % F.S.
D - 81 % Li ₁₇ Pb ₈₃ , 9 % F.S.	K - 100 % Al 2219
E - 75 % Li ₁₇ Pb ₈₃ , 25 % F.S.	L - 100 % He
F - 5 % H ₂ O, 95 % F.S.	M - 17.8 % Al, 69 % Al 2219, 4 % He, 9.2 % INSULATION
G - 90 % Pb, 5 % H ₂ O, 5 % F.S.	

energy multiplication through material selection and design. In Table IX-A-1, the basic neutronic results are given. In the following sections, these and other neutronic design parameters will be discussed.

IX-A-2. Methodology and Data

The neutronic and photonic calculations were made with the one-dimensional code ANISN.⁽¹⁾ A P3-S8 approximation in cylindrical geometry was used in the transport calculation. The transport cross sections were a coupled 25 neutron-21 gamma group data library created from the RSIC DLC-41B/Vitamin-C Data Library.⁽²⁾ Kerma factors, helium and hydrogen production cross sections, and displacement cross sections were created from the DLC-60/MACKLIB-IV Data Library.⁽³⁾ A standard CTR weighting function was used to collapse the DLC-60 and DLC-41B libraries into the 25 neutron and 21 gamma energy group structures. Preparation of the cross section data is discussed in more detail in a UWFD report.⁽⁴⁾

The calculational model of the blanket, shield and magnets was shown in Fig. IX-A-1. Note that the TMR, because it is basically a long cylinder, lends itself well to 1-D cylindrical modeling. The tube banks overlap, and therefore, they are essentially a homogeneous zone, again simplifying modeling.

Thus, for the central cell of the TMR, it was felt that 2 or 3-D modeling was not needed, as is often the case for other conceptual designs with their toroidal or more complicated geometry. We did take into consideration the headers, which present a different geometry to the 14 MeV neutrons. We ran several cases with models which we felt adequately described this geometry to insure that the magnets in these regions were adequately protected.

IX-A-3. Parameter Studies

In this section we discuss the parametric neutronic studies that were made in order to establish the basic blanket for the reactor. Since these calculations were a part of an ongoing study, results obtained from a particular parameter study were subject to iteration due to changes in wall loads, plasma radius, and magnet design. These changes, however, usually had only a secondary effect on normalized results. For this reason the results presented in this section are normalized to a wall load of 1 MW/m^2 , however they may be scaled to any desired value.

Basically, the purpose of the parameter studies was to determine the blanket and shield thickness, determine if a reflector was needed, and

Table IX-A-1Tandem Mirror Neutronics Parameters(Based on 1 MW/m² Wall Load)Blanket & Shield

Breeding Ratio

⁶Li 1.043⁷Li .025

Total 1.068

Energy Deposited per 14.1 MeV Neutron

Neutron 7.166 MeV

Gamma 12.216 MeV

Total 19.38 MeV

Energy Multiplication 1.38

First Wall

Radius 96.7 cm

Volume/Unit Length 182.6 cm³/cm

Thickness .3 cm

Power Density

Neutron 4.41 W/cm³Gamma 3.52 W/cm³Total 7.93 W/cm³DPA per year/MW/m² 16.88

H production ppm/yr 482

He production ppm/yr 117

Energy Deposition per 14.1 MeV Neutron

Neutron .187 MeV

Gamma .149 MeV

Total .336 MeV

Breeding Blanket

Radius 97 cm

Volume/Unit Length 60,700. cm³/cm

Thickness 72.5 cm

Power Density	
Neutron	.490 W/cm ³
Gamma	.722 W/cm ³
Total	1.212 W/cm ³
Energy Deposition per 14.1 MeV Neutron	
Neutron	6.90 MeV
Gamma	10.17 MeV
Total	17.07 MeV
<u>Reflector</u>	
Radius	169.5
Volume/Unit Length	32,283 cm ³ /cm
Thickness	28 cm
Power Density	
Neutron	.0078 W/cm ³
Gamma	.249 W/cm ³
Total	.257 W/cm ³
Energy Deposition per 14.1 MeV Neutron	
Neutron	.058 MeV
Gamma	1.86 MeV
Total	1.92 MeV
<u>Shield</u>	
Radius	197.5 cm
Volume/Unit Length	85,765 cm ³ /cm
Thickness	60 cm
Power Density	
Neutron	.0012 W/cm ³
Gamma	.0017 W/cm ³
Total	.0029 W/cm ³
Energy Deposition per 14.1 MeV Neutron	
Neutron	.023 MeV
Gamma	.034 MeV
Total	.057 MeV
Percent of Total Energy	.29%

Magnets

Thermal Insulation (Mylar)

Radiation Dose

Midplane

 $8.6 \times 10^5 \text{ rad/yr}$

Header

 $1.6 \times 10^6 \text{ rad/yr}$

Electrical Insulation (Epoxy)

Radiation Dose

Midplane

 $6.4 \times 10^5 \text{ rad/yr}$

Header

 $1.5 \times 10^6 \text{ rad/yr}$ DPA in Al per yr/MW/m² 2.73×10^{-7}

Power Density

Neutron

 $1.12 \times 10^{-8} \text{ W/cm}^3$

Gamma

 $6.30 \times 10^{-8} \text{ W/cm}^3$

Total

 $7.42 \times 10^{-8} \text{ W/cm}^3$

Radius

330. cm

Volume/Unit Length

 $238,760 \text{ cm}^3/\text{cm}$

Thickness

100 cm

determine the volume per cent structural material. These items are subject to certain constraints such as the necessity to breed tritium and provide adequate protection for the magnets.

In the first set of parametric calculations, the volume per cent of structural material in the blanket was varied from zero to 90 per cent. The composition of the structural material was 11 weight per cent chromium and 89 weight per cent iron. These calculations were made in order to determine the effects of the structural material on the tritium breeding ratio so that limits on the blanket thickness could be set. A one meter thick blanket was used in these calculations. The reflector and shield were not considered here. For conservative estimates, a vacuum boundary condition was used on the outside of the blanket. The results are plotted in Fig. IX-A-2.

As may be noted in Fig. IX-A-2 the breeding ratio varied almost linearly as a function of volume per cent of structural material in the blanket. At 22 per cent structure, the breeding ratio was 1.0 and increased to a value of 1.4 at zero per cent structure. From the standpoint of the tritium breeding requirement, the blanket could contain up to 22% structure. This is a conservative estimate since the blanket could be made much thicker than the one meter with vacuum boundary conditions at which these cases were evaluated.

In order to estimate the effect of radius on the tritium production, two of the above cases containing 5% and 10% structure were considered. The tritium production was integrated from the inner radius to a variable depth giving the cumulative production to that depth. This is shown in Fig. IX-A-3 and is similar to the breeding ratio of a blanket of that thickness with some reflector.

Concurrent geometrical calculations noted that 10 volume per cent was a reasonable value. Using this value and the results obtained from Fig. IX-A-3 indicated that a tritium breeding ratio of ~ 1.1 , a design goal, could be obtained within a .75 meter blanket.

Next, two basic blanket/shield designs were evaluated. The first design had a breeding blanket of .75 meters followed by a ferritic steel reflector of .25 meters, and then a shield of .5 meter thickness. The breeding blanket was composed of 90 per cent $\text{Li}_{17}\text{Pb}_{83}$ and 10 per cent ferritic steel structure. The reflector was composed of 95 per cent ferritic steel and 5 per cent water. The shield was 60 per cent ferritic steel, 15 per cent lead, 15 per cent boron

BREEDING RATIO vs.
PERCENT STRUCTURE

1 METER BLANKET
NO REFLECTOR

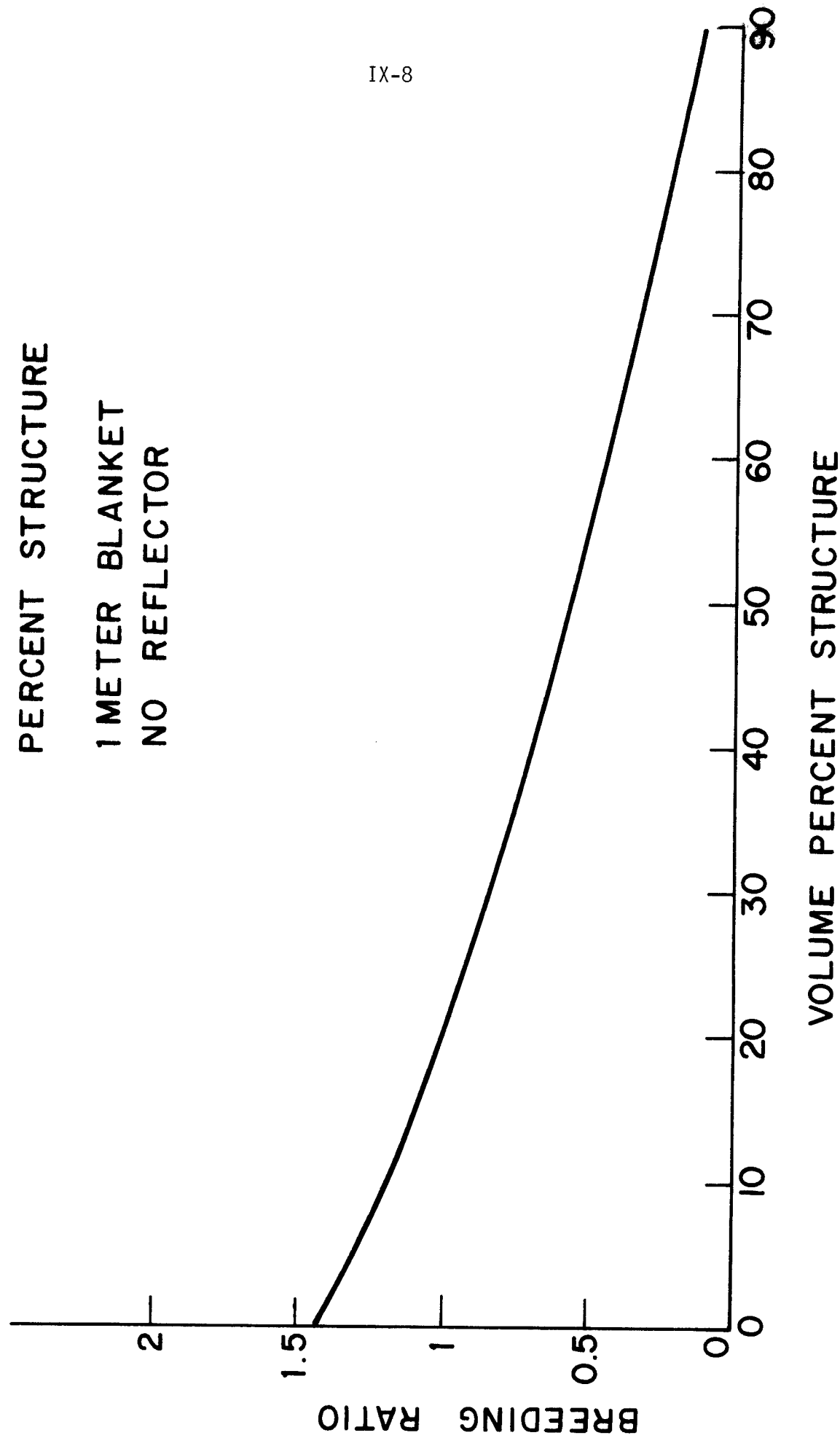


Fig. IX-A-2

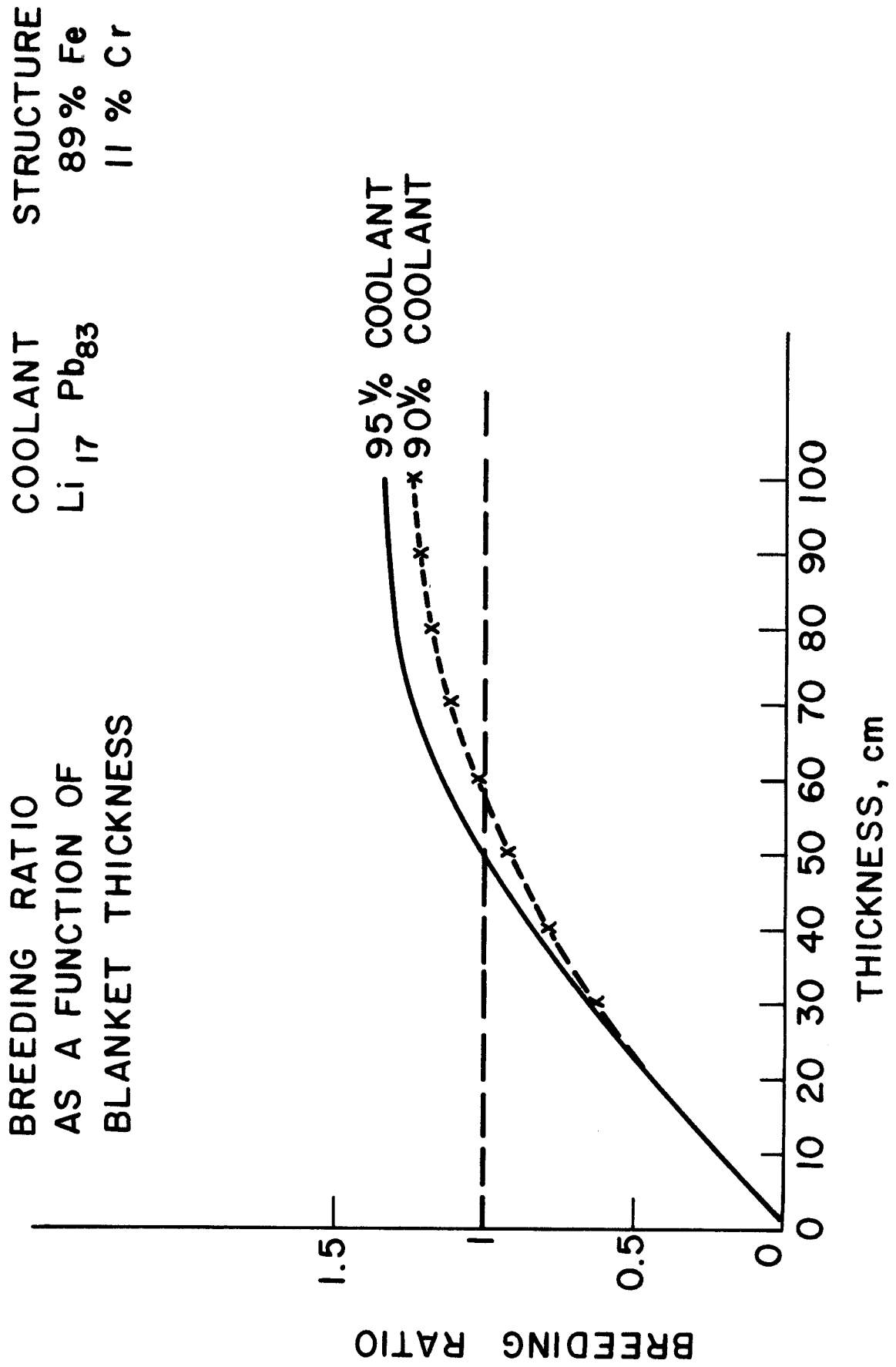


Fig. IX-A-3

carbide, 5 per cent water, and 5 per cent void. A schematic of this blanket/shield is shown in Fig. IX-A-4.

The second blanket/shield design eliminated the reflector, thus extending the breeding blanket to one meter thickness. This was also followed by a .5 meter shield. In both cases, the shield was followed by .2 meters of insulation and then the .8 meter magnet. For calculational purposes the magnet consisted of 60 per cent aluminum, 30 per cent copper, and 10 per cent helium.

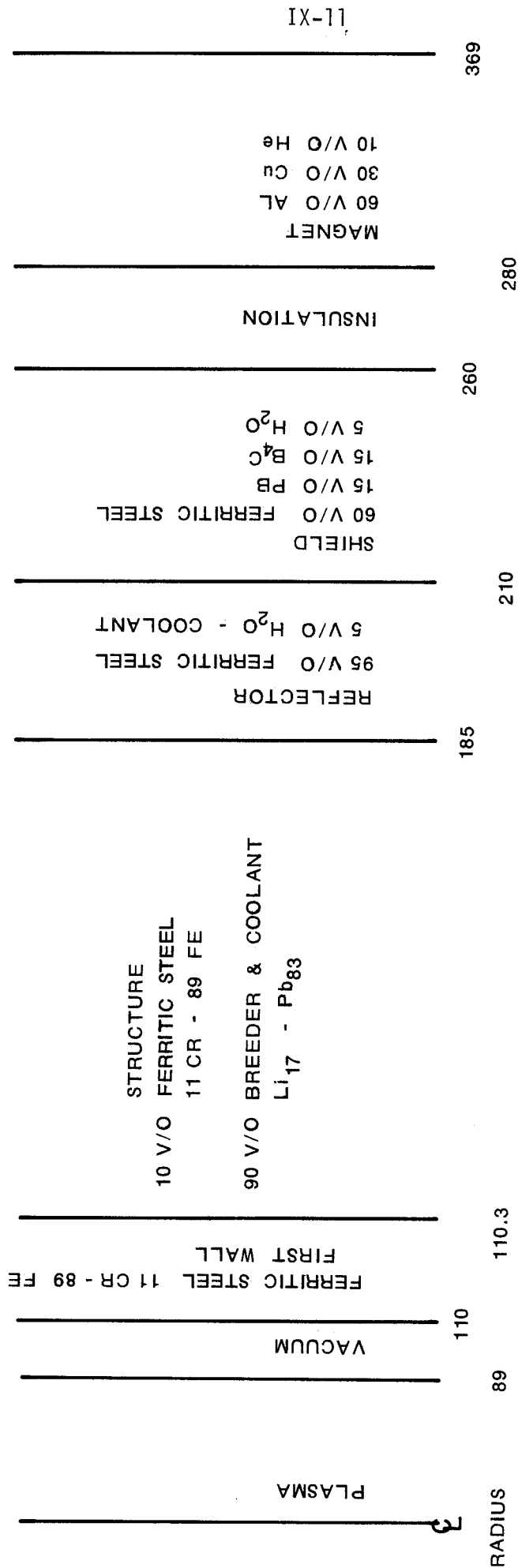
The first case resulted in the release of 18.9 MeV per 14 MeV neutron, an energy multiplication of 1.34. Sixty per cent or 11.3 MeV of the energy is deposited in the reflector, and it is expected that this energy will be recoverable. Less than 1 per cent of the energy was deposited in the shield. The tritium breeding ratio was 1.10.

In the second case a total of 18.0 MeV per 14 MeV neutron was deposited in the blanket and shield, yielding an energy multiplication of 1.28. Since the breeding zone increases in thickness, the breeding ratio also increases to 1.18. The energy deposited from the neutron collisions increased from 7.60 MeV in the previous case to 8.23 MeV here, and is primarily related to the increased tritium production. The gamma energy deposited was 9.77 MeV, which decreased due to the reduction in capture gammas from lead, iron, and chromium since more neutrons are captured in lithium. This accounts for the reduction in energy multiplication from the previous case since about 7 MeV is released from a capture in lead, iron, or chromium as compared to 4.8 MeV released from a capture in ${}^6\text{Li}$. The results of these two cases are summarized in Table IX-A-2.

After determining the approximate thickness of shielding required (see Section IX-A-4), we began the iterative process to arrive at a final blanket and shield design. We began the process by adding voids and changing the structural material densities across the blanket. In Fig. IX-A-5 are typical examples of the blanket configurations we investigated. A final design which was shown in Fig. IX-A-1 was developed based on adequate tritium breeding, high energy multiplication and sufficient magnet protection. The neutronic results for this blanket are discussed in more detail in the following sections.

IX-A-4. Shield Design

At one point we explored the idea of having only a small amount of shielding. This concept was precipitated by the idea that if the blanket were thick



BLANKET SCHEMATIC

Fig. IX-A-4

Table IX-A-2
Comparison of Blankets With and Without Reflector

Case 1 With Reflector

11.9 MeV Gamma Energy
9.6 MeV Neutron Energy
18.9 MeV Total Energy Deposited per 14 MeV Neutron
1.34 Energy Multiplication
1.13 Tritium Breeding Ratio

Case 2 Without Reflector

9.77 MeV Gamma Energy
8.23 MeV Neutron Energy
18.0 MeV Total Energy Deposited per 14 MeV Neutron
1.28 Energy Multiplication
1.18 Tritium Breeding Ratio

FIGURE IX - A - 5

EXAMPLE BLANKET CONFIGURATIONS

CASE 1 ENERGY DEPOSITED 19.48 MeV TRITIUM BR 1.04	CASE 2 ENERGY DEPOSITED 19.17 MeV TRITIUM BR 1.10
0.3 cm	0.3 cm
100 V/O FS	100 V/O FS
5 cm	5 cm
79 V/O Li ₁₇ Pb ₈₃	79 V/O Li ₁₇ Pb ₈₃
0 V/O FS	0 V/O FS
4.5 cm	4.5 cm
87 V/O Li ₁₇ Pb ₈₃	87 V/O Li ₁₇ Pb ₈₃
7 V/O FS	7 V/O FS
9 cm	
81 V/O Li ₁₇ Pb ₈₃	
9 V/O FS	
18 cm	45 cm
75 V/O Li ₁₇ Pb ₈₃	81 V/O Li ₁₇ Pb ₈₃
25 V/O FS	9 V/O FS
36 cm	18 cm
60 V/O Li ₁₇ Pb ₈₃	60 V/O Li ₁₇ Pb ₈₃
40 V/O FS	40 V/O FS
28 cm	28 cm
5 V/O Li ₁₇ Pb ₈₃	5 V/O Li ₁₇ Pb ₈₃
95 V/O FS	95 V/O FS
60 cm	60 cm
SHIELD	SHIELD

enough, perhaps it could be self-shielding due to the large amount of lead which would attenuate gammas and ^6Li , with its high capture cross sections, would attenuate the neutrons. Under this concept the blanket would be simpler to design and complete energy recovery would be easier.

To explore this concept a series of transport calculations was made in which both the shield and blanket thickness varied. The blanket was composed of a homogenized mixture of 10% steel and 90% $\text{Li}_{17}\text{Pb}_{83}$. The shield was a homogenized mixture of 60% ferritic steel, 15% lead, 15% B_4C and 5% H_2O .

Prior to looking at the shielding requirements we wanted to insure that adequate tritium and energy multiplication could be obtained. In Fig. IX-A-6, the tritium breeding ratio is plotted as a function of blanket thickness. In these calculations enough shielding was added so that the total blanket and shield was 1.5 meters. The result here is similar to that shown in Fig. IX-A-3, and again it may be noted that adequate tritium may be obtained in rather thin blankets.

In Fig. IX-A-7, the total energy deposited per 14 MeV neutron in the blanket and shield as a function of blanket thickness is shown. Here also the total blanket and shield thickness was 1.5 meters. Note however, that the total maximum energy released of ~ 18.5 MeV is much less than that obtained with more structural material. Note also that if the blanket is thin, a considerable amount of energy would be lost in this configuration if a reflector is not employed.

The controlling parameter used to determine the shielding required is usually the dpa in the stabilizing material in the magnets. In Fig. IX-A-8 we plotted the dpa/year in a copper stabilizer as a function of blanket and shield thickness for a 1 MW/m^2 neutron wall load. There were four blanket thickness: .70, .98, 1.24, and 1.36 meters. In Fig. IX-A-8 the dpa/year is given as a function of shield thickness added to the blanket.

If a 50% resistivity increase (which corresponds to $\sim 3 \times 10^{-5}$ dpa) in the copper stabilizer is taken as the limiting case, it may be seen that considerable shielding will be required if the magnets are to operate any reasonable time without annealing. These results indicate that shielding will be required and that the lithium lead blankets are quite transparent to neutrons. Later in the study, when it was decided to use aluminum as the stabilizer, which is more susceptible to radiation damage than copper, we found that 60 cm of shielding was necessary to protect the magnets.

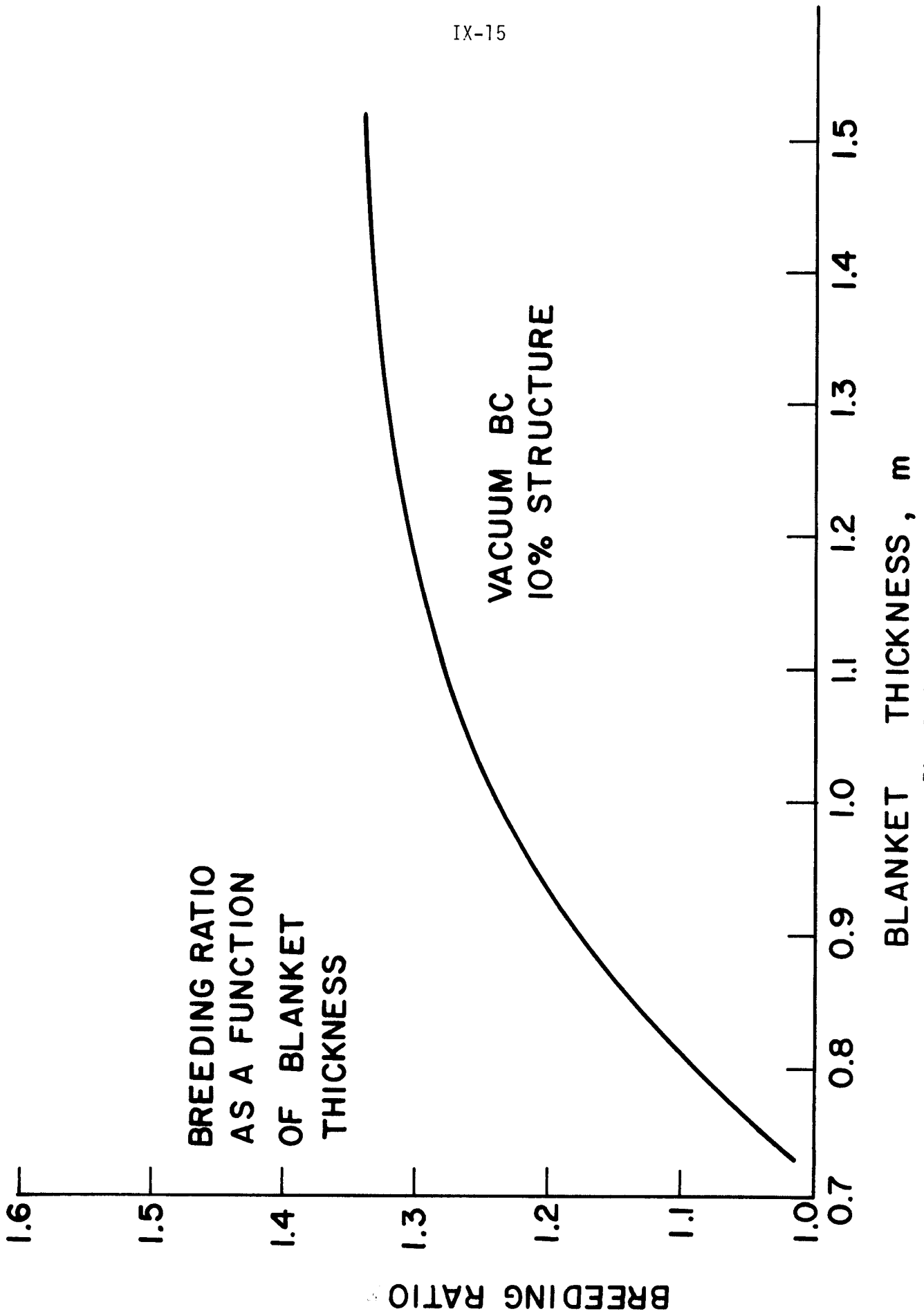


Fig. IX-A-6

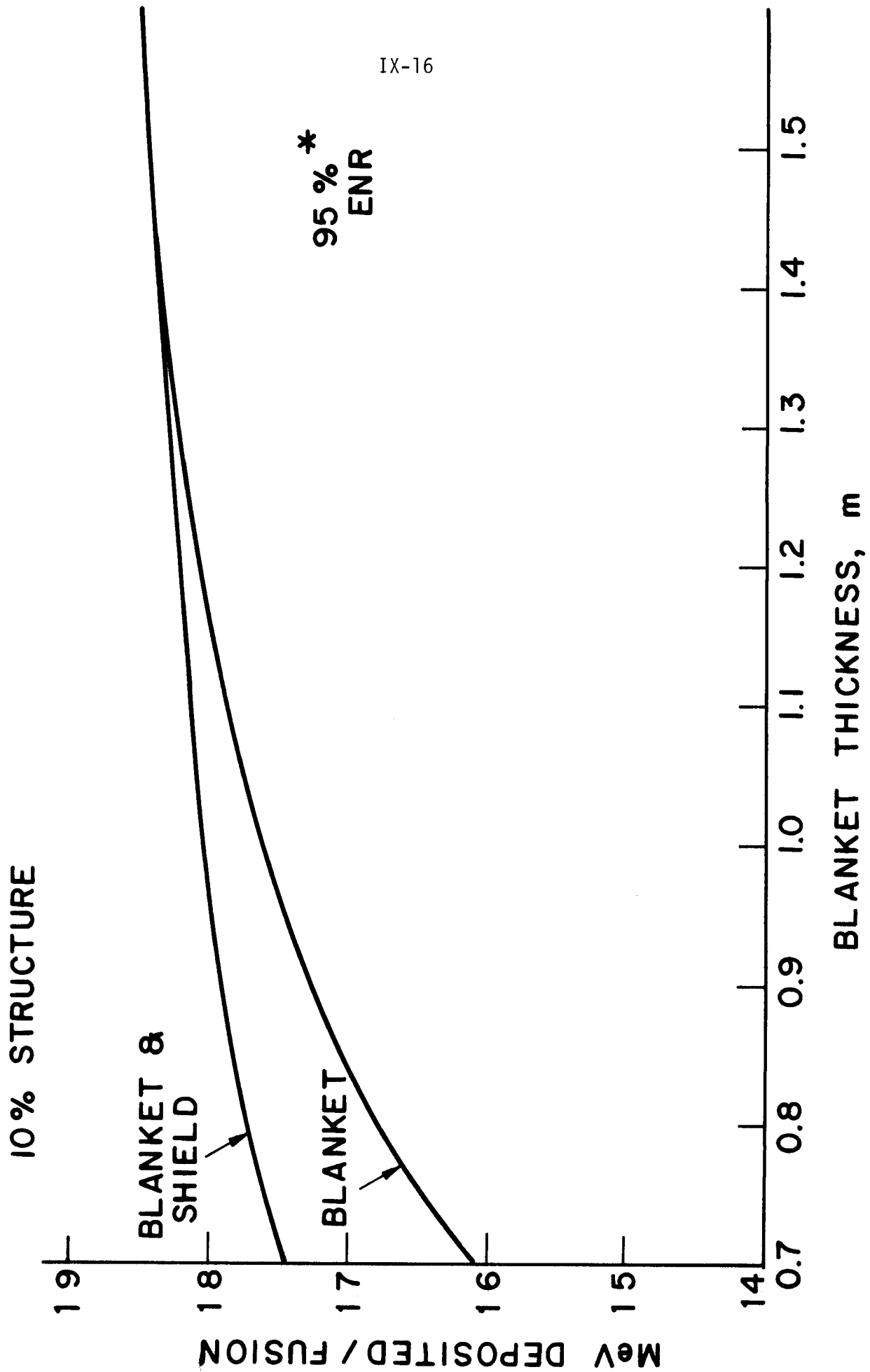


Fig. IX-A-7

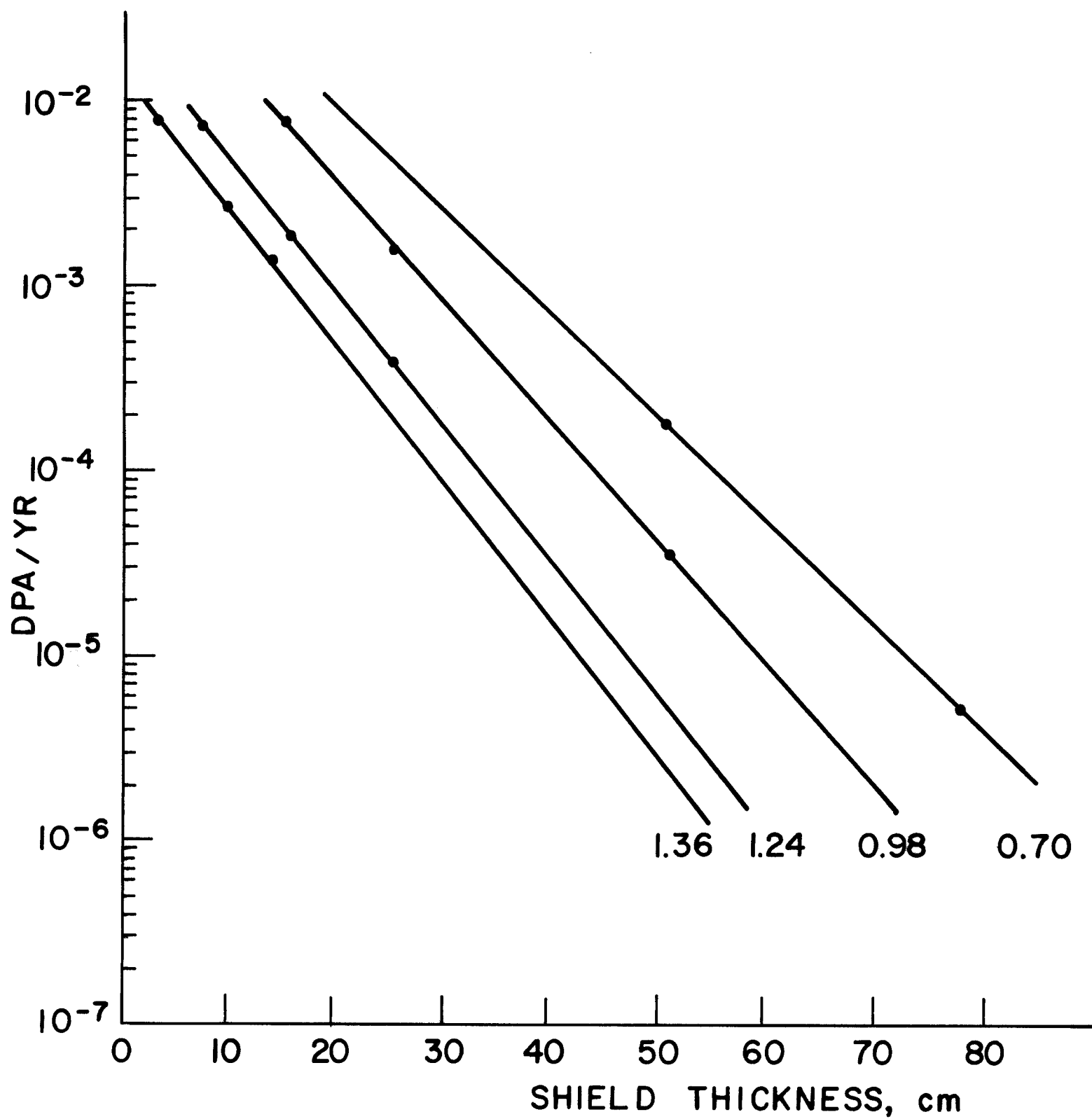


Fig. IX-A-8

Dpa/year as a function of shield thickness
added to the blanket.

Our final shield design (Fig. IX-A-1) is laminated, however overall it contains 15% Pb, 55% ferritic steel, 3.6% H₂O and 26.4% of (.87 theoretical density) B₄C. This shield also adequately protects the magnets at the headers. These radiation damage parameters are discussed in more detail in a following section.

IX-A-5. Energy Deposition and Tritium Breeding

In this section we discuss the neutronic results of our final blanket design which we chose following parametric studies in which blanket thickness, per cent structure and lithium enrichment were varied. We found that the energy multiplication increased with increasing volume per cent of structure. Since large energy multiplication is highly desirable from an economic point of view, it was a major consideration in our parameter studies. Basically our final design was one in which we had maximized structural material consistent with adequate tritium breeding, magnetic protection and geometrical requirements.

The energy multiplication for this blanket is 1.37. The energy multiplication is defined as the ratio of the energy deposited in the blanket to that carried by the fusion neutron, 14.06 MeV. In Table IX-A-3, the energy multiplication and wall load for several reactor designs are given. Note that the energy multiplication for WITAMIR-I is significantly greater than the other conceptual designs. These numbers do not take into account the 3.52 MeV carried by the fusion alpha particle. It is interesting to compare the wall loads between the designs which, with the exception of WITAMIR-I, are tokamaks. From them one can see that the power densities and structural radiation damage will not be greatly different between tokamaks and TMRs.

In this blanket, the energy multiplication is enhanced by the low lithium content in the blanket in two ways. First, there is a greater probability of a high energy neutron collision in lead which can result in an (n,2n) or (n,3n) reaction. Lead has large cross sections for these reactions, thus decreasing the lithium content increases neutron production. Secondly, a neutron capture in the structural material or lead will release about 7 MeV as compared to a capture in ⁶Li which releases about 4 MeV.

We designed the blanket to take advantage of these processes. Toward the plasma, the structural material is kept to a minimum to enhance the (n,2n) and (n,3n) reactions in lead. Into the blanket, however, the per cent of structural material is increased, in fact by far more than is actually needed

Table IX-A-3
 Energy Multiplication and Neutron
 Wall Loads for Various Fusion Reactor
 Conceptual Designs

	<u>Energy Multiplication</u>	<u>Wall Load MW/m²</u>
UWMAK I ⁽⁵⁾	1.17	1.25
UWMAK II ⁽⁶⁾	1.28	1.16
UWMAK III ⁽⁷⁾	1.29	2.5
NUWMAK ⁽⁸⁾	1.22	4.0

structurally. This was done to suppress the captures in ${}^6\text{Li}$, and enhance the captures in the steel. In this blanket the zone usually referred to as a reflector is used to enhance the energy multiplication through neutron capture, rather than reflecting neutrons back into the active blanket, although this occurs.

In Table IX-A-4 the energy deposited per zone per 14 MeV neutron is given. Note that the gamma energy released is larger than the neutron energy indicating large neutron captures in the lead and structure. This blanket has a tritium breeding ratio of 1.07. To emphasize the point that increases in the tritium breeding ratio reduce the energy multiplication, we enriched the lithium in a blanket to 95% ${}^6\text{Li}$. The tritium breeding ratio increased to 1.6; however, the total energy production dropped to 16.4 MeV.

In Fig. IX-A-9, the heating rates in the blanket are shown. These results are for 1 MW/m^2 wall load and may be scaled to any desired value. The reflector generates 9.9% of the total energy and most of that energy is from gamma interactions.

The shield, which consists of several layers of ferritic steel, B_4C and lead, is water cooled. About 3/10 of 1 per cent of the total energy is deposited in the shield.

IX-A-6. Structural Radiation Damage

The blanket, and especially the first wall, are subject to intense high energy neutron bombardment. A measure of the damage expected is the number of displacements per atom of structural material. In addition, helium and hydrogen production in the structural material through various neutron reactions also lead to material degeneration. In Table IX-A-5, the displacements per atom, helium production and hydrogen production in the first wall are given. These results are for 1 MW/m^2 year of continuous operation. In Fig. IX-A-10 a plot of the displacements per atom in the structural material through the blanket is shown.

IX-A-7. Magnet Shielding

The function of the shield is to protect the magnets from heat and radiation. Unfortunately, the blanket is a poor attenuator of neutrons, primarily due to the large volume per cent of lead which is relatively transparent to neutrons. Thus, a 60 cm shield was required for adequate protection.

The neutron fluence in the superconductors primarily affects the stabilizing material and results in an increase of the electrical resistivity. The

Table IX-A-4
Energy Deposited by Zone per 14 MeV Neutron

	- MeV -		
	<u>Gamma</u>	<u>Neutron</u>	<u>Total</u>
First Wall	.15	.19	.34
Blanket	10.17	6.90	17.07
Reflector	1.86	.06	1.92
Shield	.0017	.0012	.0029

Table IX-A-5
First Wall Radiation Damage Parameters per Year:
1 MW/m² Wall Load

Displacement per Atom	16.88
He Production ppm	117
Hydrogen Production ppm	482

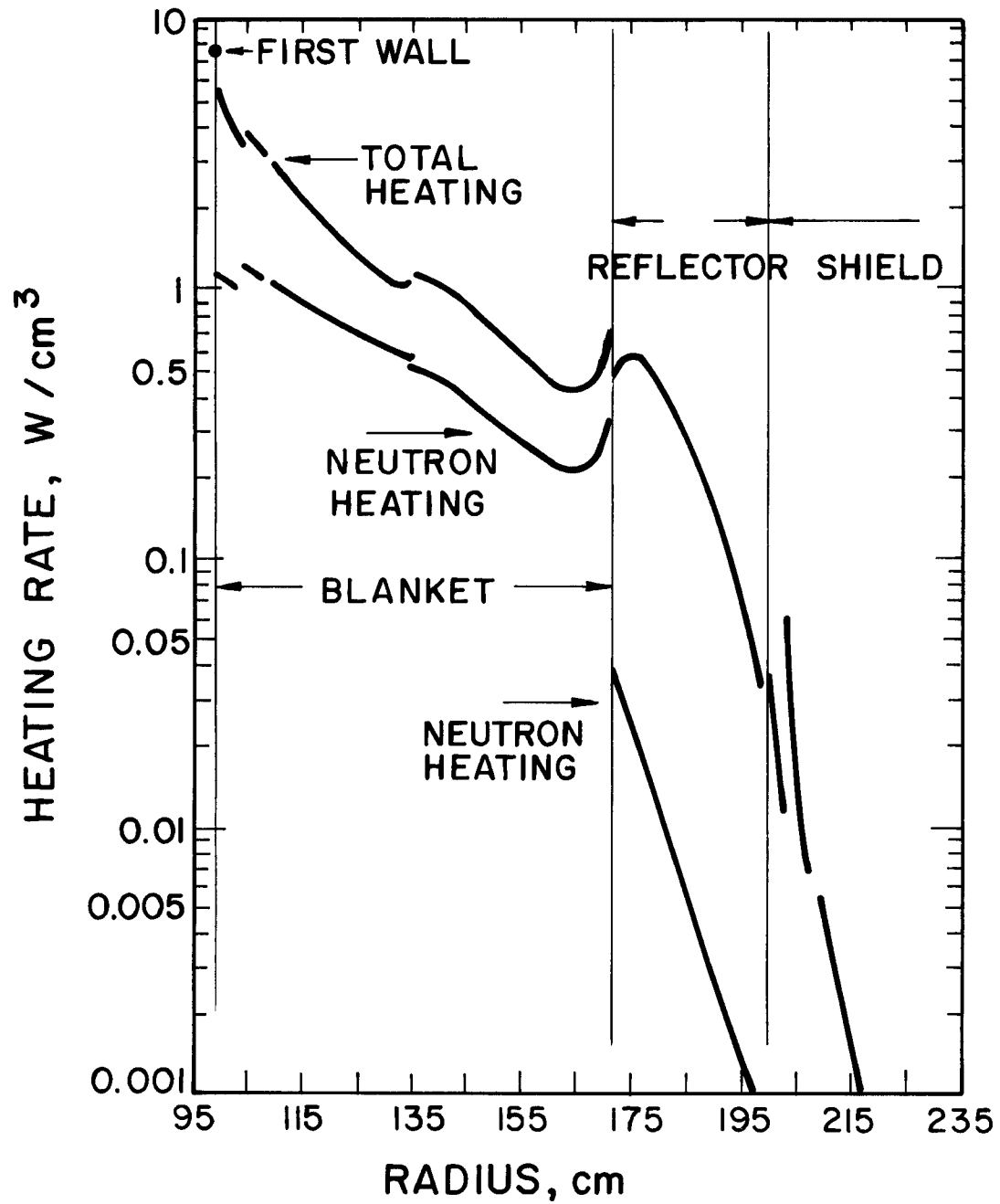


Fig. IX-A-9. Heating Rates per MW/m^2 Wall Load

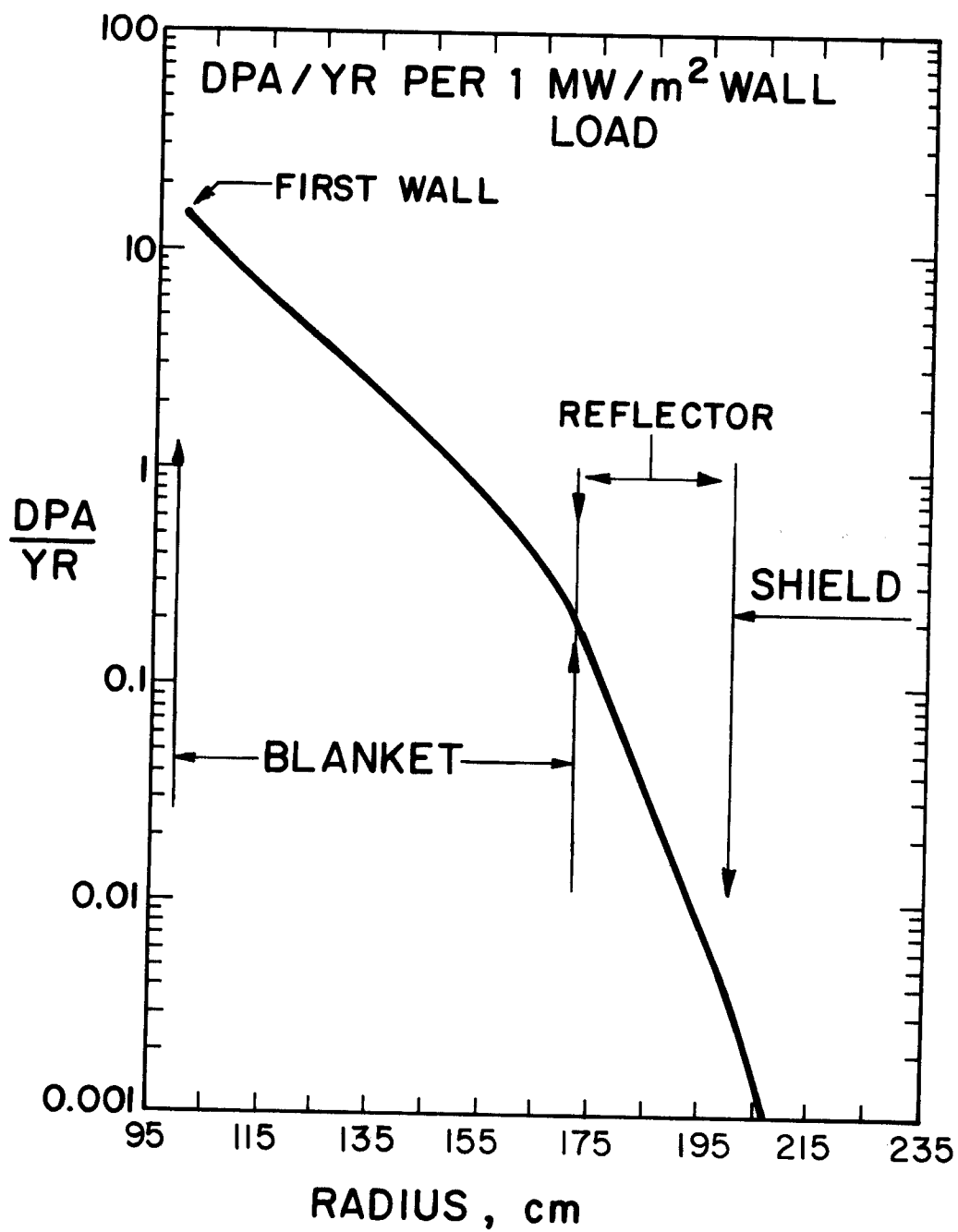


Figure IX-A-10

Dpa in the Blanket Structural Material

increase in resistivity is related to the number of displacements per atom in the stabilizer, which in our design is aluminum. The increase in resistivity limits the magnet functions; however, complete recovery of the magnet is possible by annealing.

A design limit of two years of operation without annealing was set. It is expected that annealing would be required when a resistivity increase of 50% occurred. This corresponds to 4×10^{-6} dpa.

A plot of the dpa/yr in the magnets are shown in Fig. IX-A-11. Taking the maximum value of the dpa in aluminum, which occurs at the edge, and scaling it to 2.4 MW/m^2 wall load for a two year period, results in a value of 1.3×10^{-6} which is well within the design limits. Similar results were found at the top and bottom of the reactor where a different blanket model was used to simulate the headers.

The radiation effects on the epoxy and thermal insulation used in the magnets are not reversible. Thus, it is important that these last for the life of the reactor. The limits would be about 1×10^{10} rads for the thermal insulation and $1-5 \times 10^9$ rads for the epoxy. Our calculations indicate that a maximum dose of 1.6×10^6 rads per year per MW/m^2 wall load will occur in the thermal insulation at the headers, and 8.6×10^6 rads at the midplane. For the epoxy a dose of 1.5×10^6 rads per MW/m^2 per year will occur at the headers and 6.4×10^6 rads at the midplane. Even at full power for thirty years, they would be within limits. The heating rates which are used to determine the heat loads in the magnets are shown in Fig. IX-A-12.

IX-A-8. Conclusions

The blanket and shield for the TMR meet all of our basic design criteria. The blanket has a tritium breeding ratio of 1.07 and the shield adequately protects the magnets. In addition, the blanket has an exceptionally high energy multiplication which enhances the economics of the system.

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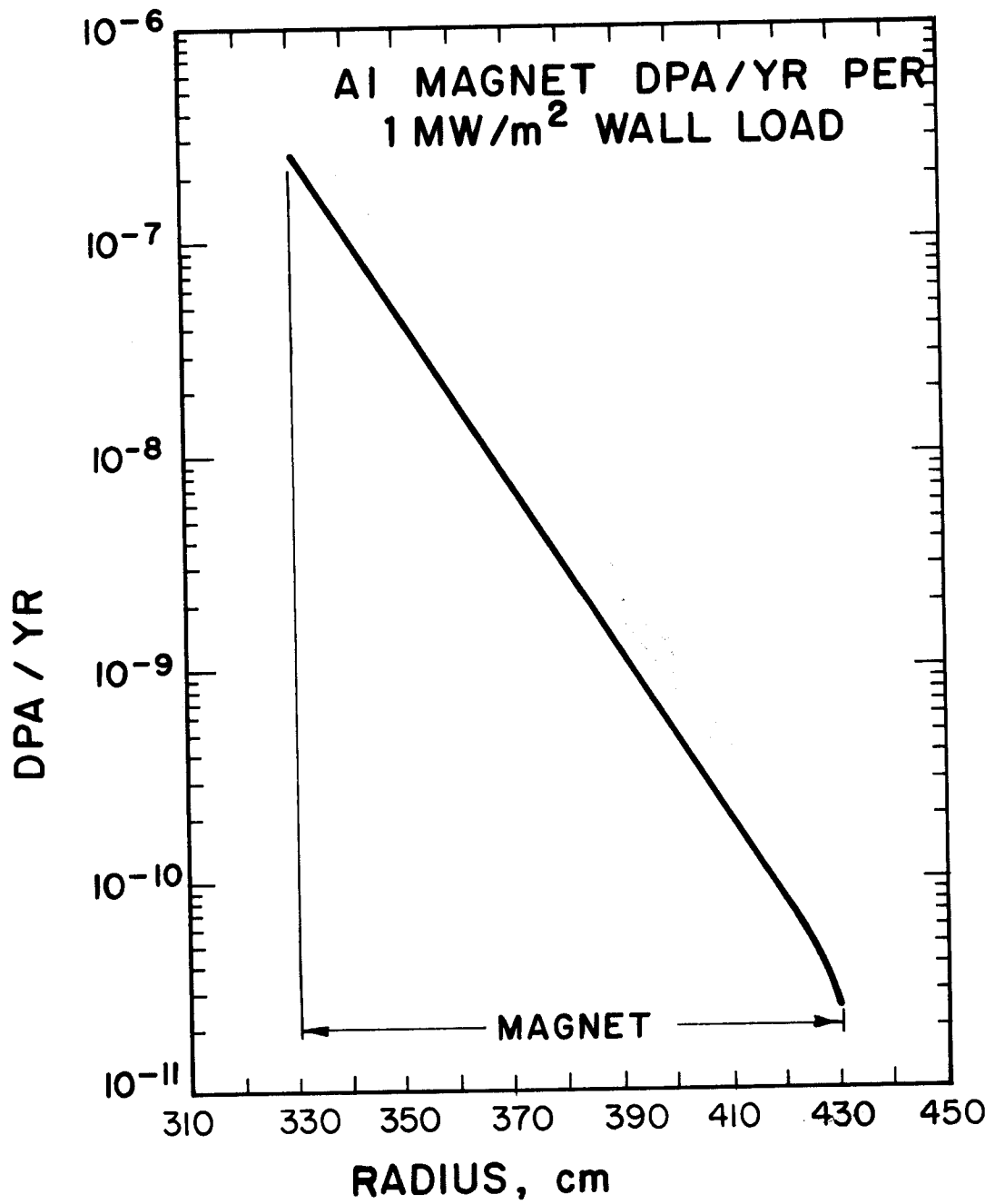


Figure IX-A-11

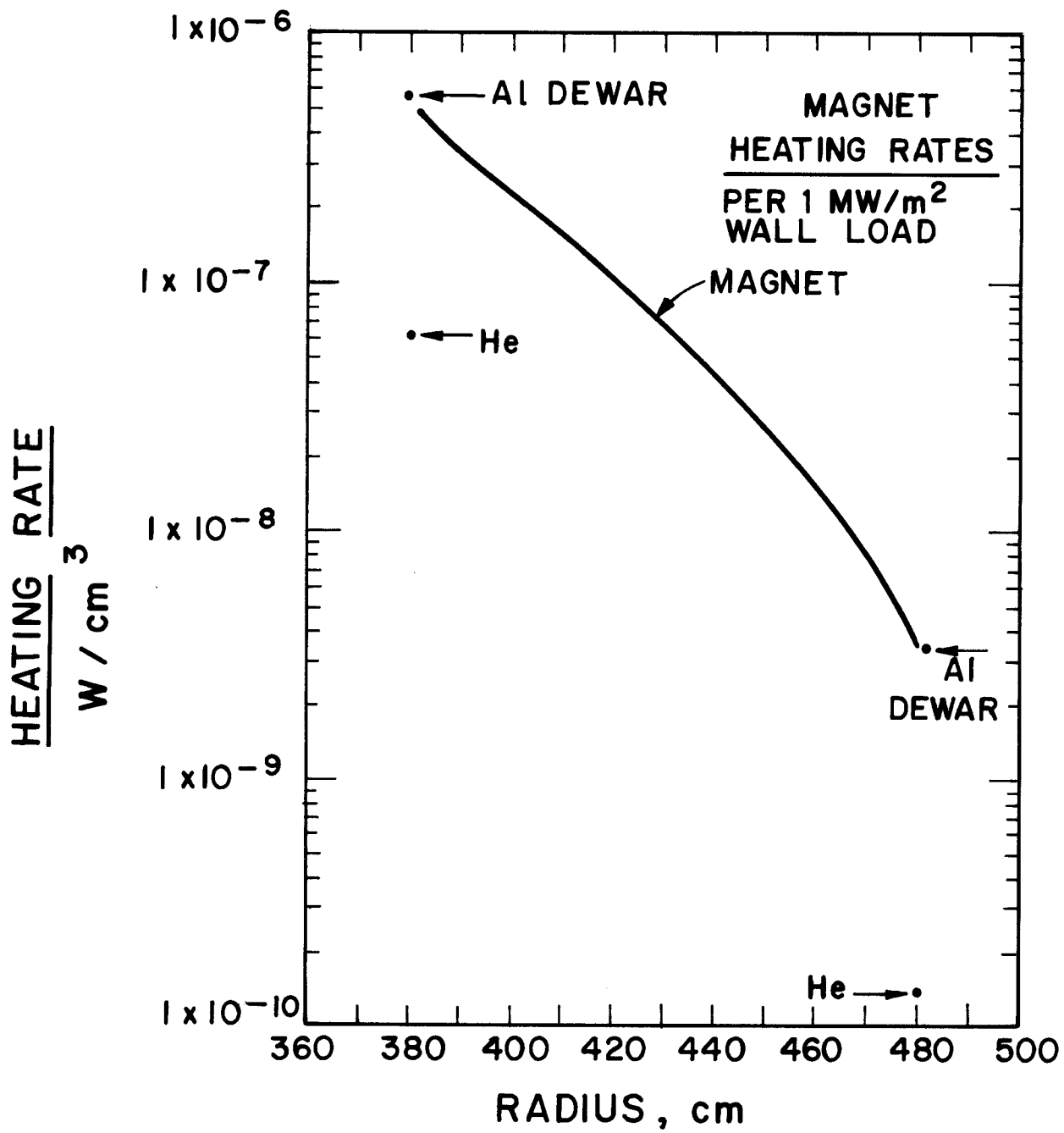


Fig. IX-A-12

IX-B. Radioactivity and Related Quantities

The radioactivity of WITAMIR-I has been calculated for the central cell using the model shown in Fig. IX-B-1. The system was treated in cylindrical geometry and the neutron fluxes taken from the calculations described in Section IX-A. The results are shown in Figs. IX-B-2 through IX-B-6. The levels were calculated for a two-year operating time and the totals for the blanket and the combined reflector and shield are presented. The levels either of activity, afterheat or BHP per unit of power are the same order of magnitude as obtained in previous studies, i.e., at shutdown, the total activity is ~ 1 curie/watt and the afterheat is $\sim 0.8\%$ of the operating power. As expected, the highest volumetric source is in the first wall but because of the large amount of ferritic steel structure in the outer portions of the blanket, this region contributes a significant portion of the total activities. This time dependence of the activity related quantities following shutdown is somewhat similar to previous designs using steel structures. During the first few hours following shutdown, there is a drop in activity by a factor of ~ 2 due to the decay of ^{56}Mn ($t_{1/2} = 2.6$ h). Subsequent to this the decay is rather slow being dominated by ^{55}Fe , ^{51}Cr and ^{54}Mn , with the result that ~ 2 years are required for activity levels to be down by a factor of 10 from shutdown and ~ 10 years for a factor of 100. ^{55}Fe ($t_{1/2} = 2.7$ y) is the dominant isotope in this 3-10 y time interval.

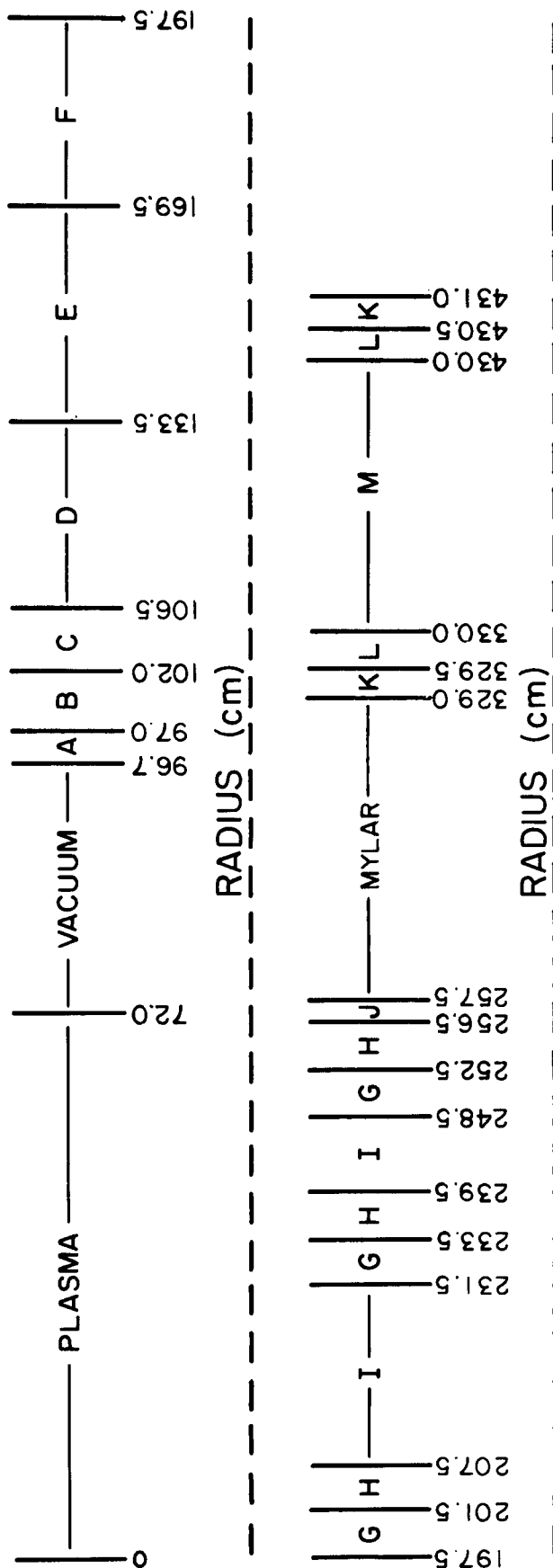
The time behavior of the afterheat is somewhat different principally because of the small decay energy of the ^{55}Fe compared to the other isotopes. The afterheat has dropped by over a factor of 10 in two days after shutdown and by a factor of 100 by two years.

The BHP, however, shows a time behavior more nearly like the activities. There is some decrease after about one day due to the decay of ^{56}Mn , but subsequently, the BHP is dominated by ^{54}Mn ($t_{1/2} = 3/2$ day) from one day to about 2 years followed by ^{55}Fe and then ^{93}Nb . Thus the fall of the BHP is slow, ~ 3 years for a factor of 10 and 10 years for a factor of 100 decrease.

In all cases, the long term effects are due to the molybdenum in the ferritic steel structure.

A similar calculation was performed for the magnets with the results shown in Fig. IX-B-7 through IX-B-9. The calculations show that the activity levels are low either on a total or per unit power basis and that there is appreciable decay in the first day, resulting in a reduction of activity by

CROSS SECTION OF WITAMIR - I USED IN RADIOACTIVITY CALCULATIONS



<u>COMPOSITION</u>		<u>TABLE</u>
A - 100% FERRITIC STEEL (HT-9)		H - 87% B ₄ C
B - 79% Li ₁₇ Pb ₈₃		I - 95% F.S., 5% H ₂ O
C - 84% Li ₁₇ Pb ₈₃ , 7% FERRITIC STEEL (F.S.)		J - 100% F.S.
D - 81% Li ₁₇ Pb ₈₃ , 9% F.S.		K - 100% Al 2219
E - 75% Li ₁₇ Pb ₈₃ , 25% F.S.		L - 100% He
F - 5% Li ₁₇ Pb ₈₃ , 95% F.S.		M - 17.8% Al, 69% Al 2219, 4% He, 9.2% INSULATION
G - 90% Pb, 5% H ₂ O, 5% F.S.		

Fig. IX-B-1

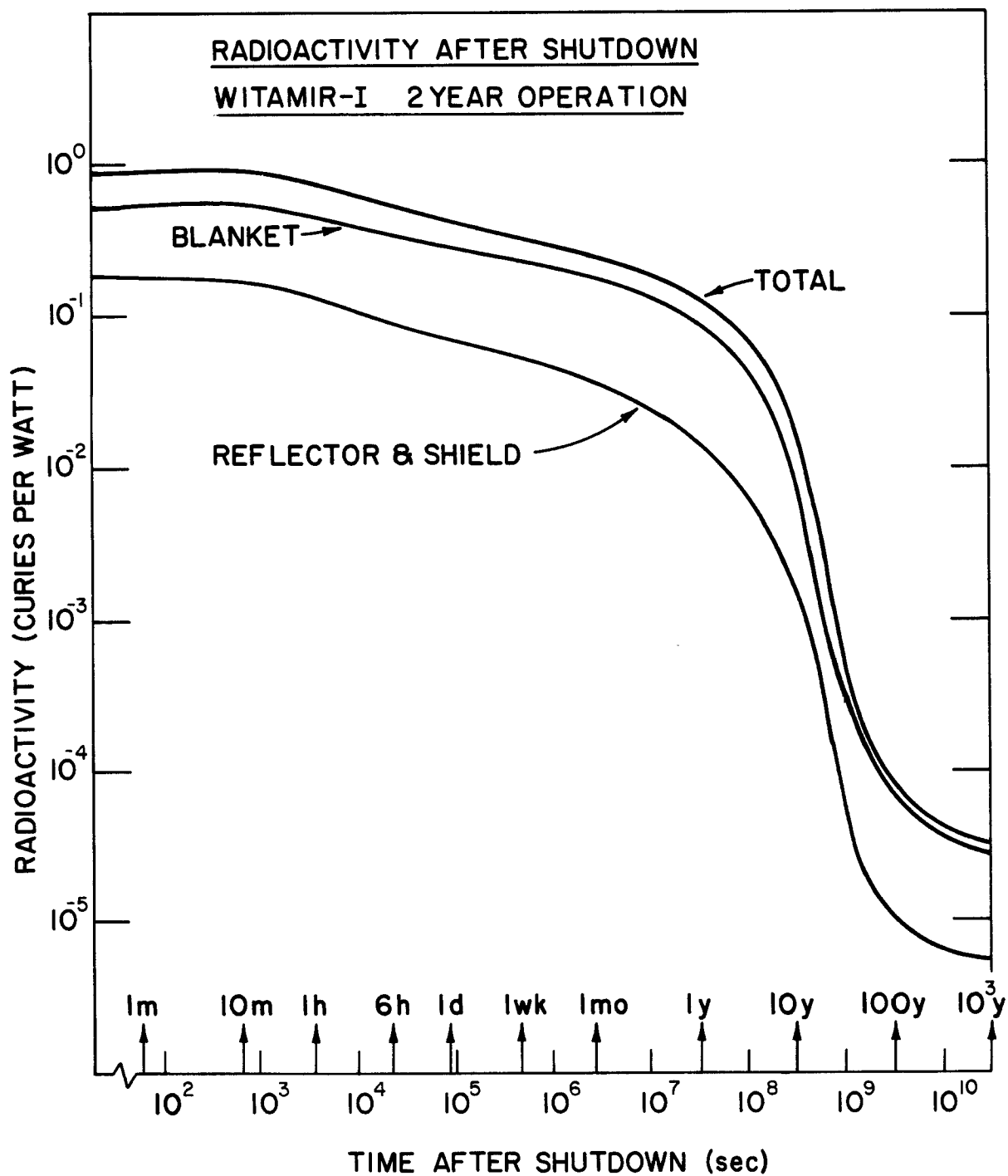


Fig. IX-B-2

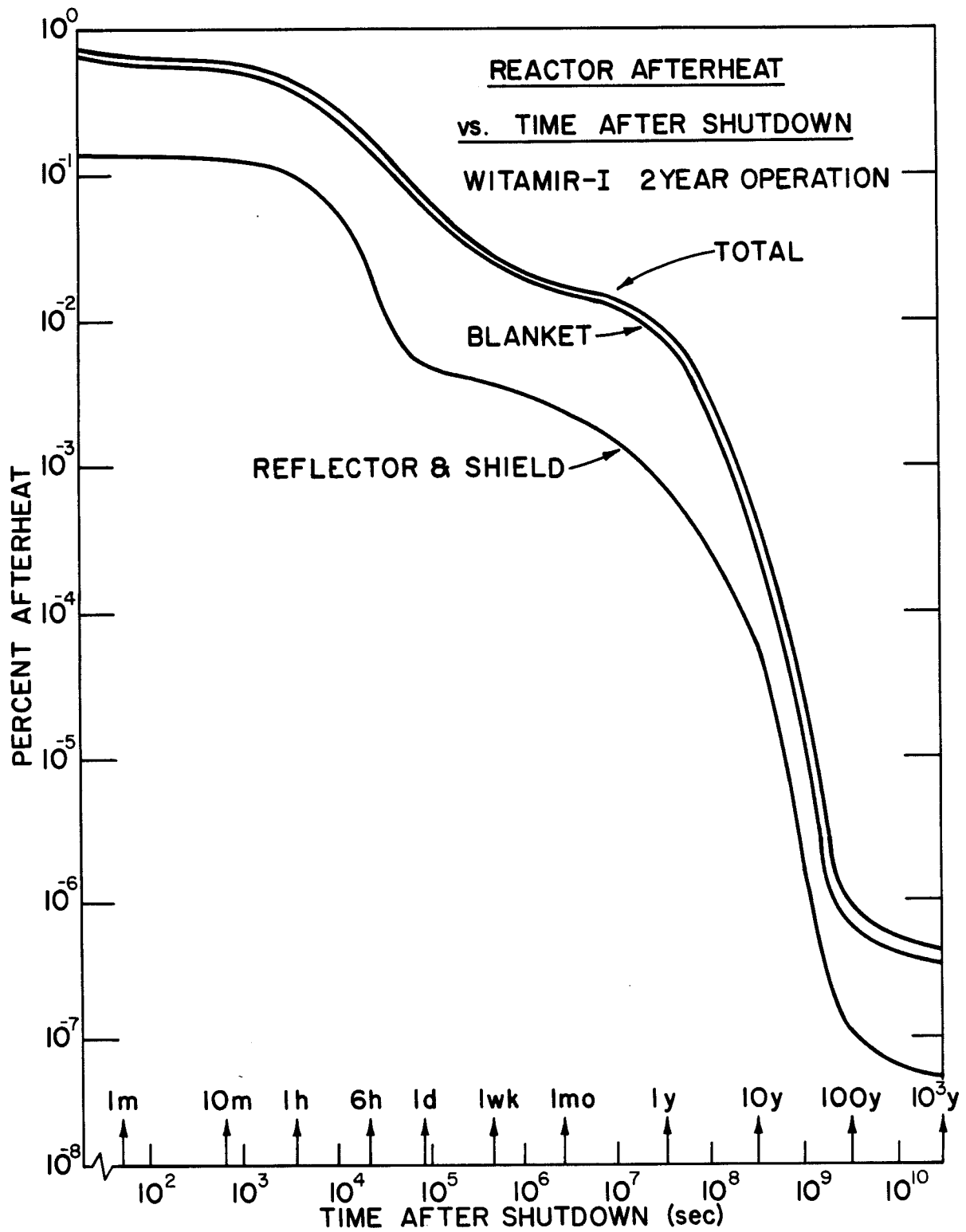


Fig. IX-B-3

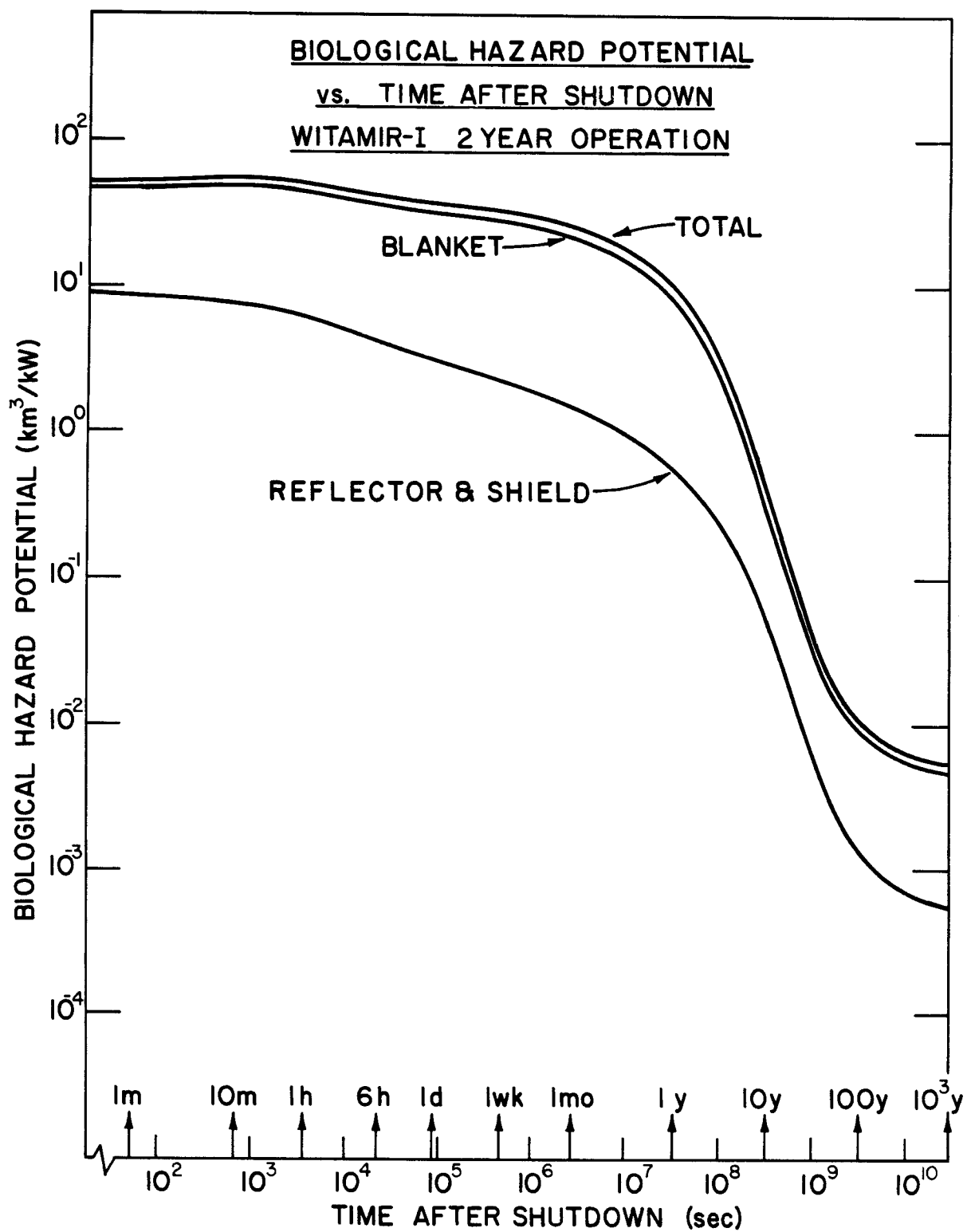


Fig. IX-B-4

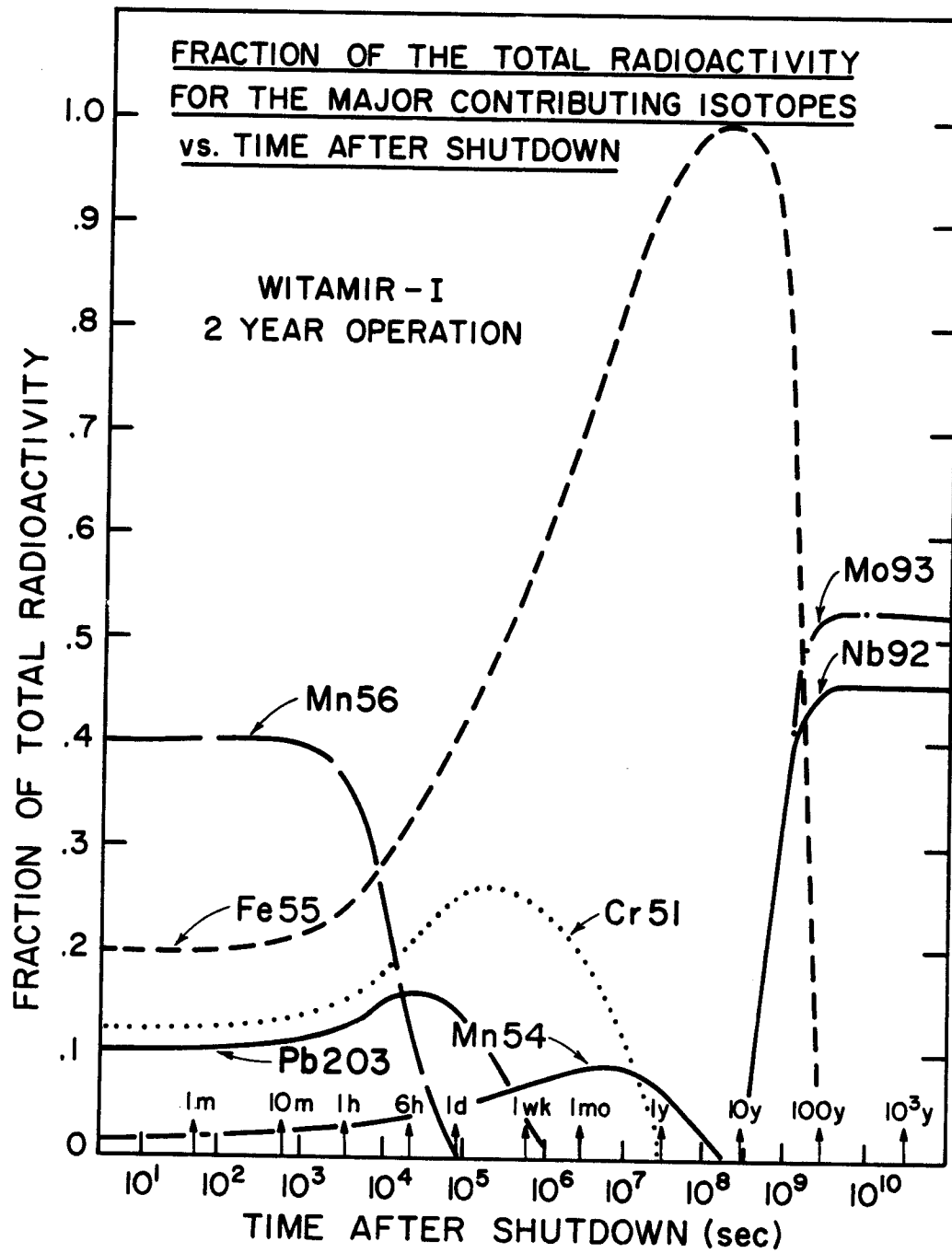


Fig. IX-B-5

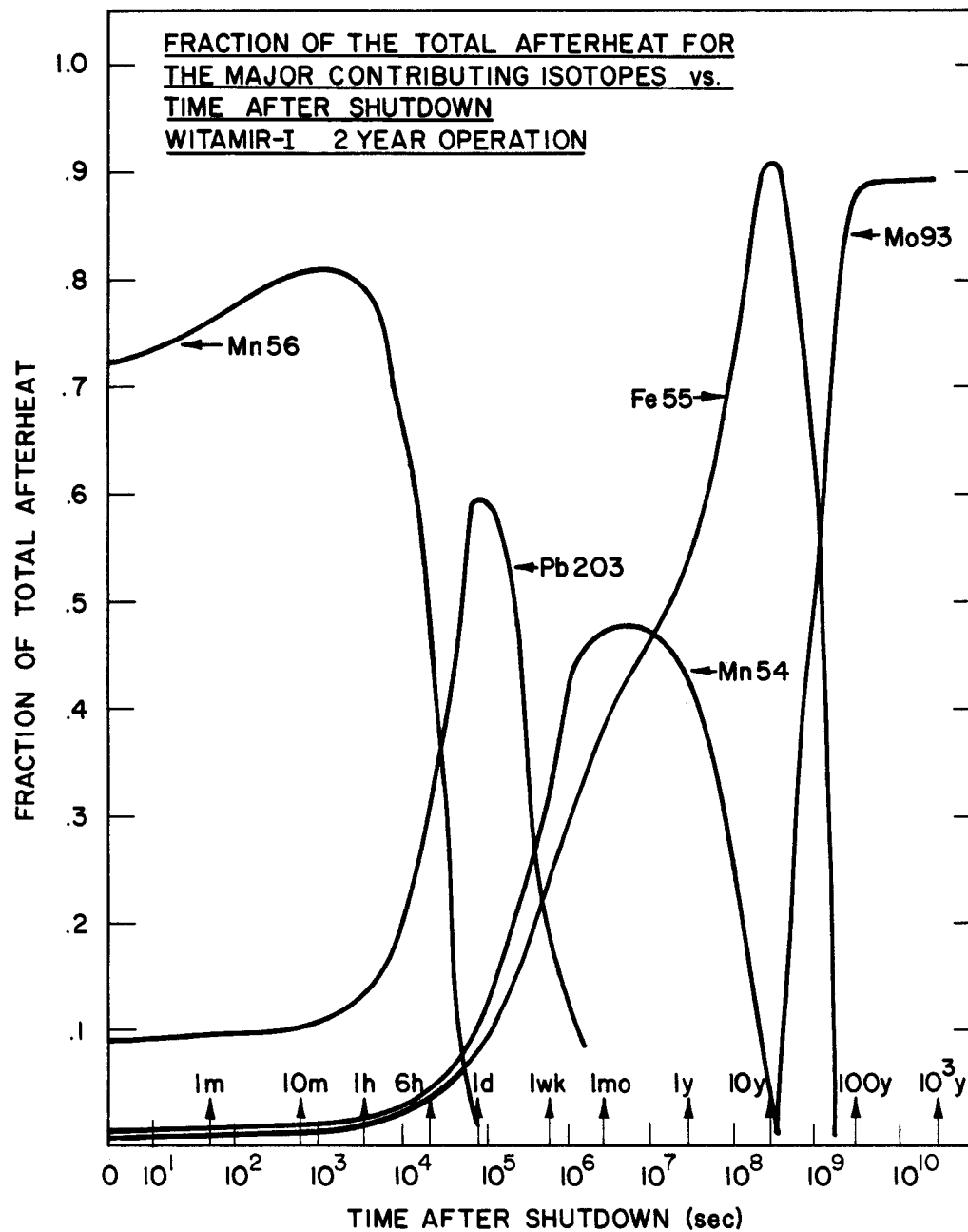


Fig. IX-B-6

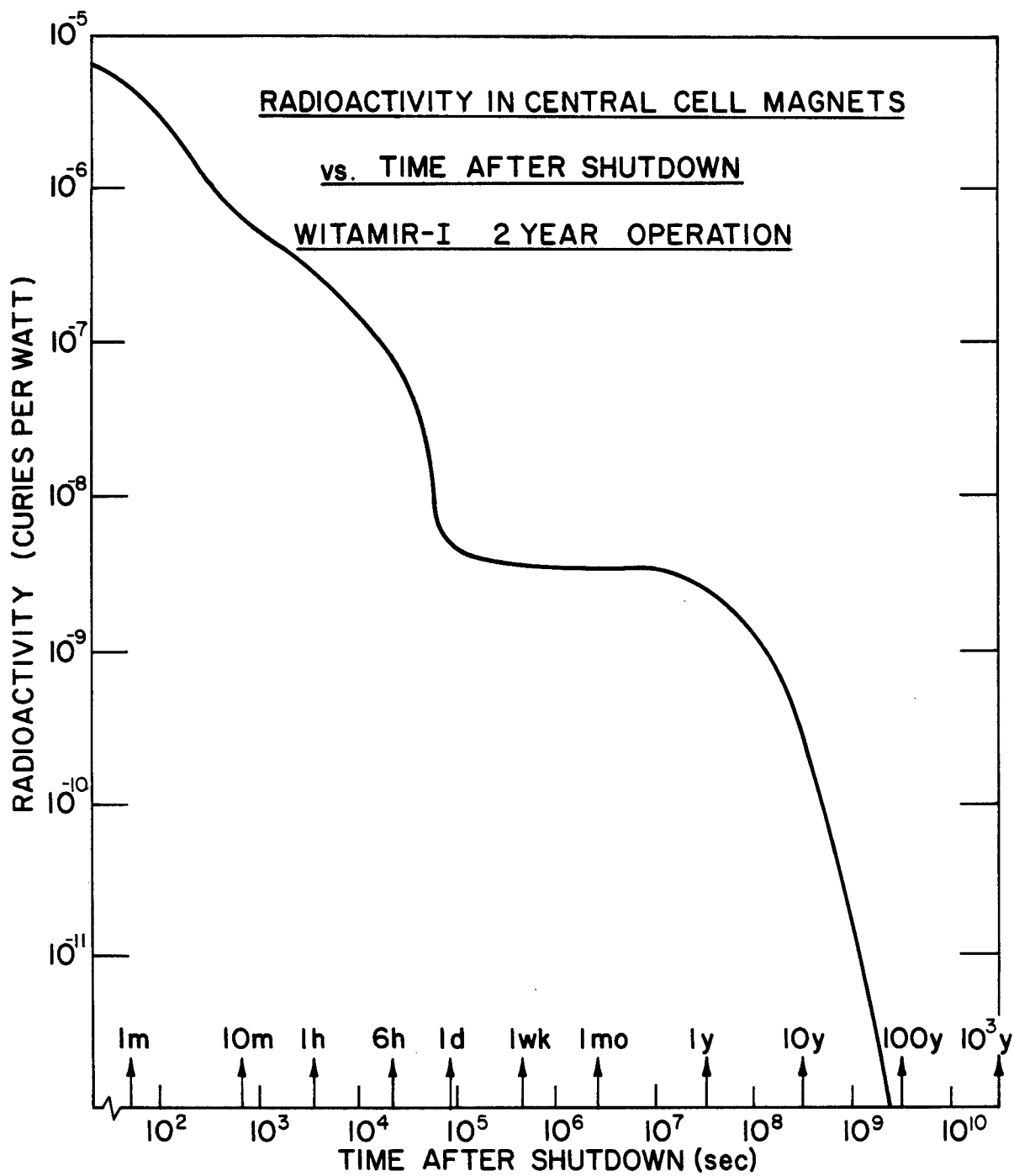


Fig. IX-B-7

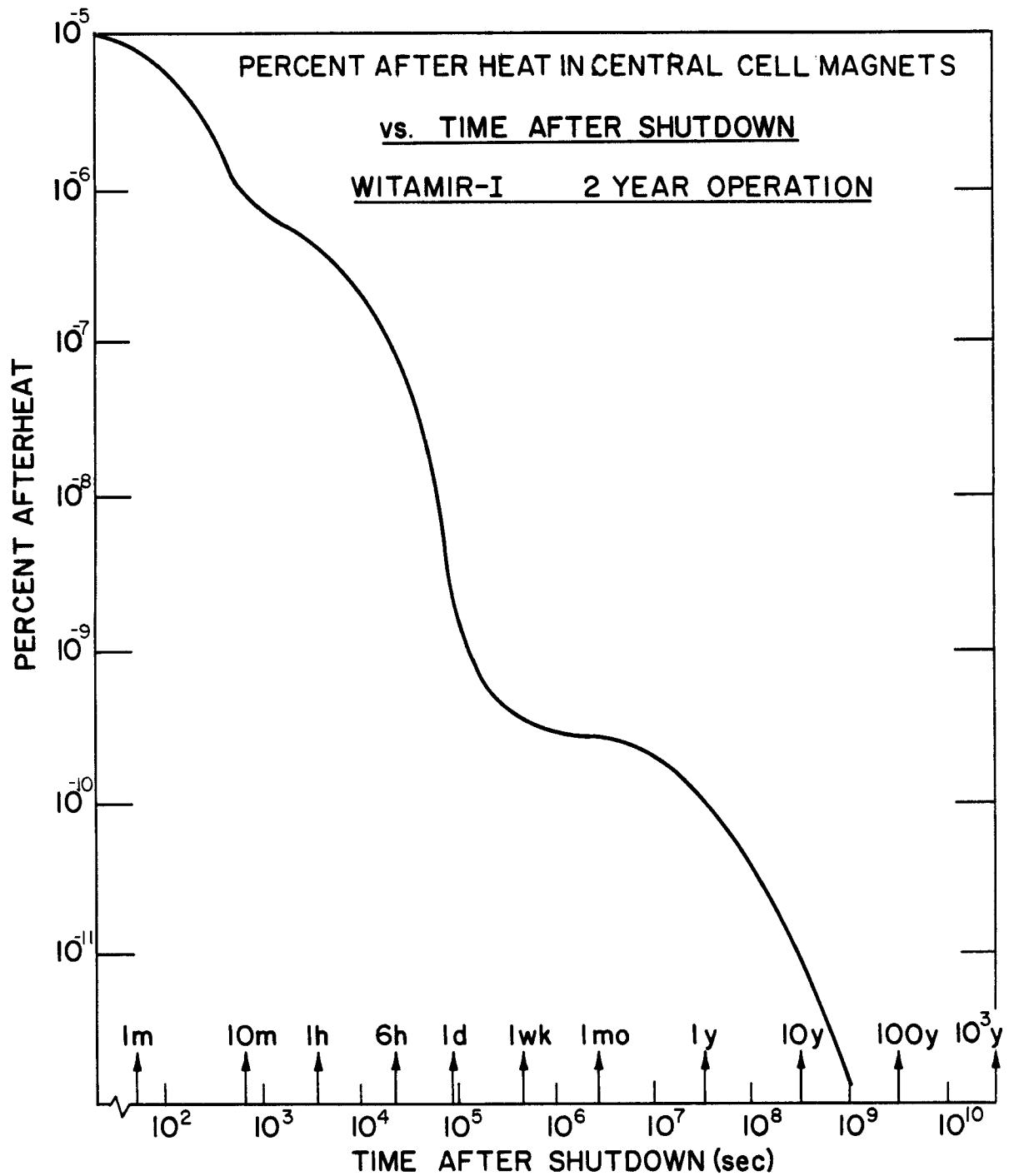


Fig. IX-B-8

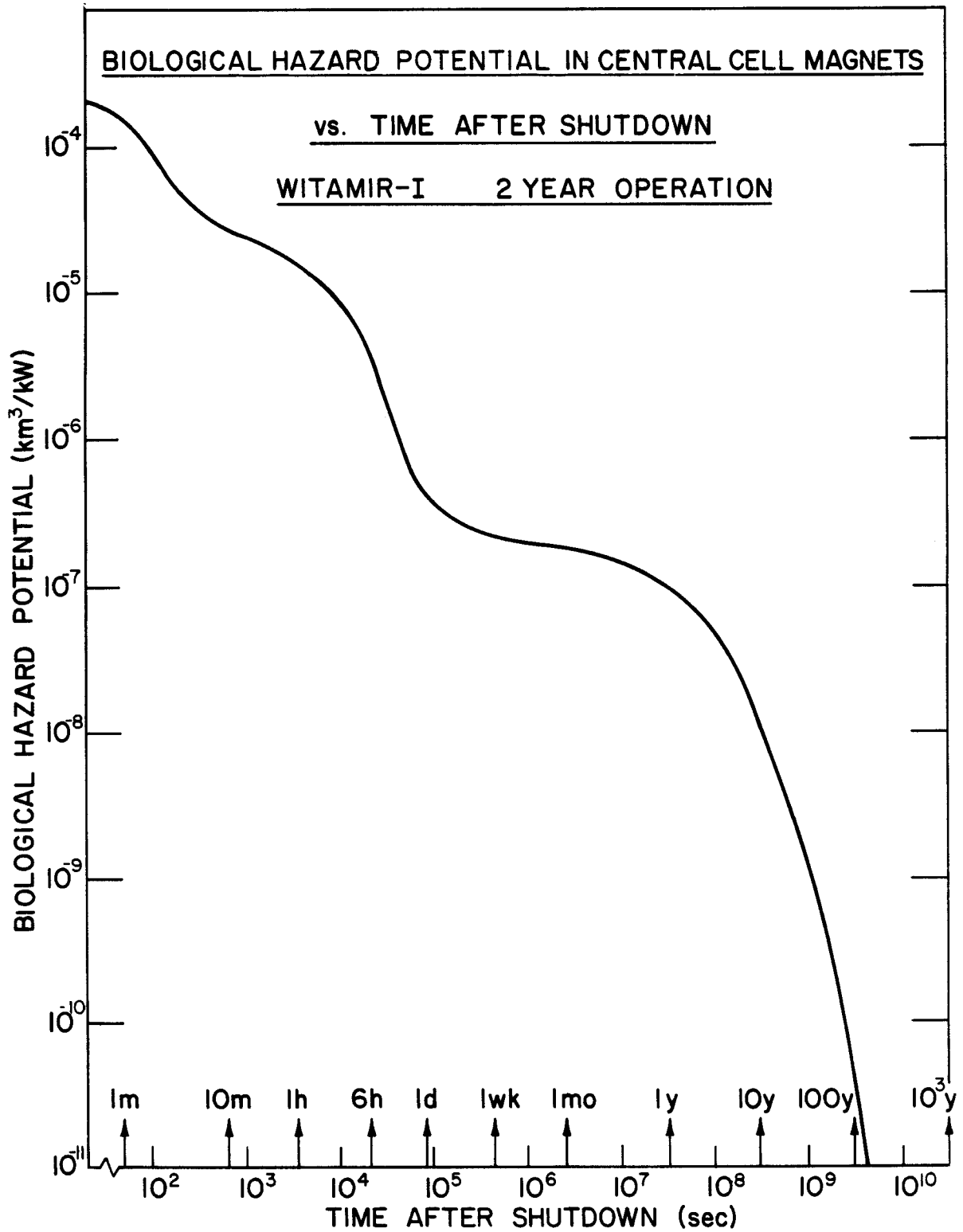


Fig. IX-B-9

about three orders of magnitude, afterheat by four orders of magnitude, and BHP by well over two orders of magnitude.

A gamma transport calculation was performed to find the dose rate outside the shield. The results of this calculation predict a dose rate of 3.9 mrem/hr at shutdown. The dose rate as a function of time after shutdown is shown in Fig. IX-B-10. There is an initial decay to less than 1 mrem/hr after 8 hours. Subsequent to this longer lived isotopes are the primary contributors to the dose and several years are required for the dose to drop below 0.1 mrem/hr. The dose from the magnets is quite low being only 1 mrem/hr at shutdown and dropping off quite rapidly after shutdown. From Fig. IX-B-11 it is seen that after one day the dose is down by over three orders of magnitude and makes little contribution to the overall dose rate. Since the blanket of WITAMIR-I has almost no penetrations for auxiliary equipment these dose rates should be representative of the system and would seem to allow for direct access to the outside of the blanket assemblies.

IX-C. Radiation Streaming

IX-C-1. Introduction

The present section is concerned with the neutronic shielding aspects of the barrier and end plugs of our tandem mirror reactor (TMR). As shown in Fig. IX-C-1, the system comprises a central cell surrounded by solenoidal magnets and end plugs consisting of magnets of complex geometrical configurations. Neutral beam injection and radiofrequency heating are done outside the central cell. This is a major advantage from the shielding point of view compared to other fusion concepts. In the TMR case, the neutral beam injectors and the radiofrequency heating ducts will not be in direct line of sight of the central cell fusion plasma neutrons and as shown in Fig. IX-C-2, the D-T reaction reaches a zero value at the center of the end plug. The barrier cell will have a less intense source which will irradiate the heating units for this region. Injection of D into the end plug as is used in a previous design⁽²⁾ for plasma heating is intentionally avoided in favor of H ion injection heating. This eliminates significant DD and DT neutron production at these positions, and avoids serious neutron leakage through plasma heating penetrations, a problem that is generic to other fusion concepts. For example, Jung and Abdou⁽³⁾ report neutron fluxes at the end caps of neutral beam injectors for a tokamak design of the order of 10^{17} n/m² · s for a 1 MW/m² 14 MeV neutron energy wall loading. Ragheb, Klein, and Maynard⁽⁴⁾

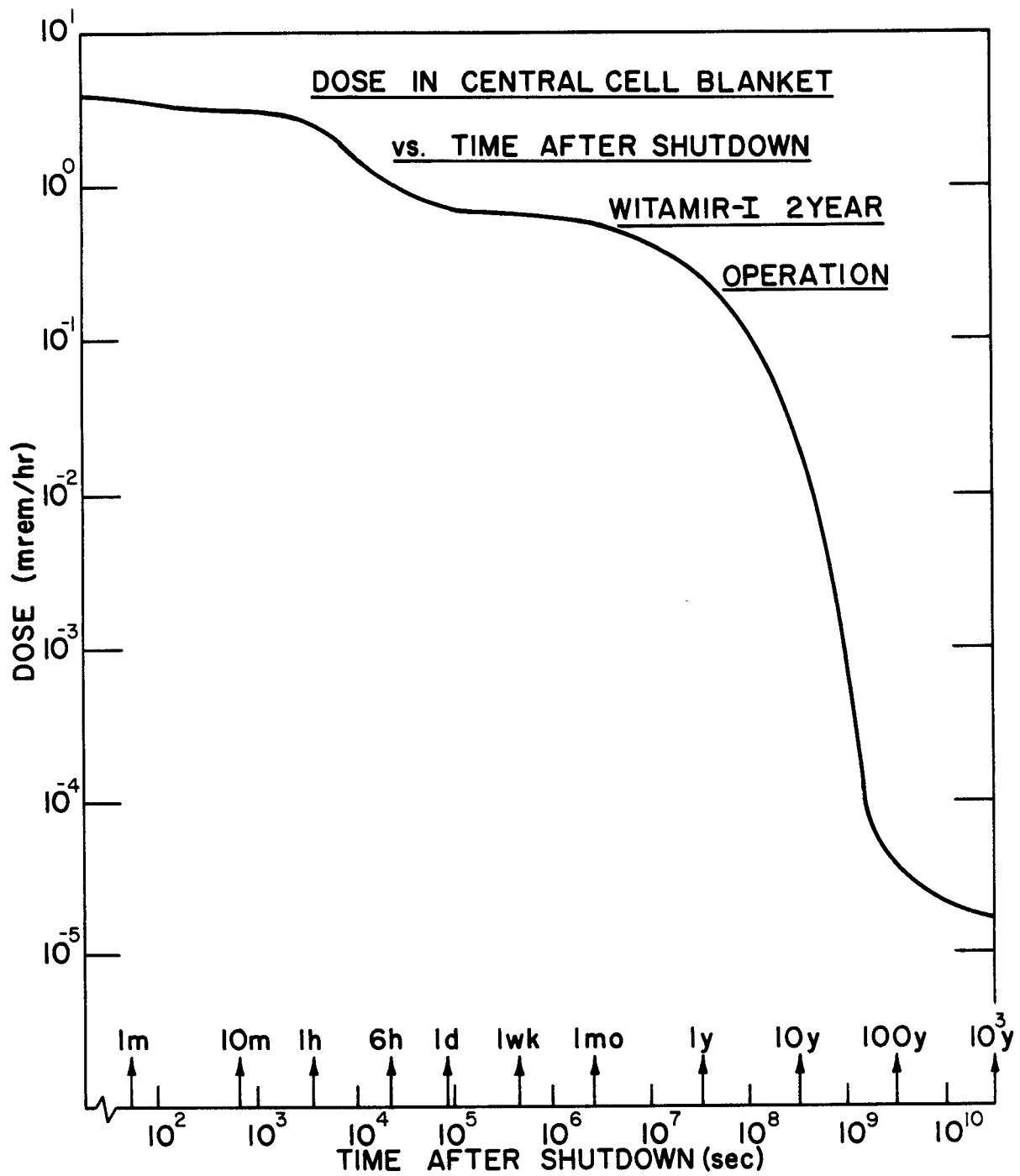


Fig. IX-B-10

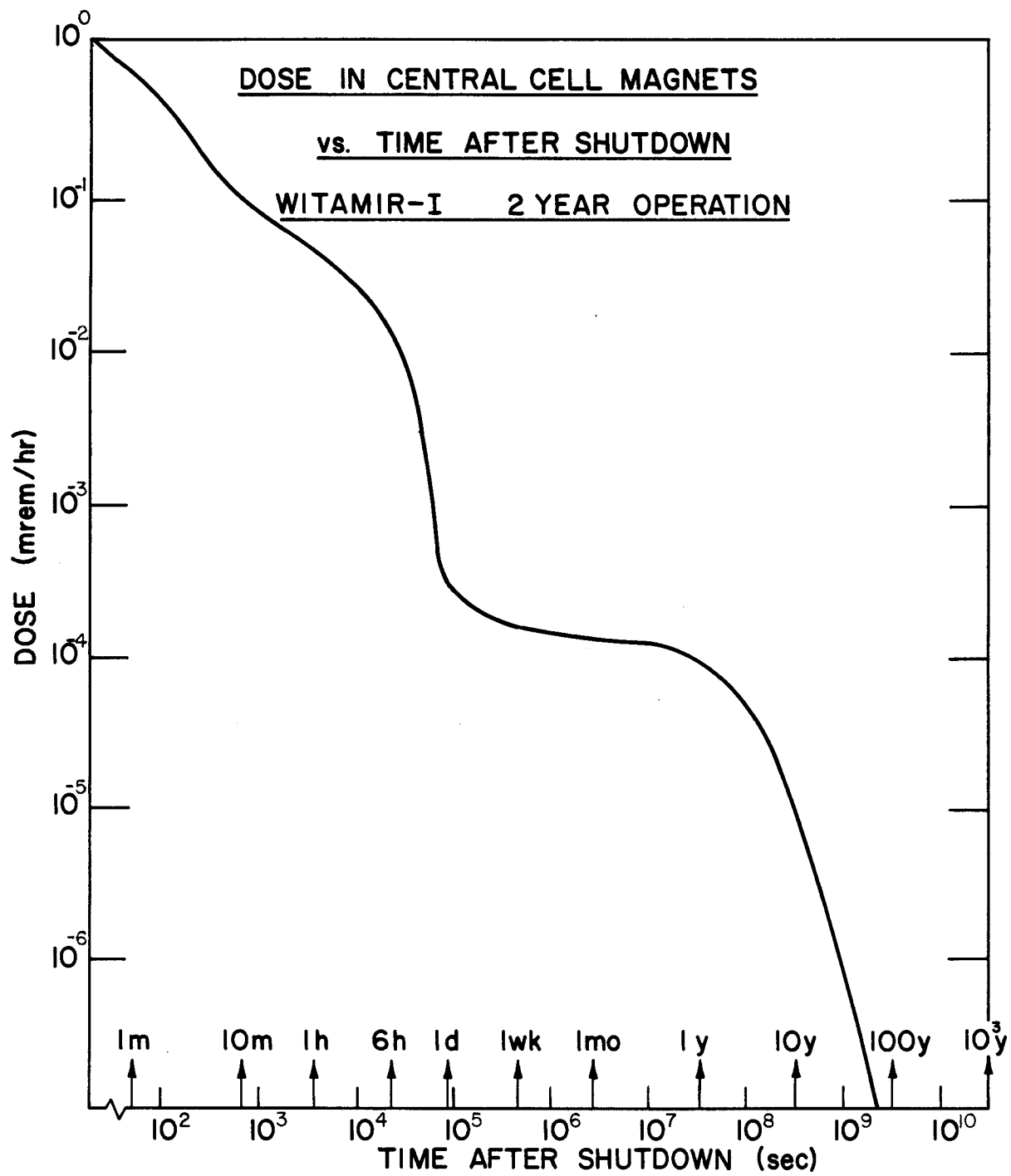


Fig. IX-B-11

WITAMIR-I MAGNET ARRANGEMENT

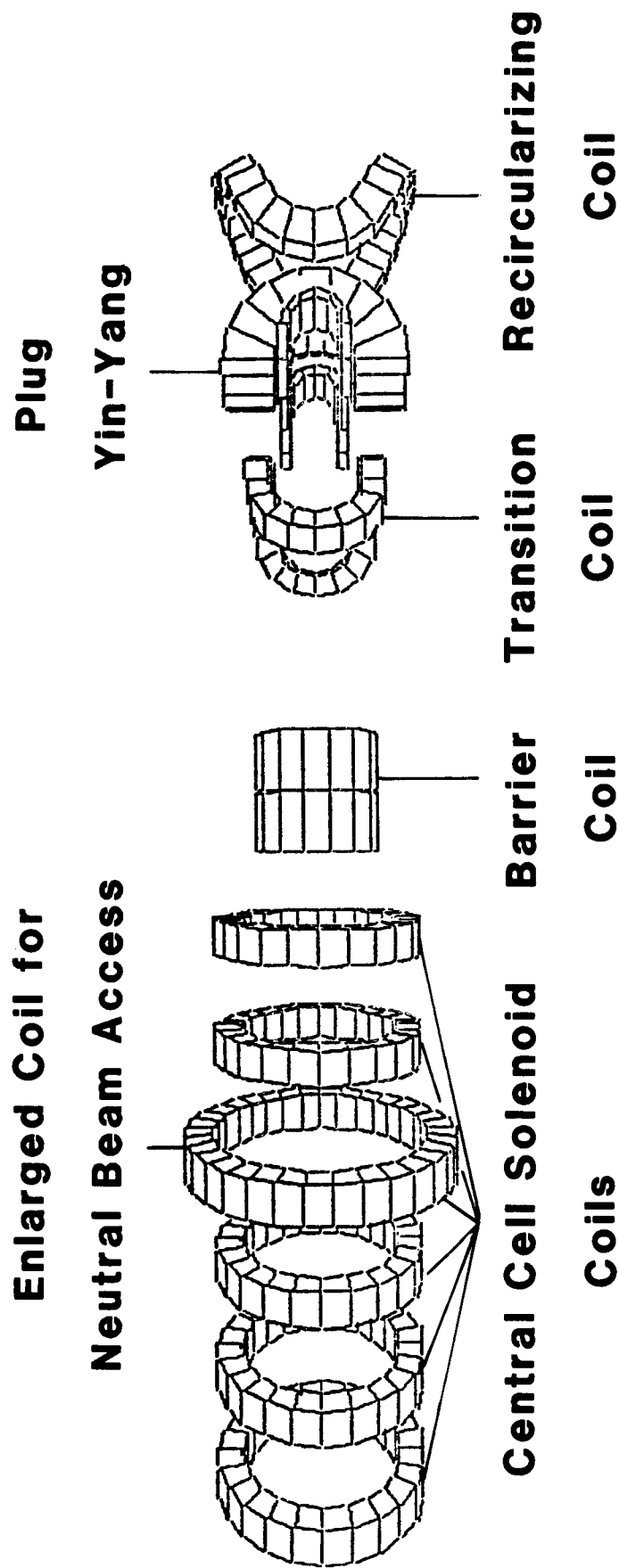


Fig. IX-C-1

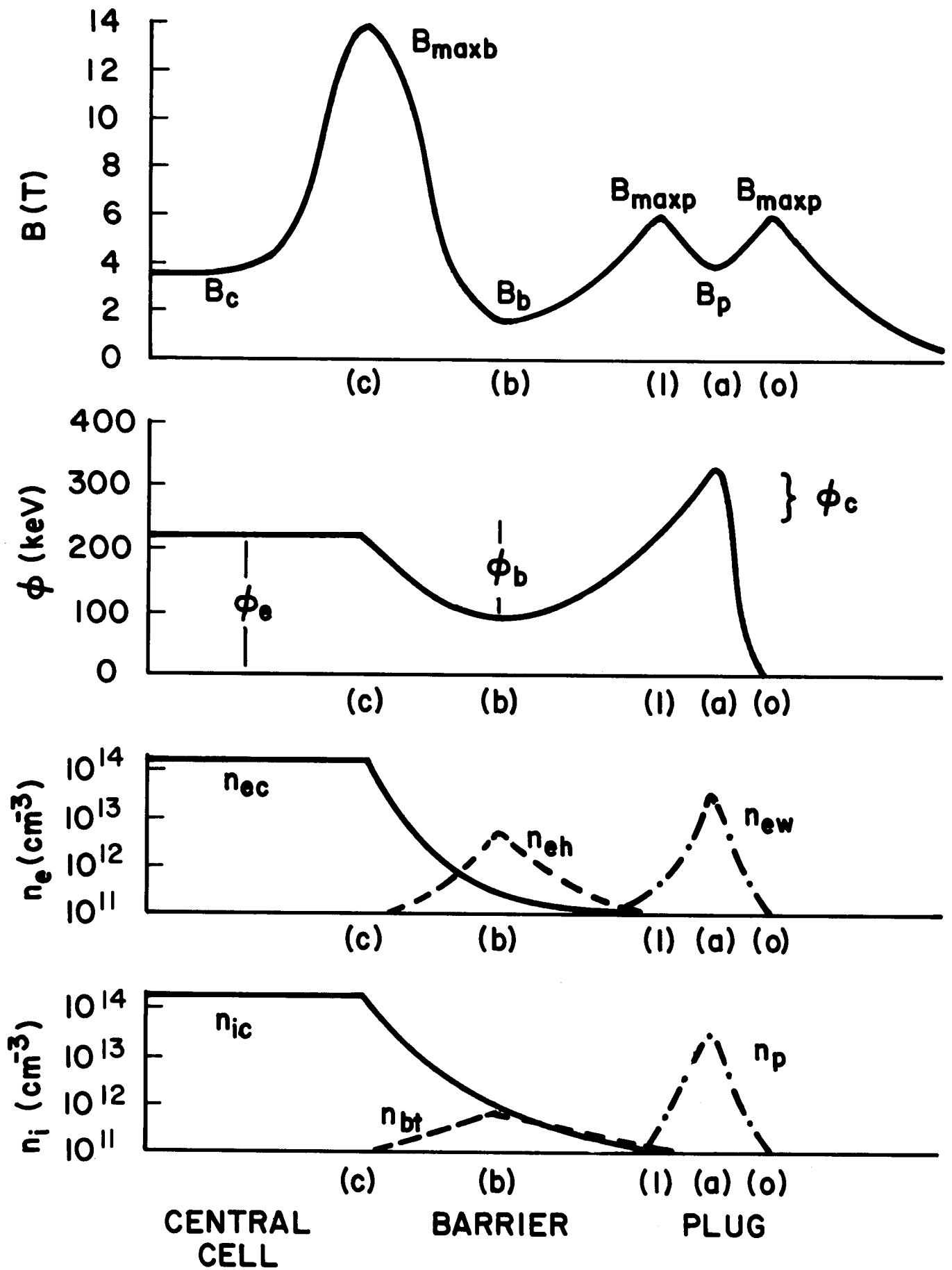


Fig. IX-C-2

report neutron fluxes of the order of 10^{14} n/m² · s after the second beam reflection in a laser-driven ICF reactor. The latter flux is estimated to lead to a 10^6 rem/hr neutron dose rate at this position for a 14 MeV neutron source strength of 10^{21} n/s.

In spite of this advantage, the end plug in the TMR is still subjected to neutron bombardment by neutrons born in the central cell portion of the reactor. In particular, the barrier cylindrical solenoid will need adequate shielding against these oncoming neutrons. The yin-yang, transition, and recircularizing coils will also be subjected to leaking neutrons. The direct convertor at the end of the end plugs will see scattered neutron radiation from the central cell. The present study was undertaken to quantify the radiation shielding and leakage problems in the end plug of a typical TMR. To deal with the complexity of the three-dimensional geometrical configurations, Monte Carlo was used for the treatment of the problem. The geometrical model will be exposed in Section IX-C-2 including an analysis of a method of position dependent angular source sampling that was developed to adequately sample the central cell neutron source. Results of calculations are discussed in Section IX-C-3.

IX-C-2. Calculational Model

Figure IX-C-3 shows the geometrical model adopted for the calculations. Because of symmetry, half the central cell is considered in the calculation with a reflecting boundary condition at its center. The half length of the central cell is 46 m and the plasma radius is 1.35 m. A clearance of 0.2 m was left between the plasma and the first wall. The blanket (Region I) was divided into two zones 1 and 2 of 0.1 and 0.653 m, respectively. The entire end plug penetration was surrounded by a 0.1 m penetration shield thickness with the composition in Table IX-C-1, and having the shape shown in Fig. IX-C-3. This shield is constricted in a conical shape inside the cylindrical solenoid and is divided for estimation purposes into four zones 3, 4, 5, and 6 in its converging part (II), and one zone 7 in its diverging part (III). The cylindrical solenoid (V) is divided into six detector zones: 9-14. Its composition is a homogenization of magnet materials as shown in Table IX-C-1.

The penetration shield for the front transition coil (IV) is designated as zone 8, and takes the form of a parallelepiped that starts with a wide base and then is constricted to a thin slit of 0.34 m width and 4.42 m length. Through this slit the magnetic field lines take the form of a fan and enter

Table IX-C-1

Material Compositions and Elemental Mixes Used in Calculations

Region	Composition	Nuclide Density [atoms/barn.m)] x 10 ⁻²
1. <u>Blanket</u>	Pb	2.381-02
90v/o Li ₁₇ Pb ₈₃ Breeder and Coolant	⁷ Li	4.512-03
+ 10v/o Ferritic Steel (11 w/o Cr	⁶ Li	3.658-04
+ 89 w/o Fe)	Fe	7.678-03
	Cr	1.019-03
2. <u>Shield</u>	Fe	4.607-02
60v/o Ferritic Steel + 15v/o Pb	Cr	6.115-03
+ 15v/o B ₄ C + 5v/o H ₂ O	Pb	4.945-03
	¹⁰ B	3.270-03
	¹¹ B	1.308-02
	¹² C	4.087-03
	¹ H	3.337-03
	¹⁶ O	1.668-03
3. <u>Magnets</u>	²⁷ Al	3.629-02
60v/o Al + 30v/o Cu + 10v/o He	Cu	2.547-02
	⁴ He	1.836-03
4. <u>First Wall</u>	Fe	7.678-02
Ferritic Steel (11 w/o Cr	Cr	1.019-02
+ 89 w/o Fe)		
5. <u>Reflector</u>	Fe	7.294-02
95v/o Ferritic Steel	Cr	9.682-03
+ 5v/o H ₂ O Coolant	¹ H	3.337-03
	¹⁶ O	1.668-03

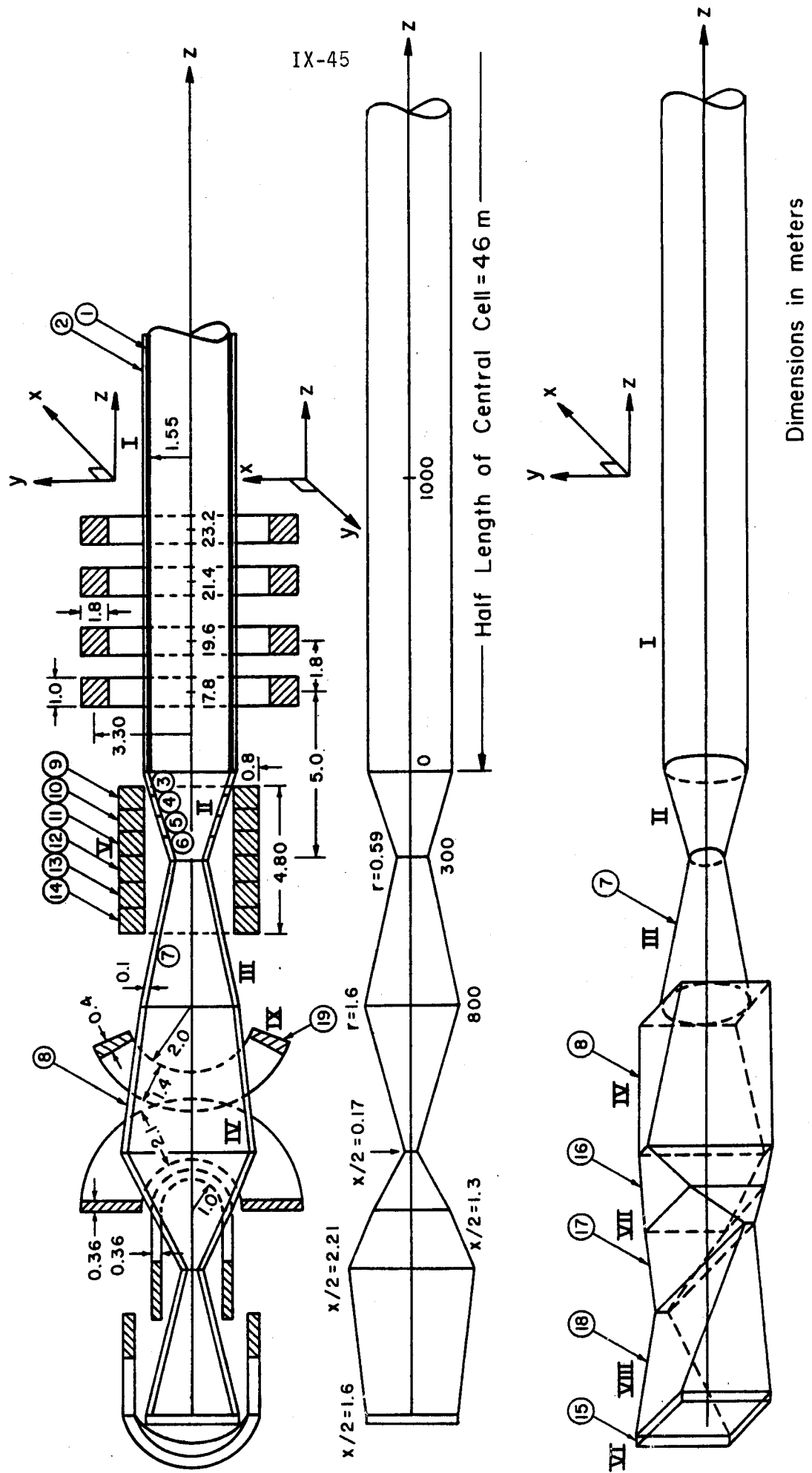


Fig. IX-C-3. Geometry of calculational model for endplug cavity.

the yin-yang coil region (VII). In this region the penetration shield is divided into two zones 16 and 17. The end part shield for the second recircularizing coil is designated as region VIII (zone 18). To quantify the neutron leakage from the system, the penetration is ended by an end plug slab (VI) of the same material as the penetration shield. Notice that the penetration shield undergoes two perpendicular squeezes into thin slits so that the end slab sees in direct line of sight a small solid angle of the central cell neutron source.

The first transition coil is modelled in its exact dimensions as shown in Fig. IX-C-4 and is designated as region IX (zone 19).

We concentrate our attention in the present work on developing the methodology to adequately sample the central cell neutron source as shown in the next subsection and study its effect on the end part of the penetration, in particular the constriction part (region II), recircularizing (IX), and cylindrical (V) coils, as well as the leakage from the system at the slab plug (VI). These are the parts to be most affected by the central cell neutron source. Future studies will also have to consider the yin-yang coils and the second recircularizing coil, and the contribution to the neutron production (even though much smaller than in the central cell) at the barrier region (regions II, III, and IV).

IX-C-2-b. Position Dependent Angular Source Biasing

The total DT fusion power of the reactor is $P_f = 3000$ MW. This corresponds to a source strength of:

$$S = \frac{P_f C}{17.62} \times 10^6 \frac{[14 \text{ MeV neutrons}]}{\text{second}} \quad (1)$$

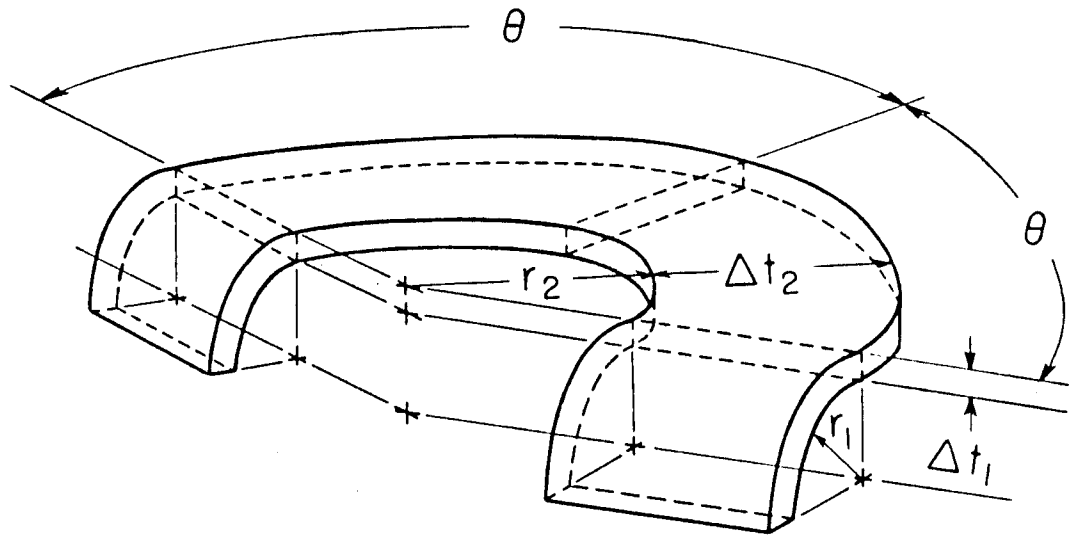
where C is a conversion factor of Joules to MeV (0.62418×10^{13}).

Substituting for the different factors we get: $S = 1.063 \times 10^{21}$ (n/sec).

This corresponds to a 14.1 MeV neutron wall loading of:

$$3000 \times \left(\frac{14.1}{17.62}\right) \times \frac{1}{2\pi(1.55)^2} = 2.68 \text{ MW/m}^2.$$

Our flux estimates will be given in units of $[n/m^2 \cdot \text{source particle}]$ so that absolute flux values can be obtained by multiplying by $S \cdot \Delta\Omega/4\pi$ to get the fluxes in units of $[n/m^2 \text{ sec}]$. We considered one quadrant of the geometry in our modeling so that $\Delta\Omega/4\pi = 1/8$.



$$\theta = 65^\circ$$

$$r_1 = 2.00, \Delta t_1 = 0.4$$

$$r_2 = 2.00, \Delta t_2 = 1.4$$

Fig. IX-C-4. Geometry on one of the recircularizing coils.
Angle of arc 65° , radius of curvature 2 m,
coil cross section 0.4 x 1.4 m.

The first column of Table IX-C-2 shows that sampling the neutron source uniformly and isotropically in the central cell fails to lead to any flux estimates at the end parts of the penetration and at the first recircularizing coil, even when larger numbers of histories were used. Thus it was necessary to develop the technique of position dependent angular source biasing as explained below. The technique is an extension of the well-known method of angular source biasing, where the angular biasing of a source particle will depend on its position in the penetration.

First an axial position z is sampled uniformly along half the penetration length from the distribution function:

$$p(z) = \frac{dz}{\int_0^L dz} = \frac{dz}{L} . \quad (2)$$

(We experimented with biased source position sampling with an isotropic source without the same improvement effect as in the case of angular source biasing.)

Second, the solid angle subtended by the penetration end from that position z is calculated as:

$$\Omega = \int d\Omega = \int \frac{ds}{r^2} = \int \frac{r^2 \sin \theta d\theta d\phi}{r^2} 2\pi (1 - \cos \theta_1) .$$

Writing down the expression for $\cos \theta_1$ in terms of the penetration radius R , cell length L (see Fig. IX-C-5), and axial position z , we get:

$$\Omega = 2\pi \left[1 - \frac{1}{\sqrt{1 + \left(\frac{R}{L-z}\right)^2}} \right] . \quad (3)$$

For an isotropic source, the probability of a particle emerging within Ω is:

$$p_1 = \frac{\Omega}{4\pi} \quad (4)$$

and the probability of a particle emerging in the solid angle $(4\pi - \Omega)$ will in turn be:

$$p_2 = 1 - p_1 = 1 - \frac{\Omega}{4\pi} . \quad (5)$$

Table IX-C-2
 Scalar Neutron Fluxes [$\text{n/m}^2 \cdot \text{source particle}$] $\times 10^{-4}$
 For Isotropic and Forward Biased Source
 Sampling in Different Reactor Regions (2000 histories)

Region	Zone	Isotropic Uniform Source in Central Cell	Forward Biased, Uniform Source in Central Cell
I. Blanket Central Cell	1	2.5771-05(0.027)*	3.7952-05(0.096)
	2	8.7237-06(0.009)	6.6793-06(0.091)
II. Barrier Solenoid Front Shield	3	5.7600-06(0.154)	5.2856-06(0.174)
	4	4.9386-06(0.160)	3.1065-06(0.233)
	5	4.1473-06(0.218)	2.3040-06(0.197)
	6	3.0498-06(0.202)	2.5236-06(0.273)
III. Barrier Solenoid Back Shield	7	1.9361-07(0.240)	1.1663-07(0.307)
IV. Transition Coil Shield	8	5.5724-08(0.439)	3.1038-08(0.760)
V. Barrier Solenoid	9	6.5995-07(0.269)	1.2934-07(0.314)
	10	3.4467-07(0.374)	1.5548-07(0.294)
	11	7.0234-08(0.469)	1.9865-07(0.378)
	12	9.6962-08(0.830)	9.4308-08(0.520)
	13	9.2967-08(0.720)	3.9598-08(0.685)
	14	2.0241-08(0.573)	3.7901-09(0.924)
VI. Penetration Plug	15	-	1.7648-09(0.592)
VII. Yin-Yang Coil Shield	16	-	3.9392-09(0.722)
	17	-	5.5289-09(0.924)
VIII. End Part Shield	18	-	1.3640-10(0.570)
IX. Transition Coil	19	-	4.0982-10(0.922)

*Numbers in parentheses are fractional standard deviations.

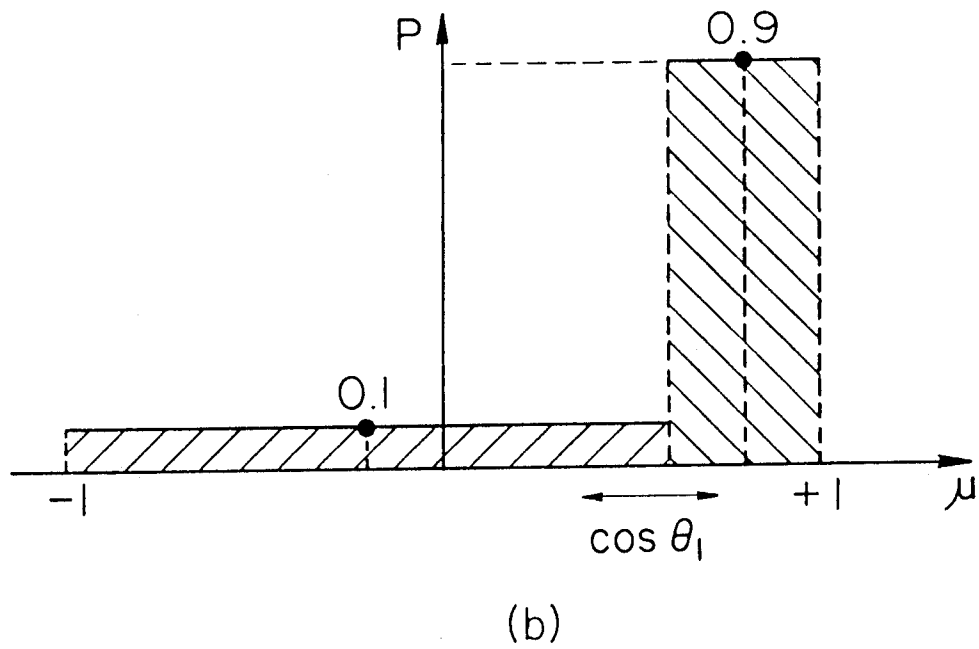
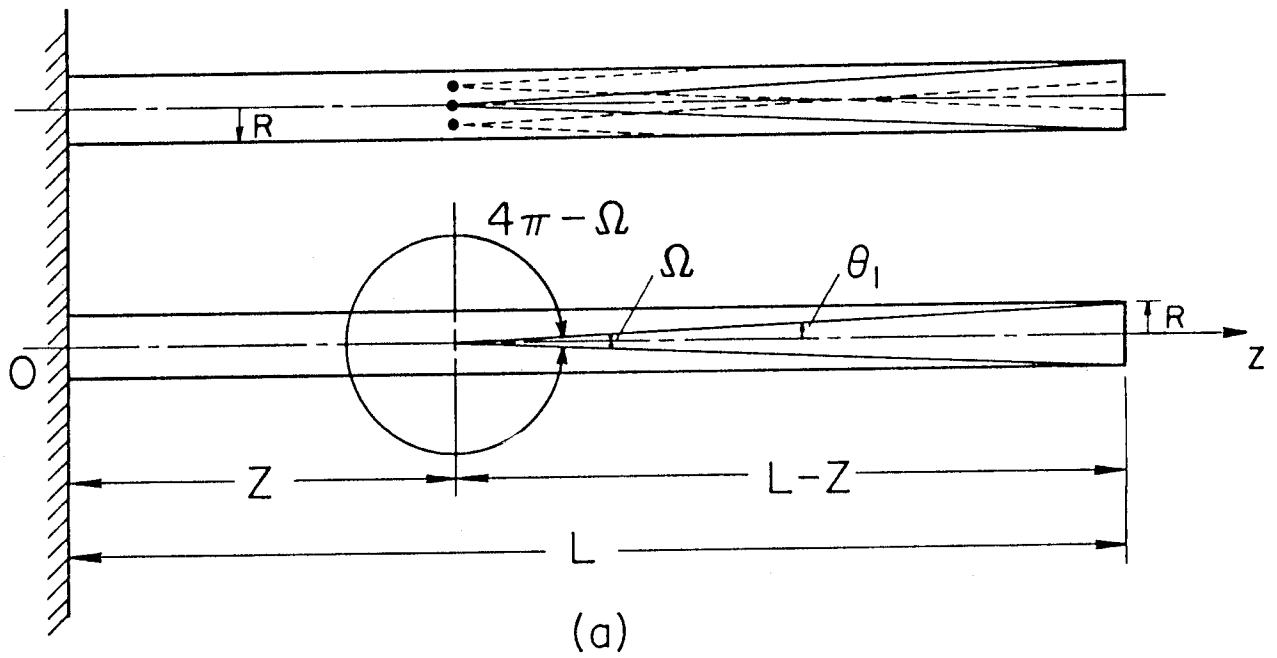


Fig. IX-C-5. Position dependent angular source biasing.

One can assign arbitrary probabilities p_1^i and p_2^i , for particle births in either Ω or $(4\pi - \Omega)$ solid angles, provided their statistical weights w_1 and w_2 are adjusted according to:

$$\left. \begin{aligned} p_1^i w_1 &= p_1 \\ p_2^i w_2 &= p_2 \end{aligned} \right\} \quad (6)$$

such that:

$$p_1^i + p_2^i = 1 \quad .$$

Substituting from Eqns. 4 and 5 into Eqn. 6, we get:

$$\left. \begin{aligned} w_1 &= \frac{\Omega}{4\pi} \cdot \frac{1}{p_1^i} \\ w_2 &= \left(1 - \frac{\Omega}{4\pi}\right) \cdot \frac{1}{p_2^i} \end{aligned} \right\} \quad (7)$$

As an example, if $\Omega = 2\pi$, with $p_1^i = 1$ and $p_2^i = 0$; implying $w_1 = 0.5$, this is a case of forward source biasing. If $p_1^i = 0.9$ and $p_2^i = 0.1$, then the source particle weights would be $w_1 = \frac{2\pi}{4\pi} \cdot (1/0.9) = 0.5555$, and $w_2 = \frac{2\pi}{4\pi} \cdot \frac{1}{0.1} = 5.0$. In both cases the probabilities $p_1 = p_2 = 0.5$ are preserved.

In our calculations, we experimented with several values of p_1^i and p_2^i and chose $p_1^i = 0.9$ and $p_2^i = 0.1$. This choice leads to particles reaching the reactor extremities without collision 90% of the time and particles suffering at least one collision before reaching these zones 10% of the time. In this way, contributions from all particles will still be obtained. This is thought better than considering purely forward source biasing without the other component. Figure IX-C-5 shows these choices of the cosine of the angle θ , where direct particles are favored with respect to indirect particles. In this case the source statistical weight adjustment for direct and indirect moving particles will be respectively:

$$w_1 = \frac{1}{2} \left[1 - \frac{1}{\sqrt{1 + \left(\frac{R}{L-z} \right)^2}} \right] \cdot \frac{1}{p_1} \quad (8)$$

$$w_2 = 1 - \left[1 - \frac{1}{\sqrt{1 + \left(\frac{R}{L-z} \right)^2}} \right] \cdot \frac{1}{p_2}$$

by use of Eqns. 3 and 7.

A radius is then sampled for the source particle from the distribution function:

$$p(r)dr = \frac{2\pi r dr}{\int_0^R 2\pi r dr} = \frac{2r dr}{R^2} \quad (9)$$

and an azimuthal angle from the distribution function:

$$p(\phi)d\phi = \frac{d\phi}{\int_0^{2\pi} d\phi} = \frac{d\phi}{2\pi} \quad (10)$$

where we are using one quadrant of the geometry with reflecting boundaries to account for symmetry.

The last step in the source sampling is the sampling of the polar angle. For direct directions this is sampled using the distribution function:

$$\rho(\phi) = \frac{2\pi \sin \theta d\theta}{2\pi \int_0^{\theta_1} \sin \theta d\theta} \quad (11)$$

which samples isotropic vectors within the conical half angle:

$$\theta_1 = \cos^{-1} \left[\frac{(L - z)}{\sqrt{R^2 + (L - z)^2}} \right] .$$

In the same way for indirect directions,

$$p(\theta) = \frac{2\pi \sin \theta d\theta}{2\pi \int_{\theta_1}^{\pi} \sin \theta d\theta} . \quad (12)$$

Where the above methodology is applied, contributions to different parts of the end plug penetration can now be obtained as shown in Table IX-C-2, even for small numbers of particle histories (2000 histories). In the next section we discuss the application of the developed methodology to obtain quantities of interest in the penetration.

IX-C-3. Discussion of Results

IX-C-3-a. Scalar Flux Distributions

The methodology developed in the last section succeeds in obtaining estimates in the end plug penetration caused by the central cell neutron source. Table IX-C-3 shows these results and their corresponding standard deviations for 2000 and 8000 particle histories, for a penetration shield thickness of 0.1 m.

For the case of 8000 histories, at the penetration plug (region VI) we get an estimate of the flux with 36% fractional standard deviation which is quite good for our type of survey calculations. Using the source strength estimate for this particle design from section IX-C-2-b, we can estimate the leakage neutron flux as:

$$\frac{1.063 \times 10^{21}}{8} \times 9.8492 \times 10^{-10} \times 10^4 = 1.31 \times 10^{15} (\text{n/m}^2 \cdot \text{s})$$

where the division by the factor 8 accounts for using one quadrant of the geometry in our modeling. This identifies the need for adequate shielding at the end of the penetration to avoid serious activation of the charged particle direct-energy convertor beyond this point. As shown in Table IX-C-4, the

contribution to the scalar flux there is $\frac{4.7178 \times 10^{-11}}{9.8419 \times 10^{-10}} \approx 5\%$ from direct line

of sight 14 MeV neutron radiation, the rest being intermediate energy group components, since the thermal neutron components would have been absorbed along the penetration.

As previously done in a laser-fusion reactor⁽⁵⁾ penetration the incorporation of boron in the material of the penetration liner would reduce this leakage, e.g., by use of Boral. The use of a flux trap at this point seems also necessary at the location of the penetration as suggested for a similar system in reference (5). The charged particles to the direct convertor can be

Table IX-C-3
 Effect of Increasing the Number of Histories
 on the Flux Estimates in the Central Cell and End Plug of the TMR.
 Uniformly Distributed, Forward Biased Source in Central Cell.
 $[n/m^2 \cdot \text{source particle})] \times 10^{-4}$
 Effective Penetration Shield Thickness = 0.1 m

Region	Zone	2000 Histories	8000 Histories
I. Blanket Central Cell	1	3.7952-05(0.096)*	4.1262-05(0.043)
	2	6.6793-06(0.091)	7.7188-06(0.043)
II. Barrier Solenoid Front Shield	3	5.2856-06(0.174)	5.2437-06(0.085)
	4	3.1065-06(0.233)	4.8442-06(0.099)
	5	2.3040-06(0.197)	3.7601-06(0.124)
	6	2.5236-06(0.273)	2.3820-06(0.147)
III. Barrier Solenoid Back Shield	7	1.1663-07(0.307)	1.4502-07(0.227)
IV. Transition Coil Shield	8	3.1038-08(0.760)	3.4541-08(0.276)
V. Barrier Solenoid	9	1.2934-07(0.314)	2.4778-07(0.185)
	10	1.5548-07(0.294)	2.0933-07(0.250)
	11	1.9865-07(0.278)	1.9423-07(0.230)
	12	9.4308-08(0.520)	7.6307-08(0.318)
	13	3.9598-08(0.685)	4.8229-08(0.352)
	14	3.7901-09(0.924)	3.4796-09(0.404)
VI. Penetration Plug	15	1.7648-09(0.592)	9.8492-10(0.360)
VII Yin-Yang Coil Shield	16	3.9392-09(0.722)	2.0527-09(0.615)
	17	5.5289-09(0.570)	3.1262-09(0.655)
VIII. End Part Shield	18	1.3640-10(0.570)	7.2900-11(0.333)
IX. Transition Coil	19	4.0982-10(0.922)	3.4235-09(0.825)

*Numbers in parentheses are fractional standard deviations

Table IX-C-4

Total, Thermal, and 14 MeV Group Neutron Flux Components in
 Different TMR Regions. Effective Penetration Shield Thickness = 0.2 m.
 Number of Particle Histories: 8000. $[n/(m^2 \cdot \text{source particle})] \times 10^{-4}$.

Region	Zone	Total Scalar Flux	Thermal Group Flux	14 MeV Group Flux
I. Blanket Central	1	4.0225-05	9.4417-11	8.3862-07
Cell	2	7.6330-06	1.7644-11	3.3045-08
II. Barrier Solenoid	3	4.6877-06	2.1065-12	1.9958-07
Front Shield	4	3.1426-06	7.8637-12	9.0889-08
	5	2.0096-06	2.3138-12	3.4453-08
	6	1.1431-06	3.4976-12	8.5859-09
III. Barrier Solenoid	7	8.9974-08	4.7289-14	1.1205-10
Back Shield				
IV. Transition Coil	8	3.1736-08	1.3962-15	9.0361-11
Shield				
V. Barrier Solenoid	9	6.3143-08	1.2200-13	1.3733-11
	10	4.1566-08	3.3235-12	1.6357-12
	11	3.5821-08	8.0941-13	
	12	9.2307-09	1.8568-14	
	13	8.9983-09		
	14	6.9132-10		
VI. Penetration Plug	15	9.8419-10		4.7178-11
VII. Yin-Yang Coil	16	2.6279-12		1.6843-14
Shield	17	9.5934-10		2.3358-12
VIII. End Part Shield	18	4.5833-11		9.4176-13
IX. Transition Coil	19	1.0475-11		

bent using magnets as suggested by Moir, Barr and Miley⁽⁶⁾, to let the neutrons fall into the flux trap.

A matter of previous concern was the tapered portion of the barrier solenoid shield. On the one hand, this part sees the whole length of the central cell source in a line-of-sight manner, but on the other hand it sees the neutron source from one side only compared to a first wall point, which sees it from both sides so that an estimate of the flux was needed. Our calculations show that the flux there will be less than at the first 10 cm of the blanket in the central cell. In fact, from Table IX-C-3, in zone 3 of the

barrier solenoid front shield the scalar flux is: $\frac{5.2437 \times 10^{-6}}{4.1262 \times 10^{-5}} \approx 12.7\%$ of

that at the first wall. From Table IX-B-4, the 14 MeV group flux ratio is

$$\frac{1.9958 \times 10^{-7}}{8.3862 \times 10^{-7}} \approx 23.8\%.$$

The transitional coil (Region IX) coil received an average flux of (multiplying the values of Table IX-C-3 by the source strength):

$$\frac{1.063 \times 10^{21}}{8} \times 3.4235 \times 10^{-9} \times 10^4 = 4.549 \times 10^{15} \text{ (n/m}^2 \cdot \text{s) .}$$

This is substantial and means that the originally chosen penetration shield thickness of 0.1 m is insufficient.

The effect of altering the material thickness of this penetration shield on neutron fluxes in the magnets surrounding the penetration will be studied by varying the effective optical thickness of the penetration shield rather than changing the actual geometry. This is achieved by modifying the macroscopic cross section by a factor n as:

$$\Sigma = n\Sigma_0 = nN_0\sigma \quad (13)$$

where: Σ , Σ_0 are the modified and original macroscopic cross sections⁽¹²⁾, respectively,

N_0 is the original nuclide density,

σ is the material microscopic cross section.

For an optical thickness of the original system:

$$\ell_0 = \frac{t}{\lambda_0} = t \cdot \Sigma_0 \quad (14)$$

where: t is the geometrical thickness,

λ_0 is the original material mean free path,

the modified optical thickness will be (using Eqns. 13 and 14):

$$\ell = \frac{t}{\lambda} = t \Sigma = nt\Sigma_0 = n\ell_0 . \quad (15)$$

Thus the complicated task of changing a complex three dimensional geometry can be replaced by the simple task of modifying the mixing of the cross section by a factor n multiplying the number densities of the materials in the code input; this will lead to the increase of the optical thickness of a given region n times.

Table IX-C-5 shows the results of this type of calculation where the effective penetration thickness is increased for 0.1 m to 0.2 m by the use of $n = 2$ in Eqn. 15. This increase in the shield thickness leads to about an order of magnitude attenuation of the scalar flux at the barrier solenoid (region V), and to two orders of magnitude attenuation at the first transition coil (region IX).

IX-C-3-b. Estimated Required Penetration Shield Thickness

We use as a criterion for the magnet shielding that the limit on local nuclearly induced resistivity increase does not exceed 10% of the unirradiated value assuring cryogenic stabilization⁽⁸⁾. Cryogenic stabilization requires that the heat transfer from the stabilized conductor matrix must be sufficient to transfer the Joule (I^2R) heat generated in the stabilizing material when a flux jump occurs. Since the magnet conductor currents must remain constant, an increase in Joule (I^2R) heating will occur through an increase of conductor resistance.⁽⁶⁾ The consequences of superconducting magnets going normal in a fusion reactor are likely to be grave, due to the large amount of energy stored in them, and the activation radioactivity in the adjacent components. This makes stabilization of superconducting coils in fusion reactors an important consideration.

For Al and Cu at 20 K the resistivity is:

$$\rho_{Al} = 5.4 \times 10^{-5} \mu\Omega \cdot m ,$$

$$\rho_{Cu} = 7.7 \times 10^{-5} \mu\Omega \cdot m .$$

Table IX-C-5
 Effect of Doubling the Optical Thickness of the TMR
 End Plug Penetration Shield on Fluxes at Magnets
 (8000 Histories). $[n/m^2 \cdot \text{source particle}] \times 10^{-4}$.

Region	Zone	Effective Shield Thickness (m)	
		0.10	0.20
I. Blanket Central Cell	1	4.1262-05(0.043)*	4.0295-05(0.035)
	2	7.7188-06(0.043)	7.6330-06(0.031)
II. Barrier Solenoid Front Shield	3	5.2437-06(0.085)	4.6877-06(0.090)
	4	4.8442-06(0.099)	3.1426-06(0.104)
	5	3.7601-06(0.124)	2.0096-06(0.115)
	6	2.3820-06(0.147)	1.1431-06(0.191)
III. Barrier Solenoid Back Shield	7	1.4572-07(0.227)	8.9974-08(0.206)
IV. Transition Coil Shield	8	3.4541-08(0.276)	3.1736-08(0.276)
V. Barrier Solenoid	9	2.4778-07(0.185)	6.3143-08(0.411)
	10	2.0933-07(0.252)	4.1566-08(0.451)
	11	1.9423-07(0.230)	3.5821-08(0.774)
	12	7.6307-08(0.318)	9.2307-09(0.496)
	13	4.8229-08(0.352)	8.9983-09(0.777)
	14	3.4796-09(0.404)	6.9132-10(0.728)
VI. Penetration Plug	15	9.8492-10(0.360)	9.8419-10(0.548)
VII. Yin-Yang Coil Shield	16	2.0527-09(0.615)	2.6279-12(0.527)
	17	3.1262-09(0.655)	9.5934-10(0.977)
VIII. End Part Shield	18	7.2900-11(0.333)	4.5833-11(0.369)
IX. Transition Coil	19	3.4235-09(0.825)	1.0475-11(0.874)

*Numbers in parentheses are fractional standard deviations

The maximum allowable increase according to our stated criterion will be:

$$\delta\rho_{Al} = \frac{\rho_{Al}}{10} = 5.4 \times 10^{-6} \mu\Omega \cdot m$$

$$\delta\rho_{Cu} = \frac{\rho_{Cu}}{10} = 7.7 \times 10^{-6} \mu\Omega \cdot m .$$

An allowable flux level ϕ_a at the magnets can be calculated from:

$$\phi_a = \frac{\delta\rho}{d\rho/d\psi} \frac{1}{t_r} \quad (16)$$

where: t_r is the required irradiation time (sec),
 ψ is the neutron fluence (n/m^2),
 $d\rho/d\psi$ is the slope of the relationship between ρ and ψ ,

$$\approx 2.210 \times 10^{-25} \frac{\mu\Omega \cdot m}{n/m^2} \text{ for Al,}$$

$$\approx 6.500 \times 10^{-26} \frac{\mu\Omega \cdot m}{n/m^2} \text{ for Cu.}$$

Annealing at room temperature produces 85% recovery in Cu and complete recovery in Al, even though the influence of cyclic irradiation is uncertain. Thus, if we want a one year time span between room temperature anneals of cyrogenic aluminum, the allowable flux level at the magnets must be:

$$\phi_a(Al) = \frac{5.4 \times 10^{-6}}{2.210 \times 10^{-25}} \times \frac{1}{3.154 \times 10^7} = 7.747 \times 10^{11} n/(m^2 \cdot s)$$

and for copper:

$$\phi_a(Cu) = \frac{7.7 \times 10^{-6}}{6.5 \times 10^{-26}} \times \frac{1}{3.154 \times 10^7} = 3.760 \times 10^{12} n/(m^2 \cdot s) .$$

Since Al has a value of ϕ_a lower than that for Cu, for a conservative design, we use the value of ϕ_a for Al.

If we assume a flux attenuation of the form:

$$\phi = \phi_0 \exp \left(- \frac{y - x}{2\lambda} \right) \quad (17)$$

where: λ is an attenuation mean free path,

ϕ and ϕ_0 are assumed measured at the center of two penetration shield thicknesses y and x respectively, such that $y > x$; then we can estimate λ from:

$$\lambda = \frac{y-x}{\ln(\phi_0/\phi)} \text{ (m)} \quad . \quad (18)$$

For $y = 0.2$ m, $x = 0.1$ m, as used in Table IX-C-5, we can estimate local values for λ as shown in Table IX-C-6 for the magnet regions of interest. Using these calculated values of λ , one can now calculate a shield thickness y for a given allowable flux ϕ_a from

$$y = x + 2\lambda \ln \frac{\phi_0}{\phi_a} \quad . \quad (19)$$

For the $x = 0.1$ m case results from Table IX-C-5, values of the required penetration shield thicknesses are shown in Table IX-C-6.

Based on the chosen criterion it appears that about 1.05 m of shield thickness will be sufficient to shield the cylindrical solenoid of the barrier coil, particularly its zone 9.

The shielding for the transition coil, however, needs only 0.247 m of shielding with the composition of Table IX-C-1.

IX-C-3-c. Heating Rates in Different Penetration Components

Table IX-C-7 shows the heating rates due to neutrons both in units of MeV/source neutron, and as volumetric heating rates in units of MW/m³. It can be noticed that the barrier solenoid shield has a high volumetric heating rate (5.8 MW/m³) that is comparable to that at the central cell (8.1 MW/m³). As a consequence this part of the shield will require a special thermal hydraulic design similar to that of the first wall/blanket region. The heating rate drops to low values in other parts of the penetration, and no substantial cooling is needed there. These estimates need to be supplemented by estimates of γ -ray heating for an exact determination of the nuclear heating. The sum of the total nuclear heating and the nuclear-induced I^2R heating through the increase in resistivity must then be made to be a fraction of the I^2R heating in the unirradiated coil, through adequate shielding.

In parts of the shield with low volumetric heating rates, low cost concrete or lead mortar shielding⁽⁴⁾ can replace the more expensive steel, lead and boron carbide shielding mixture used in the present study.

Table IX-C-6

Required Penetration Shield Thicknesses to Attenuate
Scalar Flux in Magnets to Allowable Limit:

$$\phi_a(\text{A1}) = 7.747 \times 10^{11} \text{ n/m}^2 \cdot \text{s}.$$

Region	Zone	$\phi_0 \left(\frac{\text{n}}{\text{m}^2 \cdot \text{s}} \right)$ $x = 0.1 \text{ m}$	$\phi \left(\frac{\text{n}}{\text{m}^2 \cdot \text{s}} \right)$ $y = 0.2 \text{ m}$	$\lambda(\text{m})$ Local Attenuation Mean Free Path	Required Shield Thickness (m)
V. Barrier Solenoid	9	3.292+17	8.390+16	0.0366	1.05
	10	2.781+17	5.523+16	0.0310	0.89
	11	2.581+17	4.760+16	0.0296	0.85
	12	1.014+17	1.227+16	0.0235	0.65
	13	6.408+16	1.196+16	0.0298	0.77
	14	4.624+15	9.186+14	0.0310	0.64
IX. Transition Coil	19	4.549+15	1.392+13	0.0086	0.25

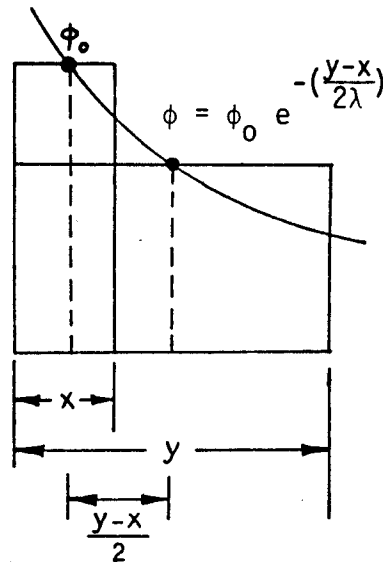


Table IX-C-7

Heating Rates in Different Penetration Regions.
Effective Penetration Shield Thickness: 0.2 m.

Region	Zone	Volume $\text{m}^3 \times 10^{+6}$ *	MeV Per Source Neutron	MW/ m^3	Fractional Standard Deviation
I. First 0.1 m of Central Cell Blanket	1	1.160×10^6	4.431+00	8.132+00	0.033
	2	9.330×10^7	2.395+00	5.465-01	0.037
II. Barrier	3	1.418×10^5	3.836-02	5.759+00	0.075
Solenoid	4	1.607×10^5	2.841-02	3.763+00	0.090
Front Shield	5	1.311×10^5	1.582-02	2.569+00	0.106
	6	9.642×10^4	6.042-03	1.332+00	0.162
III. Barrier	7	9.020×10^5	4.897-03	1.156-01	0.165
Solenoid Back Shield					
IV. Transition	8	1.440×10^6	2.348-03	3.471-02	0.238
Coil Shield					
V. Barrier	9	1.910×10^6	6.581-04	7.335-03	0.440
Solenoid	10	1.910×10^6	3.959-04	4.413-03	0.416
	11	1.910×10^6	2.274-04	2.534-03	0.668
	12	1.910×10^6	8.692-05	9.688-04	0.509
	13	1.910×10^6	7.326-05	8.165-04	0.654
	14	1.910×10^6	3.057-06	3.407-05	0.707
VI. Penetration	15	3.610×10^5	3.385-05	1.996-03	0.437
Plug					
VII. Yin-Yang	16	5.660×10^6	1.224-06	4.604-06	0.404
Shield	17	5.660×10^6	2.752-04	1.035-03	0.974
VIII. End Part	18	1.440×10^6	3.977-06	5.879-05	0.364
Shield					
IX. Transition	19	3.650×10^6	2.319-07	1.353-06	0.852
Coil					

*Volumes pertain to one quadrant of the geometry.

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X. Materials Analysis

X-A. Introduction

The selection of materials that can safely operate in a fusion environment for a long period of time has always been a problem for fusion reactor designers.⁽¹⁻³⁾ The problem is even more complicated than that faced by fission reactor designers because of the wide diversity of thermal and irradiation environments in a fusion reactor plus the fact that a larger variety of materials is required. In a fission reactor, only three main classes of materials need be considered: fuel, core structure, and pressure vessel. In a fusion reactor, first wall and blanket structure, plasma limiters and particle dumps, breeder material, insulators, neutron reflectors, neutron absorbers, magnet and/or final beam focussing devices, and auxiliary equipment such as neutral beam or RF heating devices must be considered.

The largest effort by materials scientists in the last 10 years has been spent on the selection of a suitable first wall structural material. Relatively little effort has been spent on auxiliary materials even though some attempt has been made to include them in full scale reactor designs.⁽⁴⁻¹⁰⁾ The dearth of data for such materials was recently pointed out in the INTOR assessment.⁽¹¹⁾

The material most favored for first wall structures has undergone considerable change over the last 10 years. For example, in the 1960's and early 1970's the refractory alloys of Nb and Mo were given the most attention. However, by the mid-1970's, austenitic stainless steels had emerged as the favorite for most reactor designers because of the large data base associated with fission neutron irradiated steels. Several challenges to that leadership position have come from V⁽¹²⁾, Ti⁽¹³⁾ or Al⁽¹⁴⁾ based alloys because of the poor thermal properties and high, long lived radiological hazards associated with austenitic steels. Nevertheless, the austenitic steel system has withstood the challenges and remained the favorite first wall material, especially in the near term magnetic fusion systems utilizing the tokamak configurations.^(11,15)

As we began our design work in the tandem mirror area we decided to re-examine the choice of austenitic steels and have, as will be shown later, concluded that the ferritic steels would be most appropriate for our conditions in WITAMIR-I. In fact, we found that, with the appropriate choice of breeder material ($\text{Pb}_{83}\text{Li}_{17}$), we can eliminate the need for a separate reflector and

have been able to design an efficient blanket system utilizing only 2 materials (ferritic steel and Pb-Li alloy) to satisfy the functions of first wall, blanket structure, coolant, breeder and reflector.

We have chosen traditional neutron absorber (B_4C) and gamma shield (Pb) materials. The materials in the magnets vary from Al/NbTi in the central cell to Cu/Nb₃Sn in the high field barrier coil. However, due to the separation from the neutron generating zone, the neutron flux in the end plugs (the Nb₃Sn area) is considerably lower and hence less of a problem than in more traditional tokamak designs.

In summary, the materials problems in WITAMIR-I are confined to a smaller number of materials and, like past studies, the major emphasis will be on the first wall structural materials. We feel that the choice of a ferritic steel and Pb-Li alloy will allow a relatively long lifetime ($\sim 10 \text{ MW-y/m}^2$) to be achieved. No other insurmountable problems have appeared outside the structural material and we feel that the materials problems for tandem mirrors are much less difficult than for tokamaks generating the same amount of power.

X-B. Materials Environment

X-B-1. Introduction

Like most magnetic fusion reactor designs, WITAMIR-I presents a wide variety of thermal, irradiation, electrical, chemical, and stress conditions to materials. We have chosen to concentrate on two properties for this report, thermal and irradiation effects, and will make a few comments about chemical compatibility with the Pb-Li alloys.

X-B-2. Thermal Environment

There are 4 regions of general importance for this study:

- 1) the first wall/blanket;
- 2) the shield;
- 3) the magnets;
- 4) the direct convertor plates and grids.

The thermal variation through the central cell blanket region is shown in Figure X-B-1 for steady state operation. After the first few cm the temperature is constant throughout the blanket and can be described as $T(^{\circ}\text{C}) = 330 + 170(\theta/180)$. When the plasma is not present, the first wall/blanket region is kept at a constant 250°C (some 22°C above the melting point of Pb₈₃Li₁₇) by auxiliary heaters and the afterheat in the blanket ($\sim 30 \text{ MW}$).

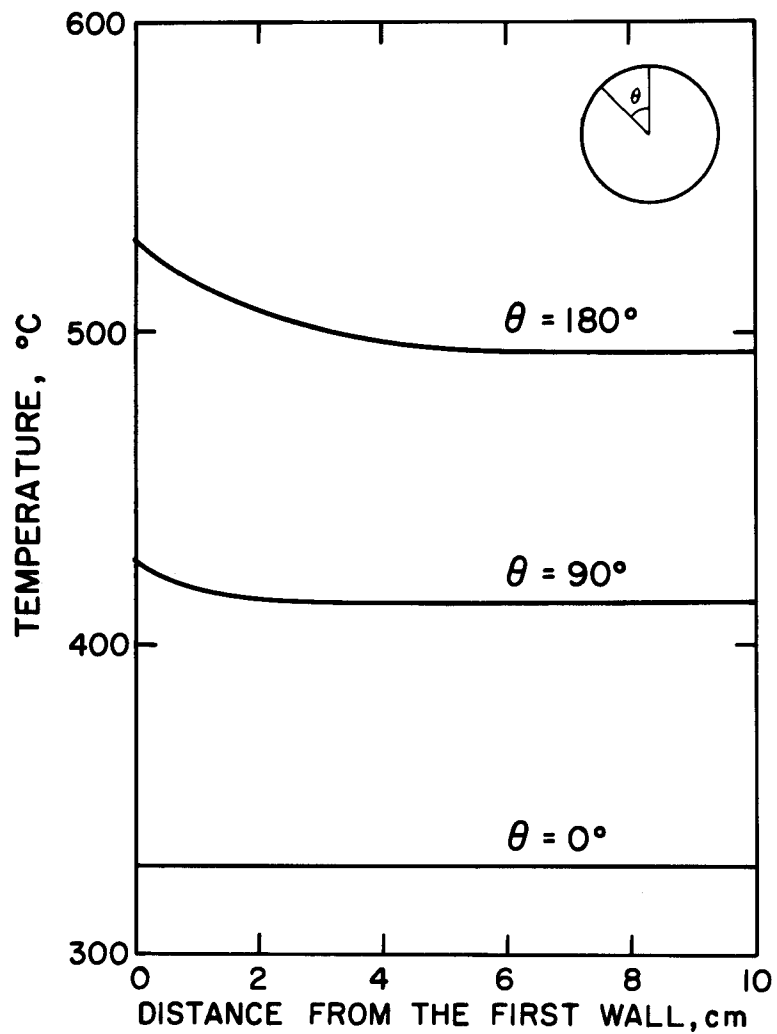


FIGURE X-B-1. TEMPERATURE DISTRIBUTION OF THE FIRST ROW OF TUBES AT DIFFERENT AZIMUTH DIRECTIONS

The shield region is cooled with water and remains between 100°C and 150°C during the entire reactor lifetime.

The temperature variation through a central cell dewar and solenoidal coil ranges from 20°C to 4.2°K. However, the temperature in the Al-2219 structural alloy, the Al stabilizer, and NbTi superconductor is constant at 4.2°K. It is possible that entire coils may be allowed to "warm up" to 20°C periodically in order to remove radiation damage accumulated at low temperatures. The temperatures in the barrier coil are 1.8°K instead of 4.2°K.

Finally, the temperature variations in the water cooled Ta-10W grids and ion collector plates for the direct convertor are 140°C-270°C and 315°C-350°C respectively. The grids are cooled with pressurized water while the collector plates are cooled with boiling water.

X-B-3. Neutron Environment

X-B-3-a. Introduction

Most of the neutronic analyses necessary for radiation damage calculations are given in Chapter IX of this report. We will only summarize and emphasize some of those points in this chapter as they pertain to the performance of the WITAMIR blanket. Most of the emphasis will be on the central cell region and the reader is referred to section IX-C for more details on the neutron flux in the end cell region.

X-B-3-b. Central Cell Blanket Neutron Environment

There are two main parameters of interest here; the rate of displacements and the production of helium in the ferritic steel first wall. Other factors, such as hydrogen or solid transmutation production rates are expected to be of somewhat lesser importance. For reference purposes Figure II-E-3 is repeated in this chapter (Figure X-B-2).

The results of the P_3 - S_8 , cylindrical geometry ANISN calculations reveal that the displacement rates fall from a maximum of 32.4 dpa per year at the first wall to 0.38 dpa per year at the front of the reflector region. These numbers assume that the wall loading is uniform at 2.4 MW/m² and the plant factor (percent of time that the plant is operating in a year) is 80%. The radial variation in displacement rates is shown in Figure X-B-3 (a repeat of Figure IX-A-10). The maximum displacement rate during plasma operation is 1.3×10^{-6} dpa per second, very close to that in a fast test facility such as EBR-II. In contrast to the first wall, the dpa rate at the reflector region is 1.5×10^{-8} dpa/sec, a value which is much lower than that produced in current

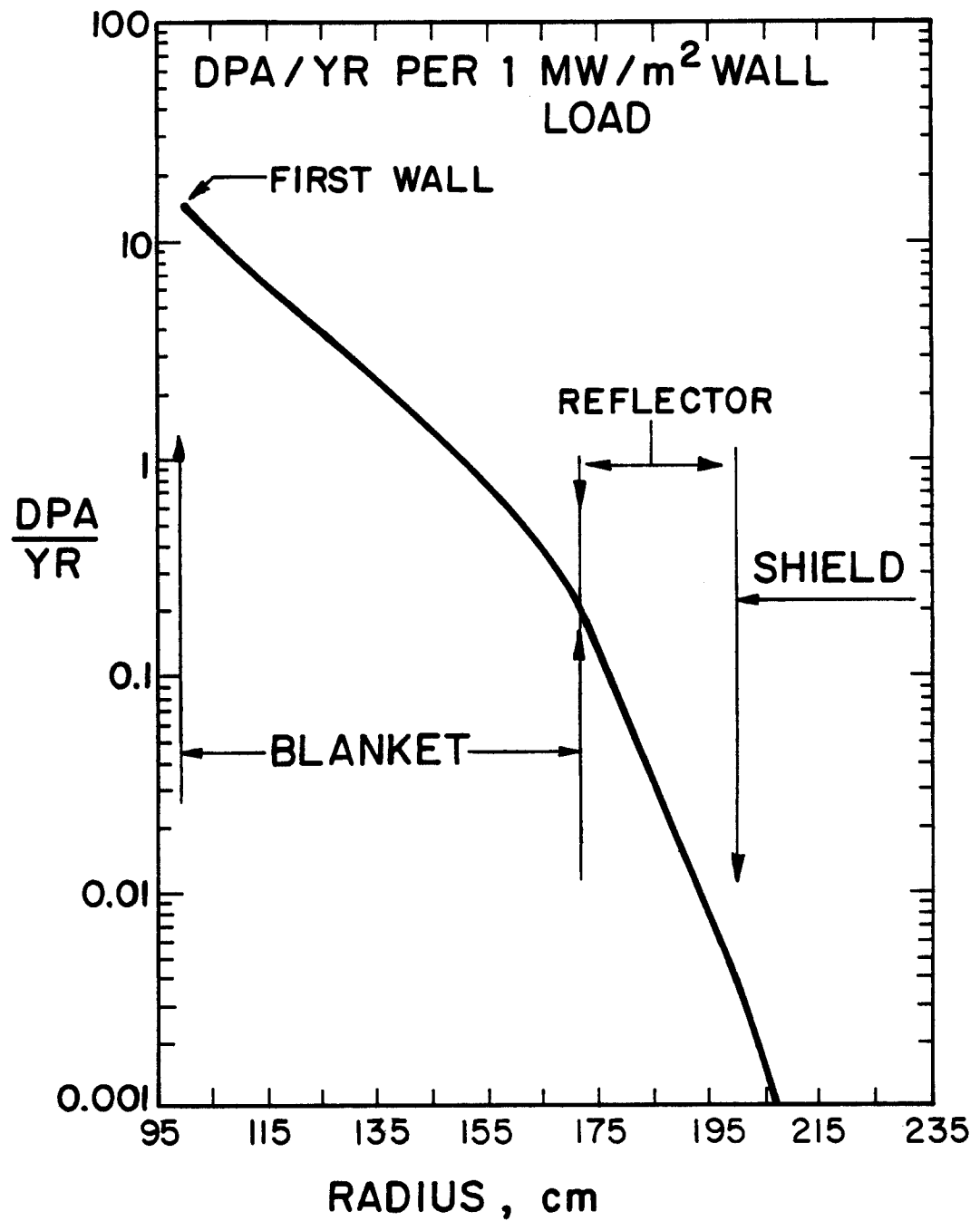


Fig. X-B-3 Dpa in the Blanket Structural Material

irradiation facilities. Care must be taken in comparing the accumulated damage in the reflector region (11.4 dpa over a 30 year period) to a sample irradiated to 11.4 dpa at higher damage rates in fission reactors ($\sim 2.6 \times 10^{22}$ n/cm²) because of recombination phenomena.

The helium production rate in the ferritic steel is 225 appm per year at 80% P.F. and this drops off to ~ 0.04 appm per year at the start of the reflector region (Figure X-B-4). Such helium production rates result in an appm He/dpa ratio of 6.9 at the first wall to 0.1 at the start of the reflector region. These values are to be compared to 0.3 obtainable for ferritic steels in thermal reactors like HFIR and 0.15 obtainable in fast reactors like EBR-II.⁽¹⁶⁾ Again, caution must be exercised when extrapolating experimental results from existing fission test reactors to future operations in a fusion reactor.

Finally, the maximum hydrogen production rates in the ferritic steel are 925 appm per year (80% P.F.).

X-B-3-c. Neutron Environment in the WITAMIR Shield

From Figure X-B-2 we see that the WITAMIR shield region is 60 cm thick and consists of Pb, B₄C, ferritic steel and H₂O. The temperature of the shield is less than 150°C. It can be seen from Figure X-B-3 that the displacement rate in the shield region is very low ($\sim 5 \times 10^{-10}$ dpa per second, maximum) so that even in 30 years, the maximum damage accumulated would be ~ 0.3 dpa which is equivalent to $\sim 7 \times 10^{20}$ n/cm² ($E > 0.1$ MeV) in a fast fission reactor.

Another parameter of interest in the shield is the burnup of the boron-10 in the B₄C absorber. Neutronic calculations show that this number is 2.6×10^{11} atoms per cm⁻³ of B₄C per second. This translates into 61 appm per operating year (2.4 MW/m² at 80% P.F.) or ~ 1800 appm in a 30 year lifetime. The consequences of this burnup will be examined in section X-E.

X-B-3-d. Neutron and Gamma Ray Environment in WITAMIR Magnets

The main concern here is the neutron damage rate in the central cell solenoidal coils. Neutronic calculations of the central cell coil region reveal that the displacement rate in the Al-2219 structural material is $< 2 \times 10^{-6}$ dpa per full power year or $< 6 \times 10^{-5}$ dpa per reactor lifetime. This is equivalent to $< 2 \times 10^{17}$ n/cm² ($E > 0.1$ MeV).

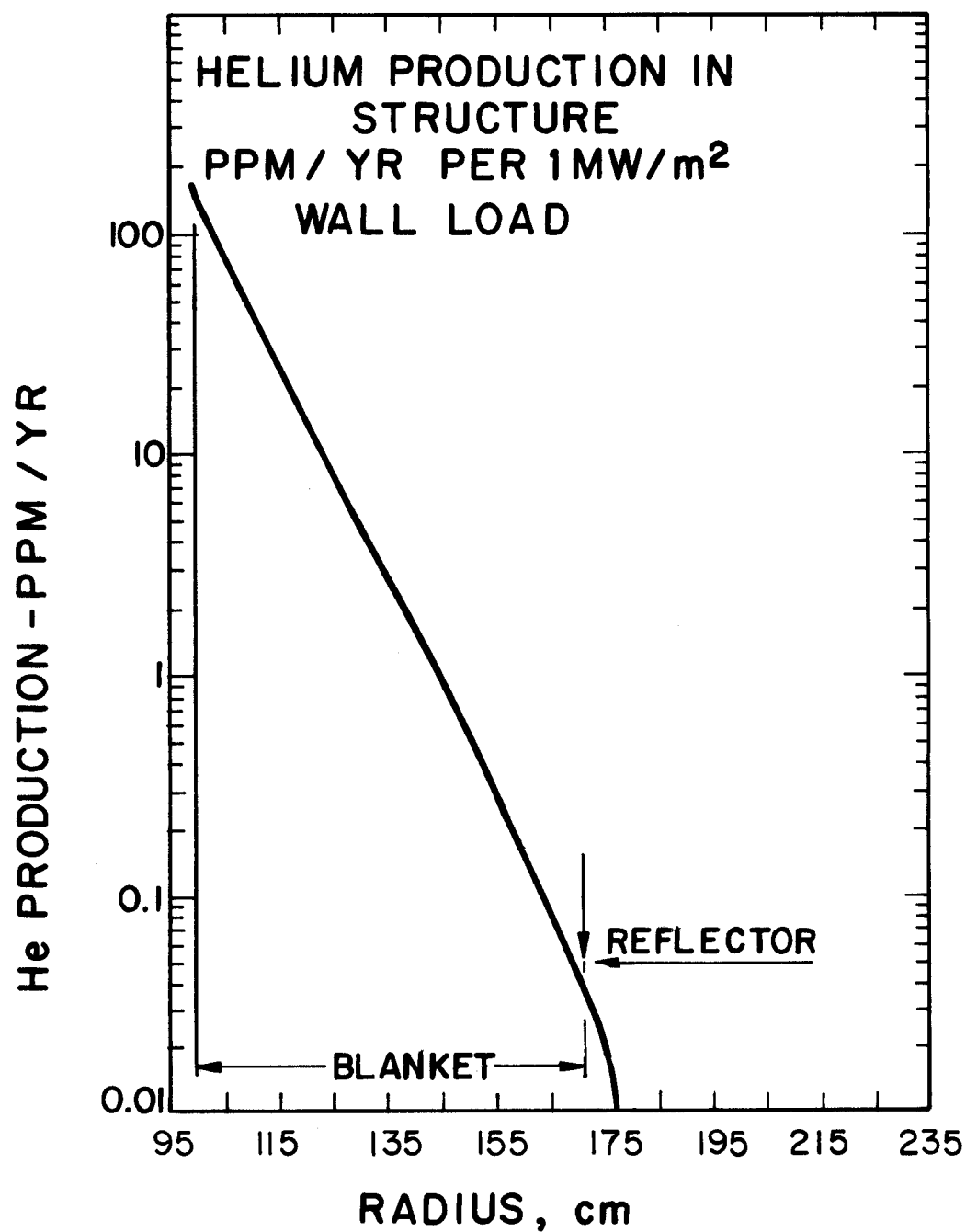


Fig. X-B-4 Helium production in structure.

The damage in the Al stabilizer is a maximum of 5×10^{-7} dpa per year and this drops by 4 orders of magnitude throughout the magnet. A similar number would apply to the NbTi superconductor.

The other critical region for WITAMIR magnets is zone #9 of the barrier coil (Figure X-B-5). The maximum neutron flux to the superconducting region is 7.7×10^7 n/cm²-s which produces a damage rate of 4×10^{-6} dpa per year in the copper stabilizer and Nb₃Sn superconductors. The damage rate in the NbTiTa and NbTi superconductors is considerably less than this value.

Finally, the gamma ray fluxes to the thermal and electrical insulators in the central cell magnets are a maximum of 1.7×10^7 rads per year or 5×10^8 rads in a 30 year period.

X-B-4. Charged Particle Flux

One of the advantages of the tandem mirror configuration is the low charge exchange neutral flux to the first wall. In tokamaks this flux is so high that it produces unacceptably high heat fluxes and erosion rates in the first wall region.⁽¹¹⁾ The major reasons for the difference between the TMR's and the tokamaks in this regard are:

- 1) No neutral beams in the central cell area of WITAMIR (fueling is accomplished by pellets).
- 2) No limiters in the central cell region of WITAMIR as opposed to tokamak designs.
- 3) The collection of charged particles in WITAMIR is done in the direct convertor region, far from the central cell area, where vacuum systems can dramatically reduce the amount of back streaming. This is not always possible in tokamaks, especially with poloidal divertors.

We have not been able to calculate the exact neutral particle flux to the first wall but estimates now predict that number to be less than 10^{12} cm⁻²s⁻¹. Such low fluxes do not significantly change the 2-3 watt/cm² heat flux to the first wall already associated with the x-rays and synchrotron radiation.

The main area where the particle flux can be a problem is to the grids and collector plates of the direct convertor. A summary of the anticipated environment in those areas is given in Table X-B-1. These numbers will be used later to calculate the sputtering and blistering problems that might be incurred by a WITAMIR type device.

Table X-B-1

Summary of the Particle Fluxes to the Grids and
Collector Plates of the Direct Converter

<u>Particles</u>	<u>Average Energy (keV)</u>	<u>Particle Flux cm⁻²s⁻¹</u>
	<u>Grid</u>	
D, T	350	7.9×10^{15}
He	1000, 4000	$1.2 \times 10^{14*}$
Plug Ions (H)	960	2×10^{13}
	<u>Collector Plate</u>	
D, T	35	7.9×10^{15}
He	300, 3300	$1.2 \times 10^{14*}$
Plug Ions (H)	640	2×10^{13}

* Ratio of 4000 keV/1000 keV particles = 2:1

X-C. Structural Material for WITAMIR-I

X-C-1. General Considerations

It was stated in Chapter I that WITAMIR-I is to be a near term design which uses technology available by the mid-1980's to early 1990. With respect to structural materials, this effectively rules out the use of refractory alloys of the V, Nb, and Mo systems because of the lack of experience and design codes for these metals. The use of Ti alloys, while still attractive, suffers from the same lack of fusion related data, particularly with respect to radiation damage. Such considerations left us with two main alloy systems to consider for WITAMIR: austenitic stainless steels and ferritic steels.

There is certainly a wide data base for both alloys with respect to non-nuclear conditions. Areas such as mechanical strength, corrosion resistance, fabrication requirements, and production capabilities are well known and there is a long history of design codes on which to base future projections. The irradiation data base is certainly extensive for the austenitic steels because of their role in the breeder reactor technology and there is a fair amount of relatively low temperature, low neutron fluence data for the ferritic steels. Furthermore, on the basis of irradiation test results from the National Clad/Duct Materials Development Program, the ferritic steels are being actively investigated for service in the fast breeder cores and a large high temperature and high fluence data base will exist by the mid-1980's.

It will be evident from the discussion in the rest of this section why we chose the martensitic (9-12% Cr) steels over the austenitic system, but the main reasons are given below:

- A) Much greater resistance to neutron induced void formation and metal swelling.
- B) A greater resistance to in-reactor creep below 600°C.
- C) Better ductility retained after high temperature fission neutron irradiation.
- D) Better thermal stress resistance, at least by a factor of two.
- E) Better compatibility with the $Pb_{83}Li_{17}$ breeder/coolant material.
- F) Considerably more experience in heat exchanger design and aqueous environments.
- G) Lower material cost per unit weight.
- H) Less demand on Cr (12 vs. 18%) and Ni (0.5 vs. ~ 8%) resources which generally have to be imported into the U.S.

Other factors which weighed heavily in our decision to use ferromagnetic steels over the austenitic alloys came from the analyses performed by scientists at General Atomic^(17,18) on the effects of magnetic steels with respect to both plasma performance and magnetic forces. These studies concluded that for tokamaks, the effects were minimal. Magnetic perturbations should be even less of a factor during the steady state operation of a tandem mirror.

Areas of concern that still need to be addressed before one could clearly support the ferritic steel system over the austenitic system include:

- 1) Effects of high helium contents on the irradiation resistance exhibited by ferritic steels in fission reactors.
- 2) Demonstration that post welding heat treatments will not result in lower radiation damage resistance.

Finally, the specific choice of HT-9 over other ferritic steels should not be taken too seriously. This alloy system has been identified as one particularly attractive to the fast breeder and, as such, gives us a convenient data base from which to extrapolate its performance in a fusion environment. It is quite possible that other ferritic steels might be developed to give even better properties in a fusion environment and future studies ought to be open to new ideas in this area.

The rest of this section is organized as follows. First we will briefly review the physical and mechanical properties of HT-9 that make it attractive. Then we will address what is known about its potential compatibility with Pb-Li alloys and its effect on magnetic fields. This will be followed by a brief summary of the performance of HT-9 in a fission reactor environment. Based on the previous information we will extrapolate the response of HT-9 to the WITAMIR environment and finally, we will estimate the useful lifetime of the first wall/blanket components.

X-C-2. Physical and Thermal Properties of HT-9

The general class of alloys that we considered are the ferromagnetic steels which contain roughly 9 to 13% Cr with some small addition of various strengthening and stabilizing elements such as Mo. These steels are conventionally used in the normalized and tempered condition for high temperature applications. Although the heat treatment can result in either a tempered martensite or bainite structure, depending on the alloy and thermal treatment

parameters, this class of materials is usually referred to as "martensitic" stainless steels for simplicity.

Because of the encouraging and rapidly expanding data base from the U.S. Fast Breeder Reactor Program and the significant operational history in Europe, the alloy Sandvik HT-9 (DIMX20CrMoWV121) was selected for WITAMIR. The normal composition of this alloy is given in Table X-C-1 and Figure X-C-1(19) gives the binary section of the Fe-Cr-C phase diagram at 12%. For high temperatures and extended time service, the alloy is usually heat treated at 1050°C for 0.5 hours in the austenite zone (Figure X-C-2), air cooled and then tempered at 780°C.

The thermal properties of the 9 Cr-1 Mo system, which should be close to HT-9 are given in Table X-C-2.(20)

The electrical conductivity of the ferritic steels is in the range of 0.002 to 0.005 mho depending on the composition and heat treatments. No specific value was found for HT-9.

X-C-3. Use of Martensitic Steels

It is fortunate that there are so many applications for martensitic steels throughout the U.S. and the world. A recent summary by scientists at General Atomic revealed that there are major applications for high temperature martensitic stainless steels in the following areas:(21)

- Fossil Power Plants
 - Superheaters
 - Reheaters
 - Evaporators
 - Steam piping
- Nuclear Power Plants
 - Superheaters, reheaters, U.K. Hinkley Point "B" AGR
 - Steam generator: U.K. PFR
 - He compressors, circulators: U.S. Fort St. Vrauns HTGR
- Other Applications
 - Gas and steam turbine components
 - Aircraft and missile thin wall pressure vessels
 - Petroleum industry pressure vessel linings

It was found that the 9-12 Cr steels were used in foreign fossil power plants under the following conditions:

Table X-C-1
Nominal Composition of HT-9 Alloy

<u>Element</u>	<u>Wt%</u>
Fe	Balance
Cr	11.5
Mo	1.0
Ni	0.5
W	0.5
Mn	0.5
V	0.3
Si	0.25
C	0.20

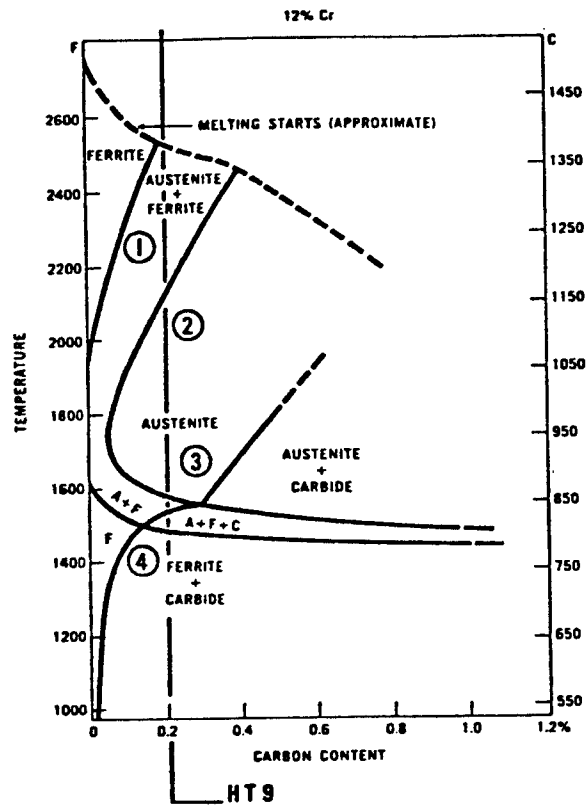


Figure X-C-1.

Reference 19.

TYPICAL HEAT TREATMENT PROFILE FOR A MARTENSITIC STAINLESS STEEL

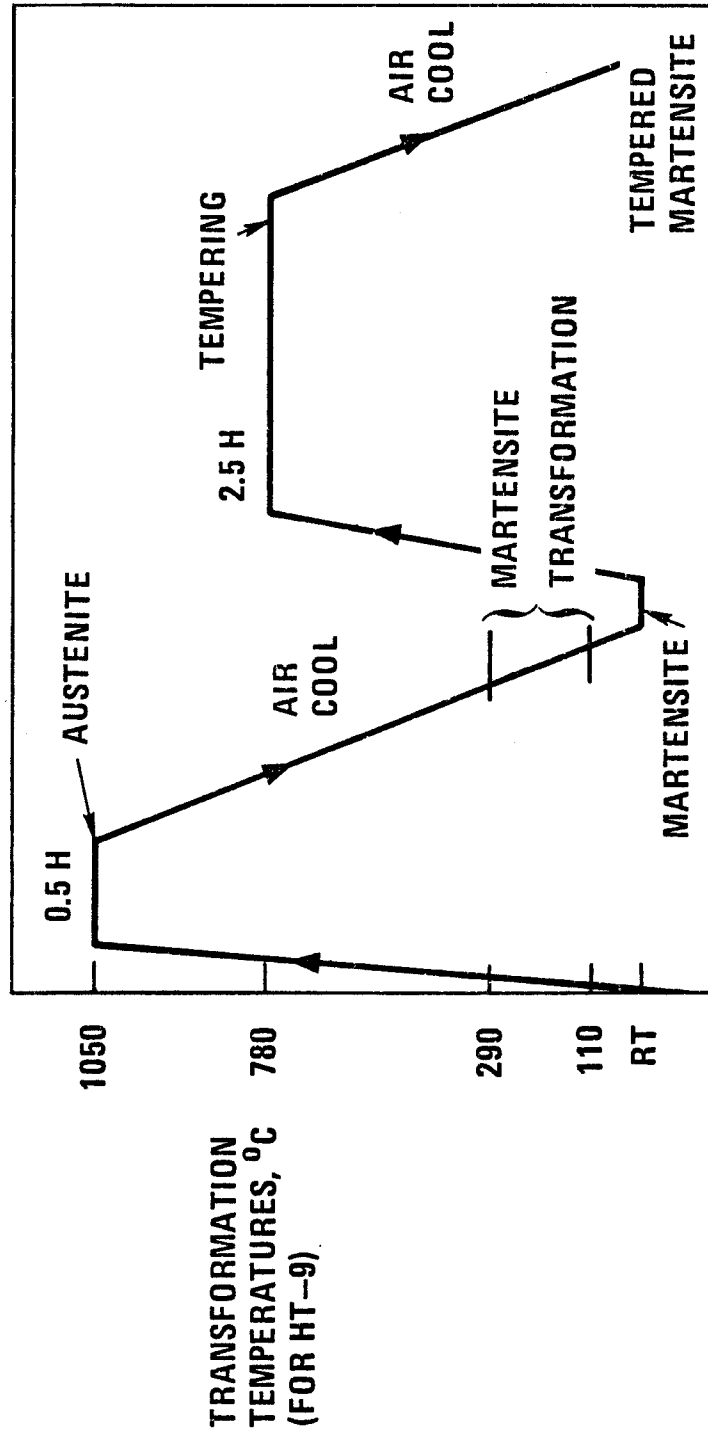


Fig. X-C-2.

Reference 19.

Table X-C-2
Selected Thermal Properties of the 9 Cr-1 Mo Steel⁽²⁰⁾

Temperature		Thermal	Thermal	Specific	Instantaneous	Mean
°F	°C	Conductivity	Diffusivity	Heat	Coefficient of	Coefficient of
		$W\ cm^{-1}\ C^{-1}$	$cm^2\ s^{-1}$	$cal\ g^{-1}\ C^{-1}$	Thermal Linear	Thermal Linear
				$\times 10^3$	Expansion	Expansion
					$10^{-6}\ C^{-1}$	$10^{-6}\ C^{-1}$
70	21	.222	.066	.105	3.23	
100	38	.227	.067	.106	3.28	3.25
150	66	.235	.067	.109	3.35	3.29
200	93	.242	.067	.112	3.42	3.34
250	121	.249	.068	.114	3.49	3.39
300	149	.254	.068	.117	3.56	3.42
350	177	.260	.067	.120	3.63	3.46
400	204	.263	.067	.123	3.69	3.49
450	232	.267	.066	.126	3.75	3.52
500	260	.270	.065	.129	3.82	3.56
550	288	.273	.064	.133	3.87	3.59
600	316	.275	.063	.136	3.93	3.62
650	343	.277	.062	.140	3.98	3.65
700	371	.277	.061	.144	4.04	3.68
750	399	.279	.059	.147	4.09	3.70
800	427	.279	.057	.152	4.13	3.73
850	454	.280	.055	.157	4.18	3.76
900	482	.279	.053	.163	4.23	3.79
950	510	.279	.051	.169	4.27	3.81
1000	538	.279	.050	.175	4.31	3.83
1050	566	.277	.047	.183	4.35	3.86
1100	593	.277	.045	.191	4.39	3.89
1150	621	.275	.043	.201	4.43	3.92
1200	649	.273	.040	.213	4.46	3.93
1250	677	.272	.037	.229	4.49	3.96
1300	704	.270	.034	.250	4.52	3.98
1350	732	.268	.030	.276	4.55	4.00
1375	746		.028	.295*		
1400	760	.263	.038	.216	4.57	4.02
1450	788	.260	.043	.190	4.60	4.04
1500	816	.260	.046	.178	4.62	4.06

* Extrapolated value

<u>Location</u>	<u>Usage</u>	<u>Alloy</u>	<u>Temp °C</u>	<u>Service Status</u>
U. K.	13 plants	9 CrMo(V)	450-660	< 110,000 hrs.
	12 plants	12 CrMo	430-850	< 45,000 hrs.
Austria				
Belgium				
Czechoslovakia	} 96 Tons	9 CrMo	< 610	< 110,000 hrs.
Germany				
Switzerland				
Turkey				
Yugoslavia	> 250 Tons	12 CrMo(V,W)	< 610	< 130,000 hrs.

In addition to past and current usage, several future areas of use are being considered.

<u>System</u>	<u>Application</u>	<u>Alloys</u>
US-LMFBR	Ducts (cladding)	HT-9
	Steam generator	MOD 9 Cr-1 Mo
US-GCFR	Ducts, cladding, steam generator, shielding	HT-9/MOD 9 Cr-1 Mo
French Super Phenix	Ducts	9-17 Cr
	Steam generator	9 Cr-1 Mo
Japanese FBR	Steam generator	9 Cr-2 Mo
US-HTGR	Steam generator & pumping, He-He heat exchangers	HT-9/MOD 9 Cr-1 Mo
	He circulators	
US-LWR	Steam generator	HT-9/MOD 9 Cr-1 Mo

It is from this extensive experience that one can often obtain a great deal of mechanical property data which is summarized in the next section.

X-C-4. Mechanical Properties of HT-9

The tensile strength of HT-9 is compared to that for several other candidate CTR materials in Figure X-C-3. This figure shows that HT-9 is superior to annealed 316 SS up to ~ 600°C and it compares favorably to Ti-6Al-4V in

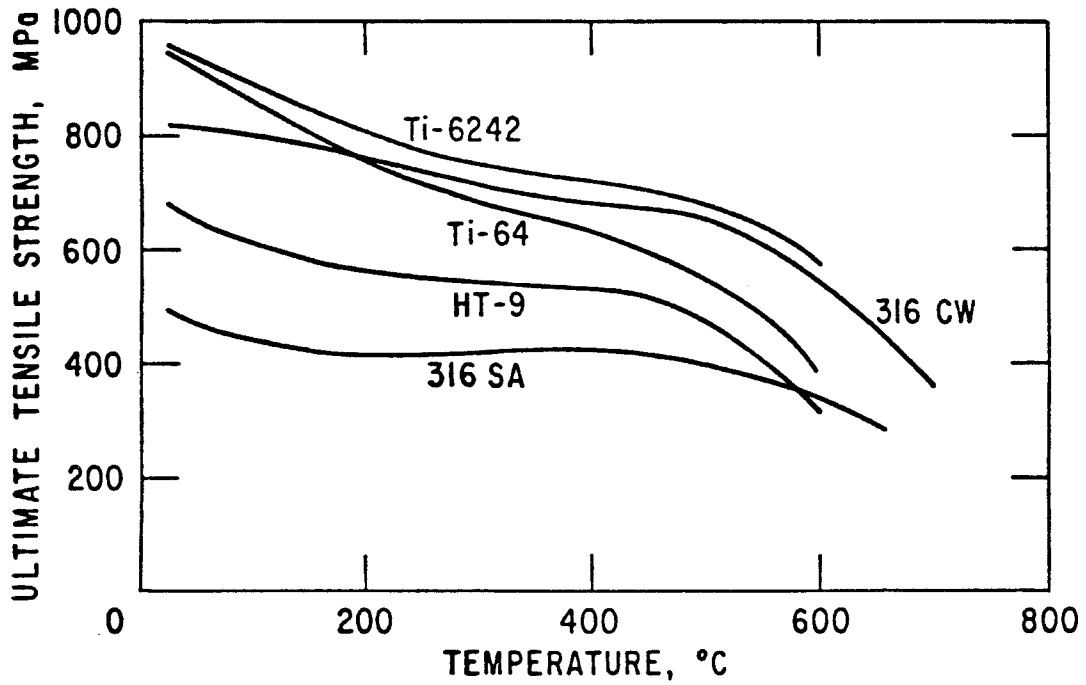


Fig. X-C-3a. Ultimate tensile strength versus temperature for candidate CTR alloys. (22)

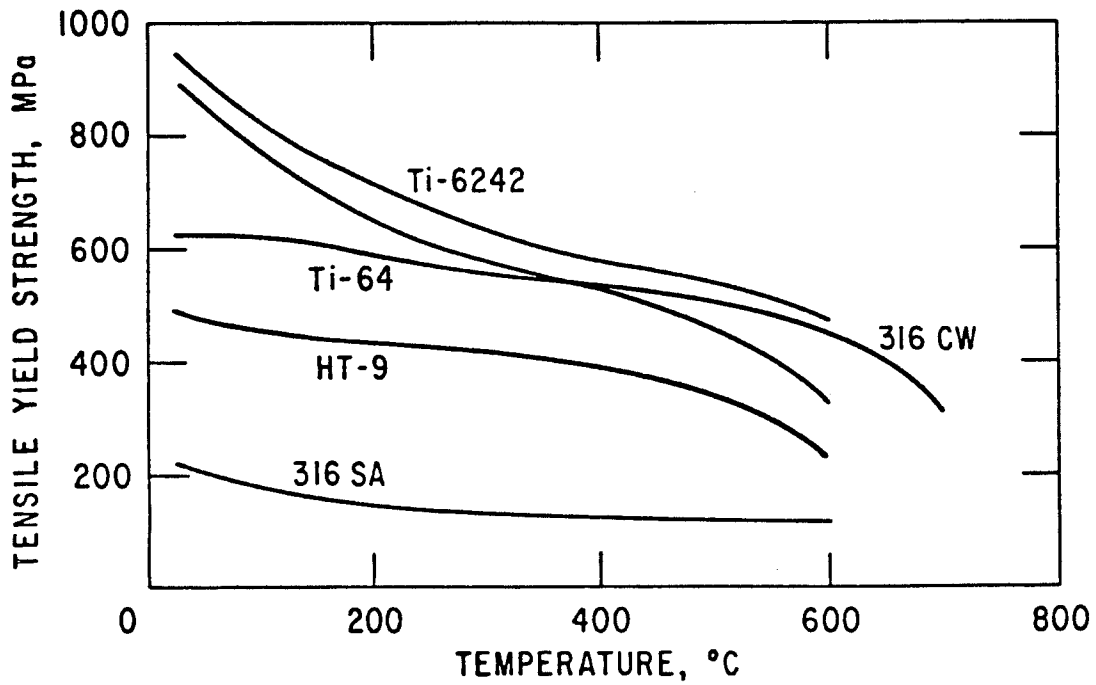


Fig. X-C-3b. Tensile yield strength versus temperature for candidate CTR alloys. (22)

the 400-600°C range. However, above 500°C the yield strength begins to drop, and it is best to limit its operating range to 500-550°C.

The effect of long term exposure at 600°C on the tensile properties of HT-9 is shown in Figure X-C-4.⁽²¹⁾ It can be seen that exposure to 600°C temperatures for 80,000 hours (~ 10 years) reveals that there is very little degradation of ultimate or 0.2% yield strength. The percentage elongation also stays between a comfortable 10 and 30% even after long term exposure. A recent study⁽²³⁾ also reports that HT-9 specimens subjected to 1 atm of hydrogen gas during testing at 200-400°C had no effect on ductility.

The ductility of HT-9 is compared to other CTR alloys in Figure X-C-5⁽²²⁾ and reveals that HT-9 retains a factor of 2 better ductility in the 400°C-600°C range compared to 316 SS.

Long term stress rupture data for HT-9 and other alloys at 500°C is shown in Figure X-C-6. This information reveals that for up to 10 years of exposure, HT-9 can withstand stresses up to ~ 200 MPa.

The effect of 80,000 hour-600°C service exposure on HT-9 stress-rupture properties is shown in Figure X-C-7. There is a slight degradation of the properties but it appears that stresses of 20 ksi (~ 130 MPa) or more are quite reasonable for 100,000 hour exposures.

Even though WITAMIR is a steady state device, there will be numerous startups and shutdowns that will impose large stresses on the structure. Therefore, information on crack growth rates is important. Such information is given in Figure X-C-8 for helium environments and reveals that even under the most severe conditions the crack growth rates of unirradiated HT-9 are less than 10^{-4} mm per cycle at 530°C operating temperatures. If the HT-9 is tested in air, a much more aggressive environment, the crack growth rates are increased, even at 425°C (Figure X-C-9). However, the growth rates are still roughly 4 times lower than found in 20% CW 316 SS or 9 Cr-1 Mo alloys.

X-C-5. Thermal Stress Factor

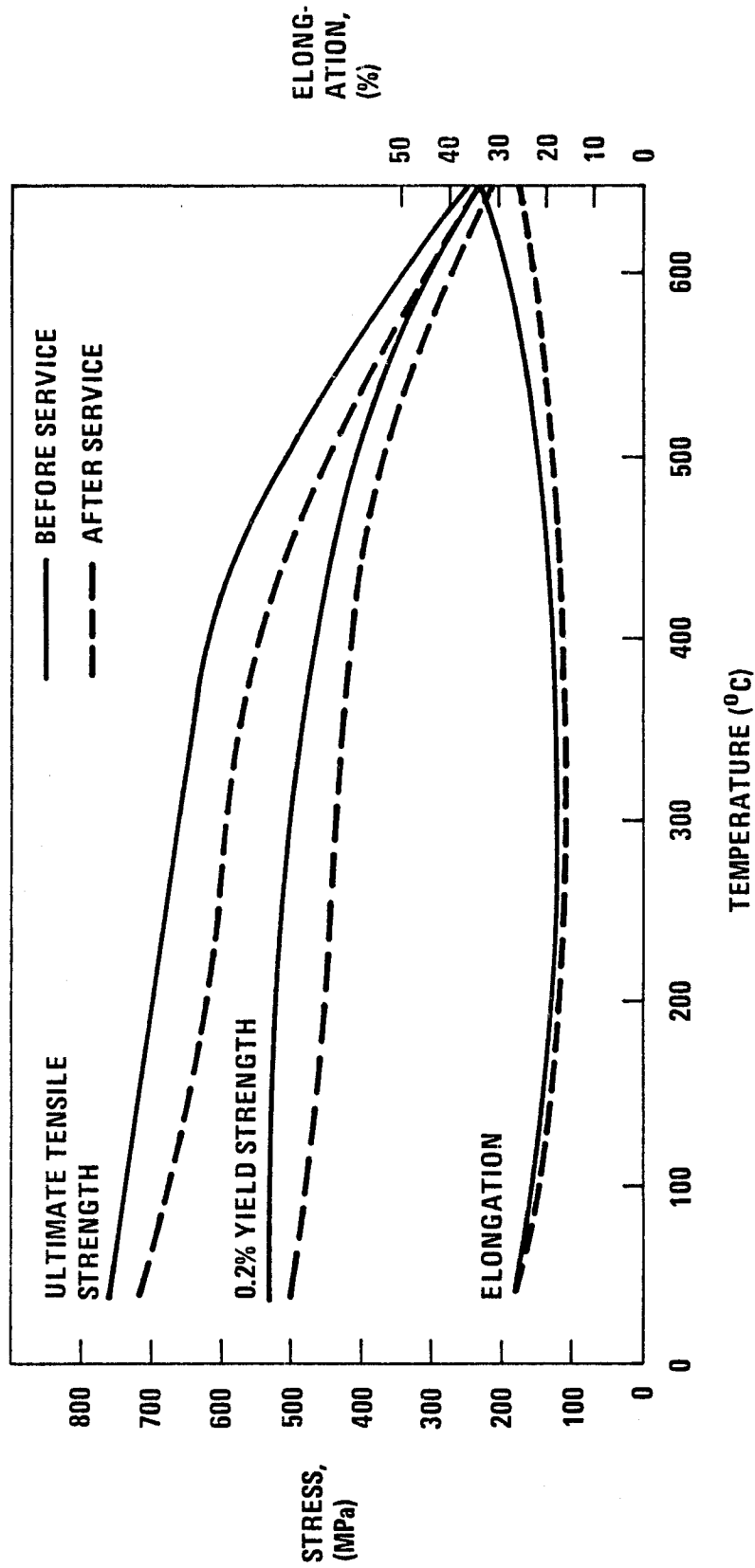
When rigidly held plates are subjected to high heat fluxes, the resulting differential thermal expansion generates large stresses and in extreme circumstances can even cause failure. This can be a particularly difficult problem for tokamaks.

A typical figure of merit which has been used to indicate the resistance of metals to failure is the ratio of the yield stress to the thermally induced

Fig. X-C-4.

Reference 21.

EFFECT OF 80,000 HOUR, 600°C SERVICE EXPOSURE ON HT-9 TENSILE PROPERTIES



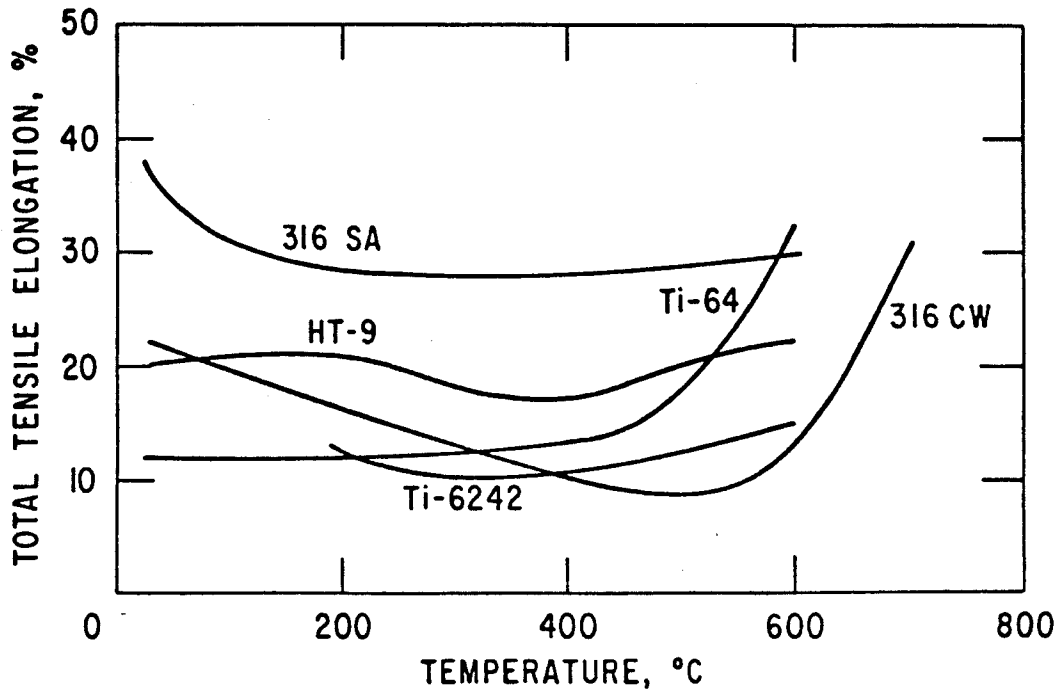


Fig. X-C-5. Total tensile elongation versus test temperature for candidate CTR alloys. (22)

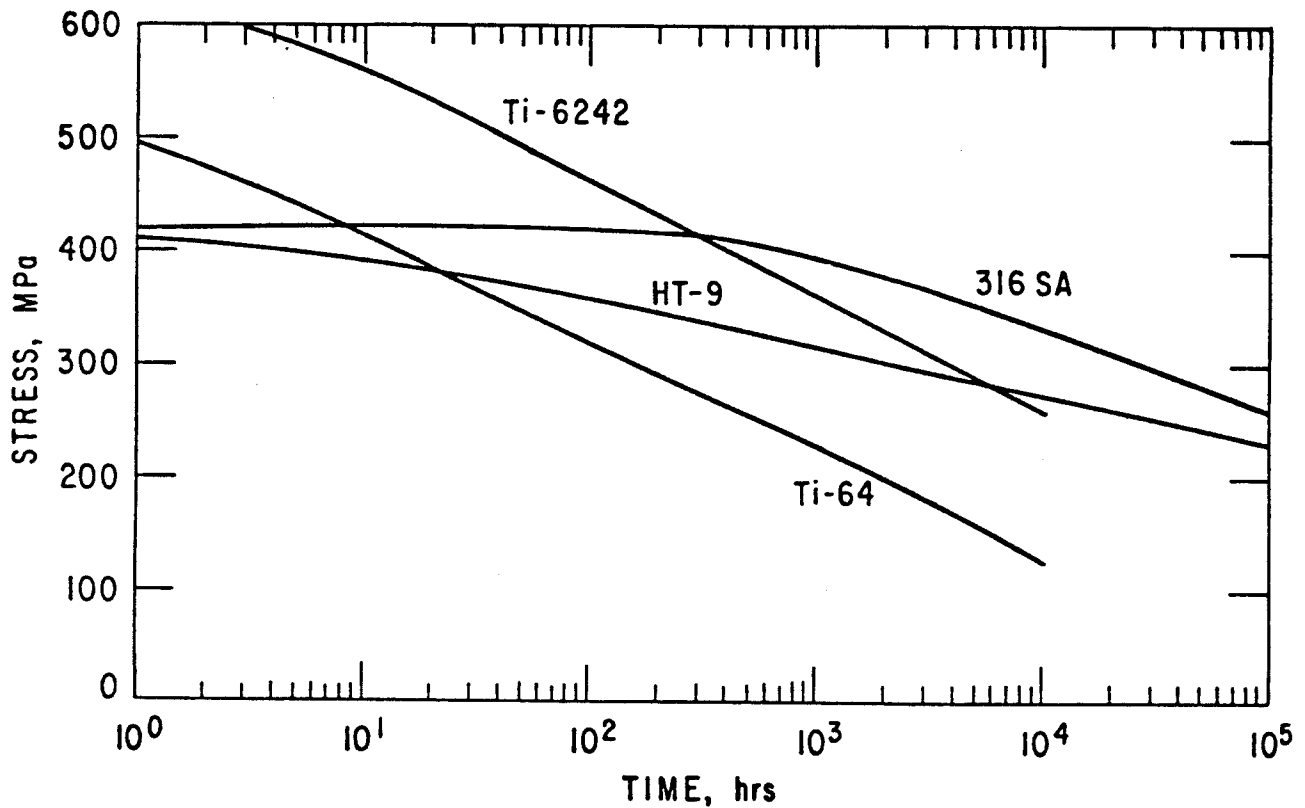
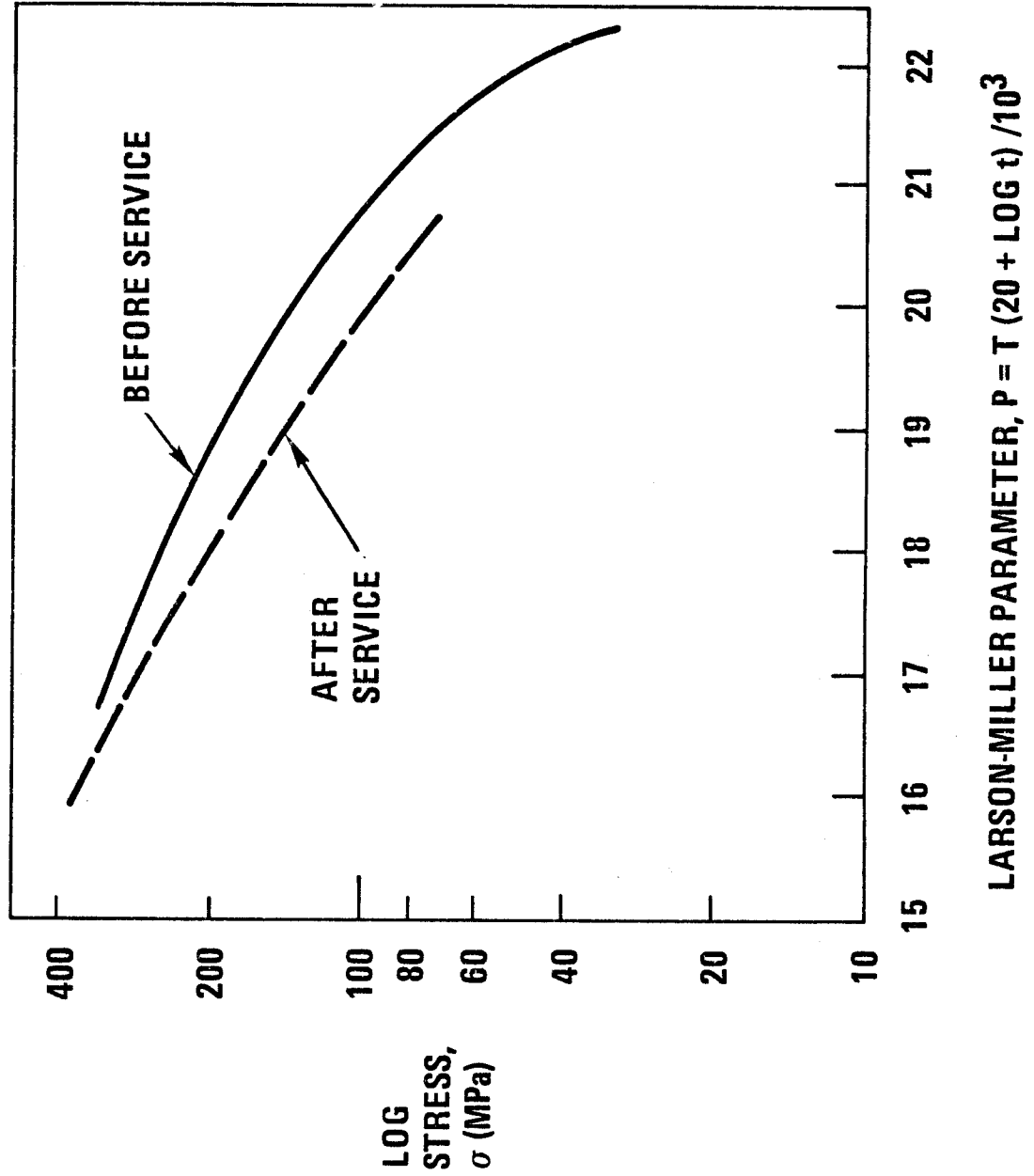


Fig. X-C-6. Stress rupture data at 500°C for candidate CTR alloys. (22)

Fig. X-C-7
Reference 21.

**EFFECT OF 80,000 HOUR, 600°C SERVICE EXPOSURE
ON HT-9 STRESS-RUPTURE PROPERTIES**



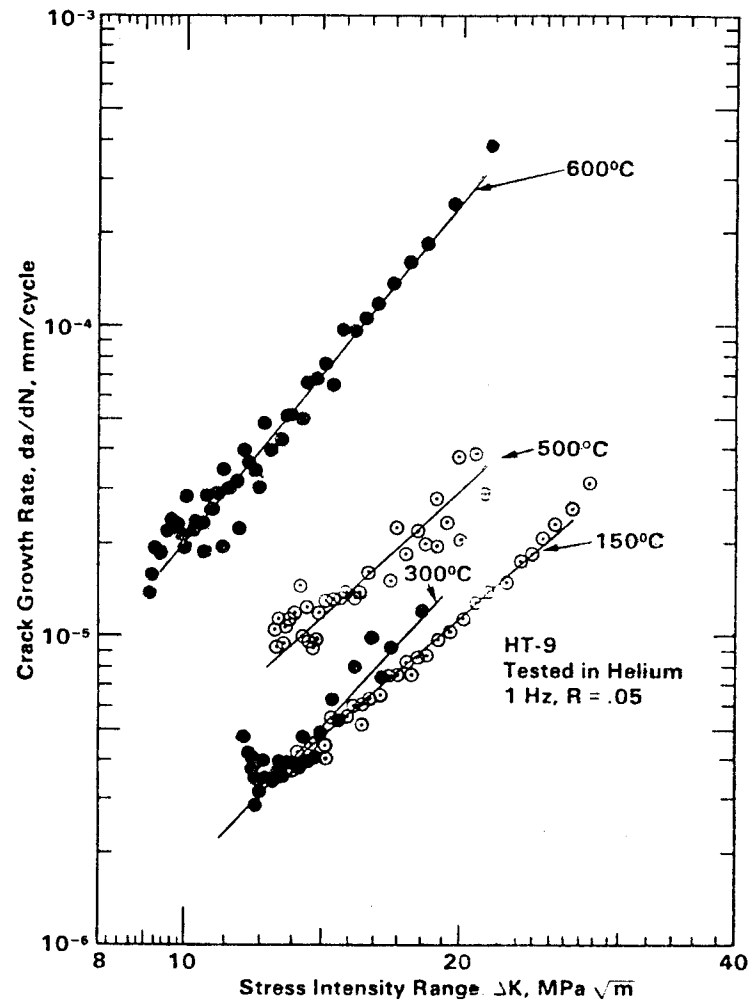


Fig. X-C-8. Comparison of Crack Growth Between 150 and 600°C.

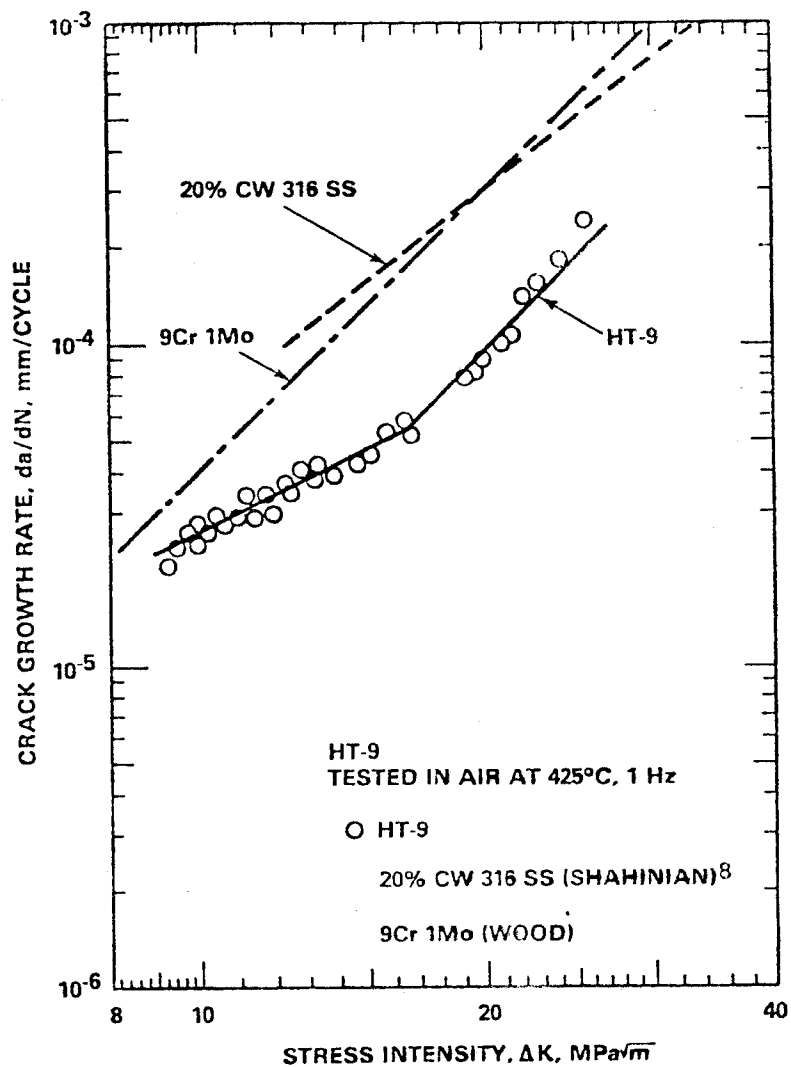


Fig. X-C-9. HT-9 crack growth compared with 9 Cr-1 Mo and 20% CW 316 SS. Reference -- D. A. Mervyn et al., p. 132 in DOE/ER-0045/2, March 31, 1980.

stress. If we assume equal thickness and nuclear heat generation rates, then the figure of merit, M (a high value is desired), is:

$$M = \frac{2\sigma_y K(1 - \nu)}{\alpha E}$$

where: σ_y is the yield strength;
 K is the thermal conductivity;
 ν is Poisson's ratio;
 α is the thermal expansion coefficient;
 E is Young's modulus.

Typical values of M have been calculated for 316 SS, HT-9, Ti alloys, Inconel 718, and V-20 Ti, at 500°C. These values are given in Table X-C-3.

It can be seen that the HT-9 alloy has a figure of merit of at least twice that of cold worked 316 SS and slightly less than Ti and Mo alloys at 500°C. HT-9 is clearly inferior to refractory metals but, for reasons previously explained, we will not consider those systems in WITAMIR.

X-C-6. Compatibility of HT-9 with Pb-Li Alloys

As might be expected, there is no data concerning the $Pb_{83}Li_{17}$ eutectic alloy on HT-9 at temperatures of 350-550°C. There is a small amount of information on low alloy, high strength steels in pure lead,^(24-26,28,30) and some data on 2-1/4 Cr-1 Mo alloys with a $Li_{99}Pb_1$ system at 500°C.⁽²⁷⁾ The only information on liquid metals and HT-9 is a recent study at ANL in pure lithium and lithium contaminated with nitrogen at 482°C.⁽²⁹⁾

Despite claims in earlier studies that low alloy steels were not embrittled by pure lead, recent results showed embrittlement could occur near the melting point of the lead (327°C).^(28,30) The embrittlement of the high strength 4100 series steels (see composition in Table X-C-4) started at ~ 200°C and reached a maximum (lowest ductility) at ~ 320°C. Above ~ 370°C there was degradation in the ductility (see Figure X-C-10). The addition of Sn increased the width of the embrittling zone.

At higher temperatures, 700°C, 2-1/4 Cr-1 Mo alloy suffered a severe attack after 300 hours of exposure to lead.⁽²⁶⁾ However, the addition of Ti to the Pb completely eliminated the corrosion under the same conditions. It has been known for a long time that the addition of Ti or Zr to Pb would inhibit corrosion by forming tightly adhering TiC and ZrC films on the steel surface.

Table X-C-3
Thermal Stress Resistance of
Potential Fusion Materials at 500°C

<u>Alloy</u>	<u>Thermal Stress Resistance</u> <u>(W/m) x 10⁻³</u>
Annealed 316 SS	1.3
20% CW 316 SS	4.9
HT-9	8.5
Ti-6Al-4V	10.2
Ti-6Al-2Sn-4Zr-2Mo	10.4
Inconel-718	10.7
V-20 Ti	12.2

Table X-C-4
Comparison of Some Alloy Steels Which Have Been Tested
in Pb, Li or Pb-Li Alloys

<u>Alloy</u>	<u>Element - wt% (Bal. Fe)</u>				
	<u>Cr</u>	<u>Mn</u>	<u>Mo</u>	<u>C</u>	<u>Si</u>
4140	0.96	0.87	0.18	0.41	0.23
4145	1.00	0.88	---	0.47	0.33
Croloy	2.25	0.5	1.0	0.12	---
HT-9	11.5	0.5	1.0	0.2	0.25

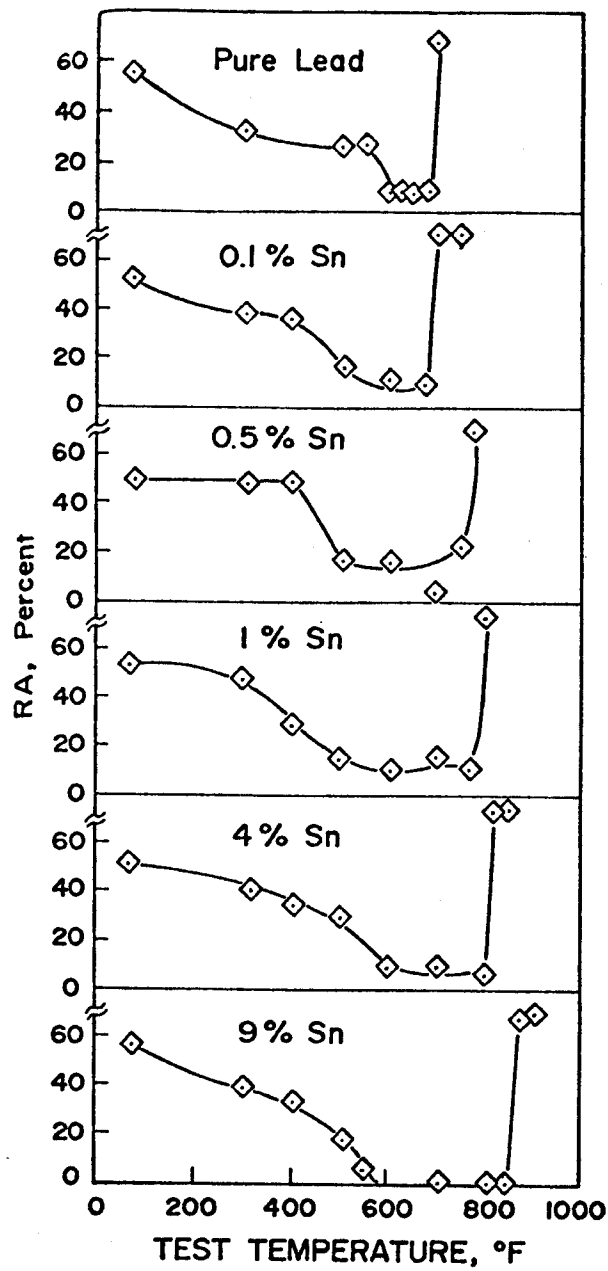


Fig. X-C-10. Reduction of area, RA, for 200-ksi (1379-MPa) 4145 steel, surface wetted with various Pb-Sn alloys as a function of test temperature. (30)

On the other end of the spectrum, a $\text{Li}_{99}\text{Pb}_1$ alloy was shown to attack Fe_3C and Mo_2C in the welded zones of a 2-1/4 Cr-1 Mo alloy at 500°C after exposure up to 1600 hours. Figure X-C-11, from the paper by Anderson et al.⁽²⁸⁾, shows that the cementite and Mo_2C are unstable with respect to Li below 500°C while the chromium carbides are stable over a wide range.

Work at Harwell⁽³¹⁾ has shown that low alloy steels can be significantly decarburized by exposure to Li even at ~ 200°C. However, higher Cr containing steels such as 2-1/2 Cr-1 Mo, or the 9 Cr-1 Mo alloys showed much greater resistance.⁽³²⁾ Presumably, the higher Cr content of HT-9 would help to protect that alloy from decarburization and hence embrittlement.

A recent study by Chopra and Smith⁽²⁹⁾ has shown that nitrogen can have a large effect on the fatigue life of HT-9 in flowing Li at 482°C. They found that in short term fatigue tests, where the nitrogen level was between 80 and 130 ppm, the fatigue life in Li was the same as that obtained in Na. However, when the nitrogen level reached > 1000 ppm of nitrogen, there was a dramatic drop in the fatigue life (Figure X-C-12) by as much as a factor of 5. This strongly suggests that close control of the nitrogen level in the WITAMIR coolant will be necessary.

In summary, it appears that pure lead can have an embrittling effect on low chromium containing steels between 200 and 320°C but operation above that temperature and below 700°C should be acceptable. Ti or Zr inhibitors can be added to the Pb-Li alloy to reduce the corrosion. The higher chromium content of the HT-9 is also likely to help the weld zones to resist attack by the lithium.

X-C-7. Magnetic Properties of Ferritic Steels in a Fusion Environment

X-C-7-a. Introduction

The idea of using ferromagnetic structural materials in magnetically confined fusion reactors is a relatively new one.⁽¹⁸⁾ Before 1979, it was commonly assumed that either the forces on the structure would be too large, or the ferromagnetic material would induce unacceptable field ripples and also cause very long "soak through" times. These ideas were effectively corrected by scientists at General Atomic who calculated the effects of using HT-9 in tokamaks.⁽¹⁷⁾ They concluded that ferritic steels could be used with very little harmful effects to the plasma.

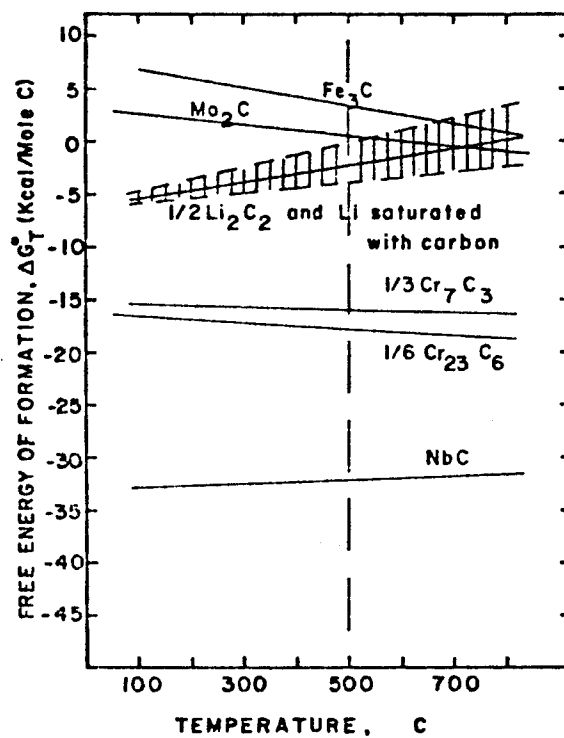


Fig. X-C-11. Relative thermodynamic stability of several carbides in carbon saturated lithium. (28)

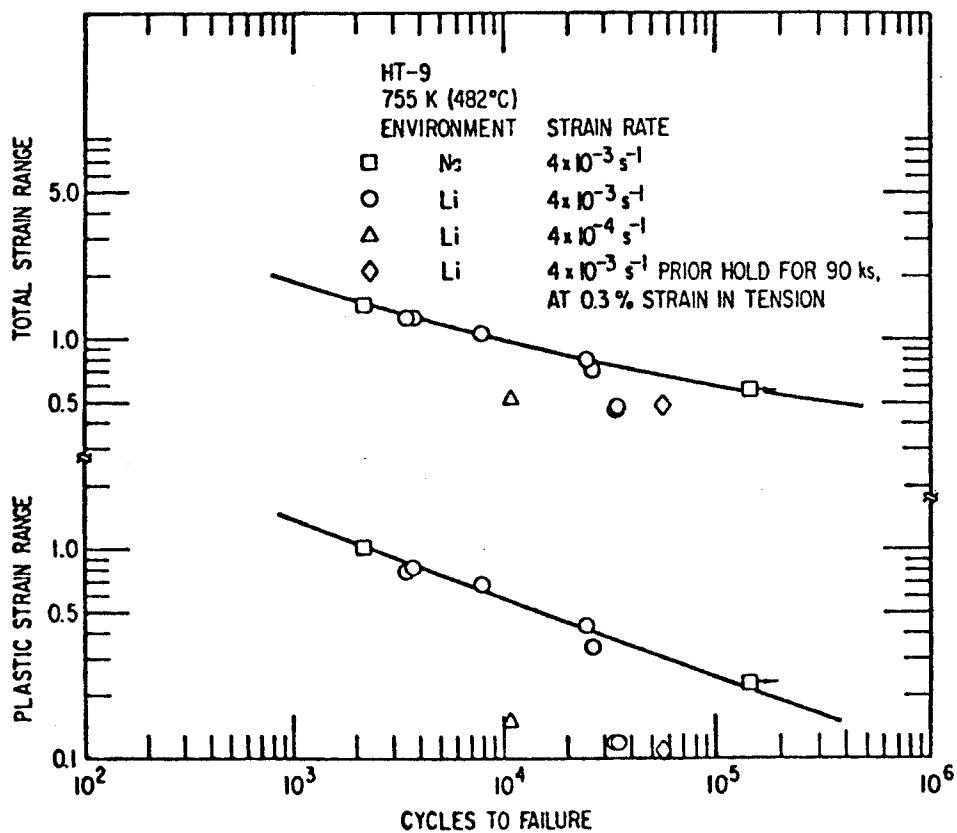


Fig. X-C-12. Total and plastic strain range vs. cycles to failure for HT-9 alloy tested in lithium at 755 K. (29)

X-C-7-b. Magnetic Properties of HT-9

Two of the most common questions that are asked when considering ferritic steels for magnetic fusion devices are "What does the hysteresis loop look like?" and "What is the saturation field?". Figure X-C-13 shows that for 9 or 12% Cr steels, B_{sat} is approximately 1 Tesla and the area of the hysteresis loop is reduced with increasing chromium content.⁽²¹⁾

The effect of operating temperature on the magnetic properties of HT-9 is shown in Figure X-C-14.⁽²¹⁾ It is found that the area of the hysteresis loop is reduced as the operating temperature goes up and the value of B_{sat} is reduced from ~ 1.4 T at 25°C to ~ 1.0 T at 500°C. Since the magnetic field at the blanket from the central cell coils is roughly 4 Tesla, it is clear that the blanket material will be saturated during the operation of WITAMIR. Another way of stating the above is that the permeability as defined,

$$B = \mu H$$

is very close to one under WITAMIR conditions.

X-C-7-c. Consequences of Using Magnetic Material in Fusion Reactors

Given the behavior of the HT-9 alloy with respect to magnetic properties, we ask ourselves the following questions:

- 1) What effect will the magnetic field have on the ferritic structure?
- 2) What effect will the ferritic structure have on the magnetic field?

We can get a clue as to the magnitude of the forces induced in the magnetic structure from calculations performed by Harkness et al.⁽³⁴⁾ They found that the magneto-pressures on a 3 cm thick ferritic steel first wall structure were relatively low for magnetic fields typical of large tokamak devices (Figure X-C-15). The stresses were largest at the outer edge where the field gradients were steepest and the levels were only a few psi. Since the pressures are roughly linear with increasing thickness of the ferritic region, stresses of less than 3 atm are anticipated. Figure X-C-16 substantiates this prediction and shows the maximum magnetic pressure in ETF, STARFIRE and TNS reactor designs when realistic steel packing fractions are considered.

The transient stresses during startup or shutdown should be much less in the tandem mirror design than in the tokamak because one can take a much longer time to start up ($\sim$ minutes) and there are essentially no disruptions to be dealt with in WITAMIR-I.

COMPARISON OF MAGNETIC PROPERTIES OF TWO CANDIDATE MAGNETIC MATERIALS

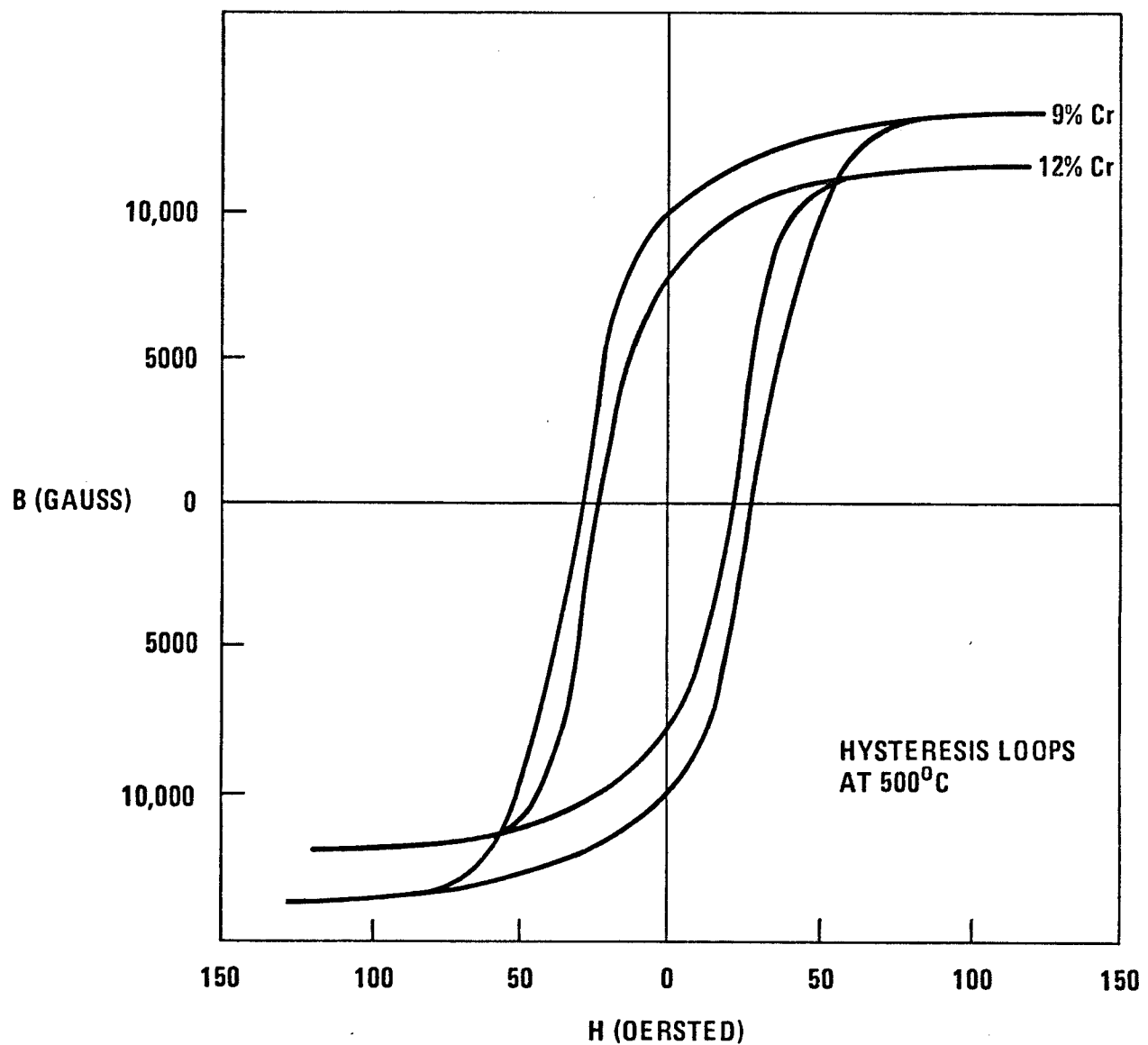


Fig. X-C-13.

Reference 21.

MAGNETIC MATERIALS IN MAGNETIC FUSION DEVICES ARE IN SATURATION

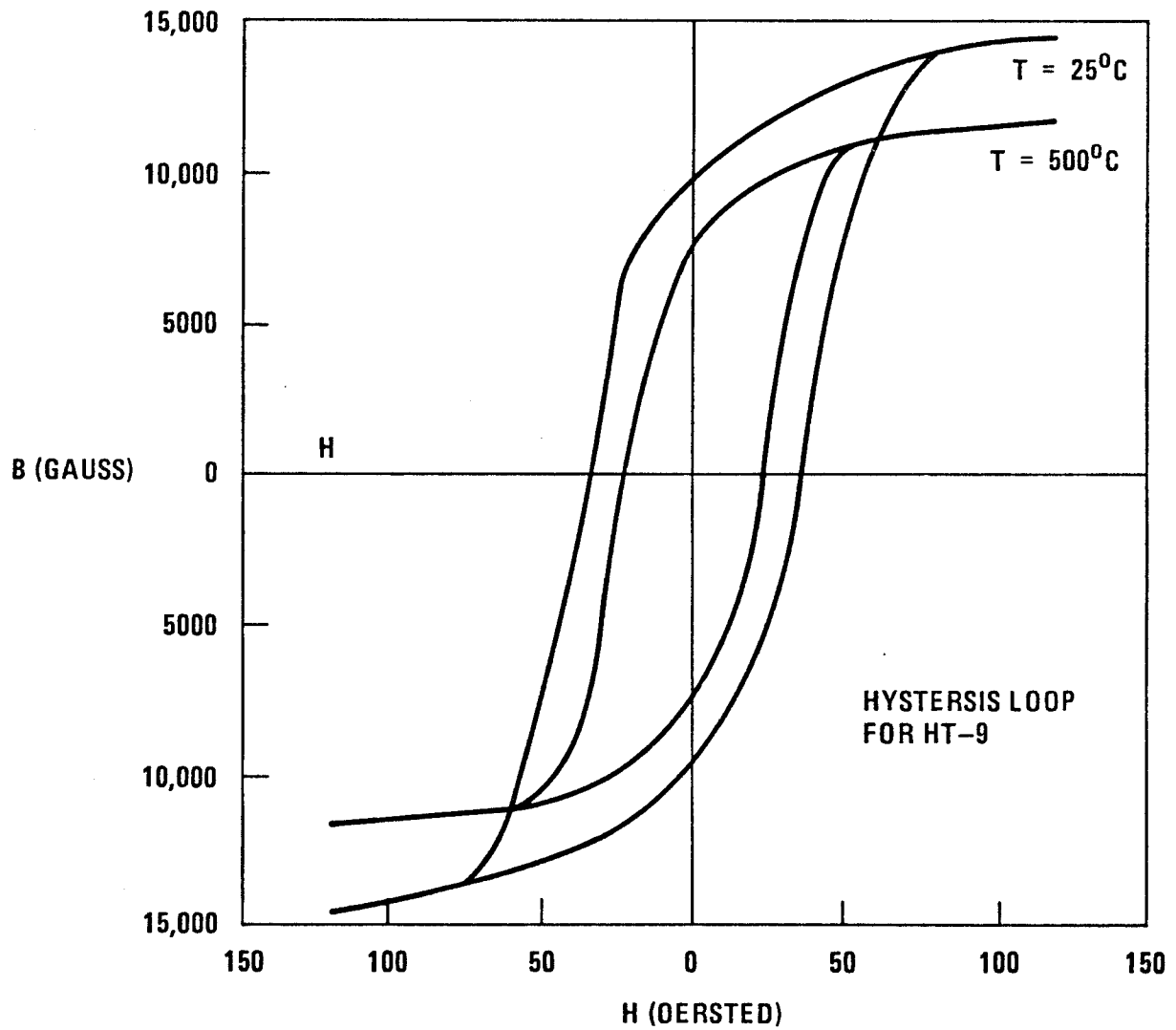


Fig. X-C-14.

Reference 21.

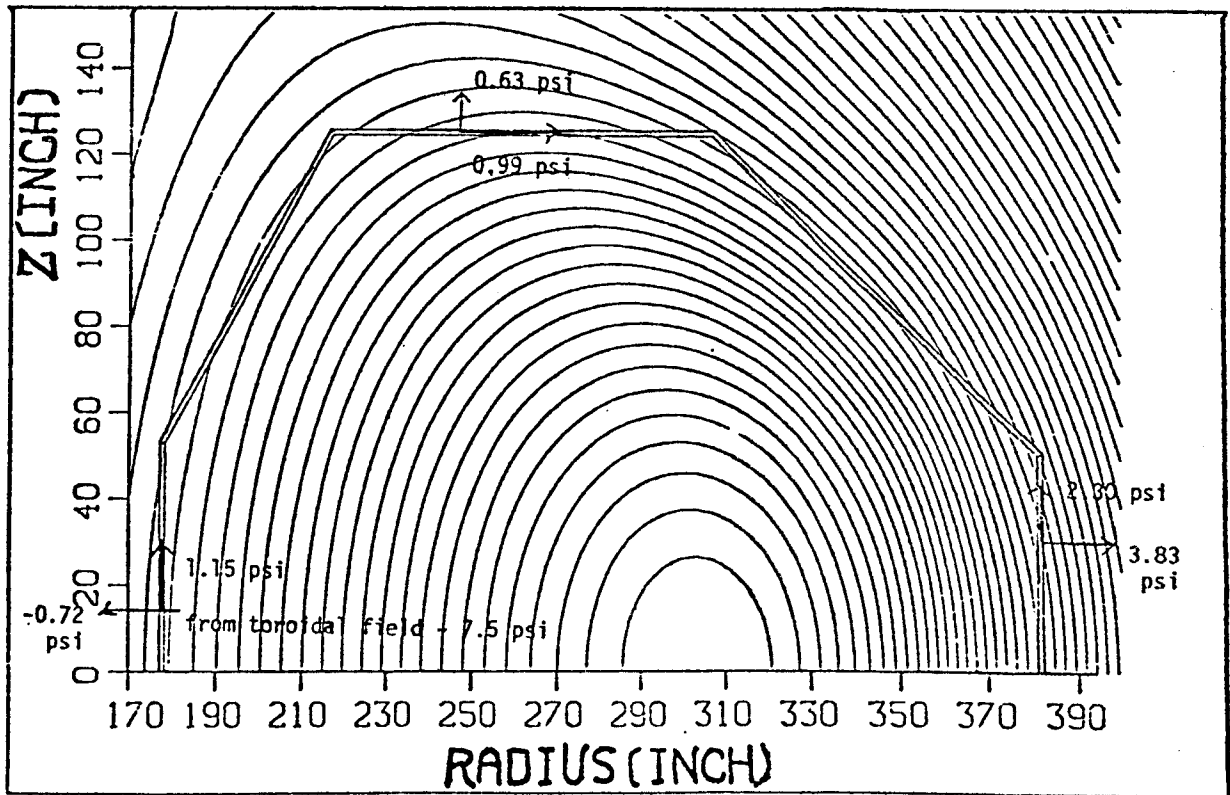


Fig. X-C-15. Forces on 3 cm ferritic stainless steel can from EF & TF coils. ⁽³⁴⁾

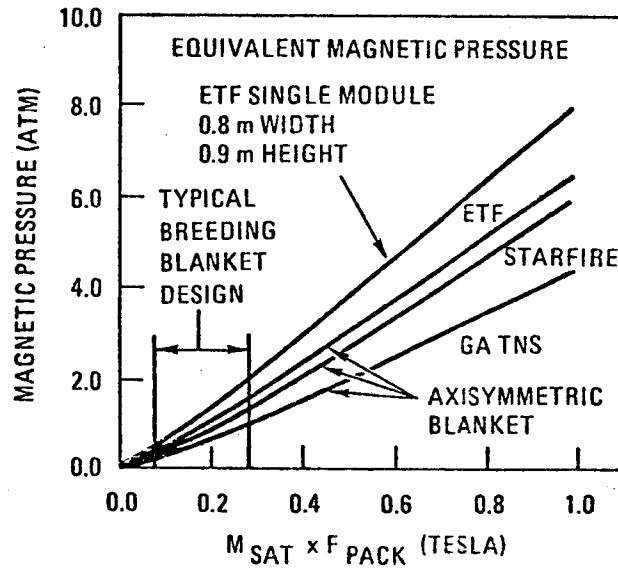


Fig. X-C-16. Magnetic loading on the ferromagnetic blankets for the GA TNS, STARFIRE and ETF. (18)

The main question with respect to magnetic field perturbations has to do with the change in magnetic field ripple when ferritic steels are included in the blanket. Recent calculations using the IRON⁽³⁶⁾ code developed at General Atomic and applied to a tokamak geometry, reveal that the change in the toroidal field ripple is very small even when penetrations are included. Figure X-C-17 shows that field ripples can be held to less than 0.3% at the outside edge of the plasma in STARFIRE.⁽³⁵⁾ It was also shown that poloidal field perturbations in STARFIRE were in the 1 to 3% range which should certainly be manageable.

In summary, the magneto-static loads in the WITAMIR-I blanket should be only a few atmospheres and changes in the field ripple between central cell coils should be quite modest. The WITAMIR blanket should be saturated at ~ 1 to 1.5 Tesla and thereafter act as if it has a permeability of 1 to 1.2. Further analysis is required to obtain more exact stress values for WITAMIR, especially in the regions where the coolant pipes extend beyond the central cell solenoidal coils. Startup forces may be considerable in this area and modified maintenance schemes may be needed to handle the magnetic fields there.

X-C-8. Effects of Irradiation on HT-9 Type Alloys

X-C-8-a. Introduction

It was previously pointed out there is a considerable data base for low temperature, relatively low fluence, irradiation of ferritic steels. More recently, because of interest in the applications of ferritic steels in the core structures of LMFBR's, some information about the high temperature behavior of ferritic steels irradiated to moderately high fission neutron fluences has begun to appear.⁽³⁷⁾ Unfortunately, the data is not as extensive as that for the austenitic system but a great deal of information should be available in the next year or two. It must also be pointed out that there is no data on HT-9 (or any other ferritic steel) irradiated with D-T neutrons. Therefore, all of the results quoted here from fission neutron, heavy ion, and electron irradiation will have to be extrapolated to typical WITAMIR conditions.

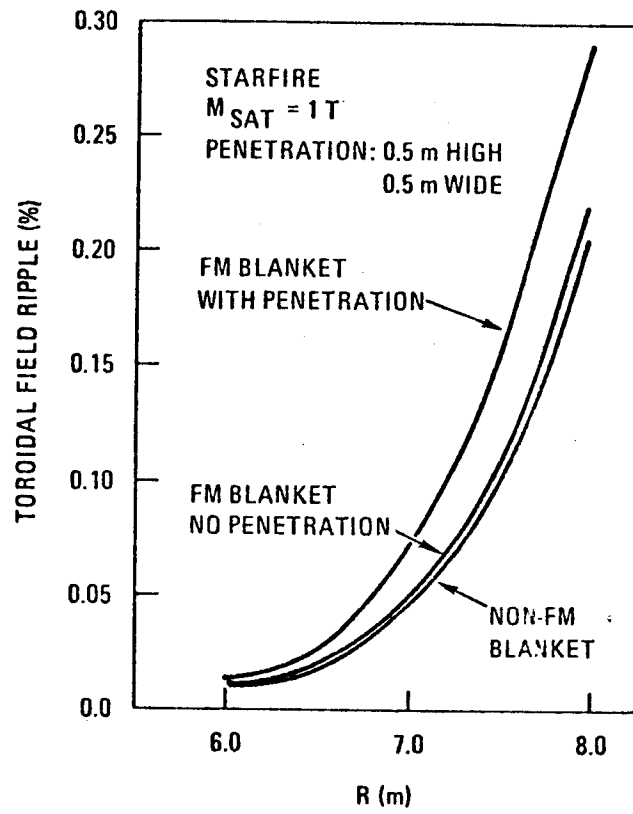


Fig. X-C-17. STARFIRE toroidal field ripple showing the effects of a ferromagnetic blanket with and without penetrations. (17)

X-C-8-b. Survey of Irradiated Ferritic Steel Information Pertinent
to WITAMIR

There are four sources of data which may be used for our study.

- 1) Fast neutron data from EBR-II.
- 2) Thermal neutron data from test reactors and commercial power plants.
- 3) Heavy ion data.
- 4) High voltage electron microscopy data.

It is not the purpose of this section to present a complete literature survey, but only to highlight those papers which might have a direct bearing on the performance of HT-9 in WITAMIR-I. A summary of the work of interest to this study is given in Table X-C-5.

There are four areas in which measurements have been made on irradiated ferritic steel:

- A) swelling due to voids;
- B) tensile properties;
- C) in reactor creep;
- D) shift in ductile to brittle transition temperature (DBTT).

X-C-8-c. Void Swelling in Ferritic Steel

The void swelling problem, which has plagued the austenitic steels in the LMFBR program and has presented some serious problems for the design of first walls in fusion reactors, seems to be greatly reduced in the ferritic steel system. Smidt et al.⁽⁴⁶⁾ showed in 1976 that the ferritic steels (including HT-9) were very resistant to the production of voids during heavy ion bombardment. In fact, he found that the maximum swelling rate was ~ 0.01 - 0.02% per dpa compared to values at least 10 times higher for the 316 stainless steel systems. One MeV electron irradiation experiments also revealed that ferritic steels were quite resistant to the formation of voids, even after predoping the specimens with 10 appm of He.⁽⁴⁷⁾

Neutron irradiation of Fe-10 Cr and FV-448 (11% Cr, 1 Mn, 0.6 Mo, 0.65 Ni) alloys showed very little swelling up to 30 dpa in the temperature range of 375°C - 460°C .⁽⁴³⁾ Figures X-C-18 and X-C-19 show that the addition of up to 5% Cr reduces the swelling compared to pure iron. Further additions of Cr have less effect but still reduce the swelling to $\sim 0.02\%$ per dpa level. Other fission neutron irradiations of FV-448 to ~ 30 dpa in the 380°C - 615°C temperature range show that $< 0.1\%$ swelling was observed. Similarly

Table X-C-5
Summary of Pertinent Irradiation Data for HT-9 in WITAMIR-I

<u>Type of Irradiation (Reference)</u>	<u>Material</u>	<u>T_{irr} °C</u>	<u>Fluence x 10²² n/cm²</u>	<u>Type of Test</u>	<u>Comments</u>
fast n ⁽³⁷⁾	HT-9	419	1.1	DBTT, σ_y	100°C shift
fast n ⁽³⁸⁾	405 SS	400-405	2.3	ΔV , σ_y , duct.	---
fast n ⁽³⁹⁾	HT-9	540-565	4.0	ϵ_{irr}	---
fast n ⁽⁴⁰⁾	FV-448	380, 420, 460, 615	(30 dpa)	ΔV	---
fast n ⁽⁴¹⁾	HT-9	605 625-635	2 4	ϵ_{irr}	---
fast n ⁽⁴²⁾	2-1/4 Cr 1 Mo	371-427	0.29	FCP, DBTT	No effect of irradiation
fast n ⁽⁴³⁾	15% Cr-Fe FV-448	380-615	30	ΔV	No ΔV change
fast n ⁽⁴⁴⁾	HT-9	440-600	1.1		---
fast n ⁽⁴⁵⁾	HT-9	550	7.5	duct., ΔV	---
Heavy Ion ⁽⁴⁶⁾	HT-9, EM-12	450-650	(250 dpa)	ΔV	No He
1 MeV Elec. ⁽⁴⁷⁾	S4	395-550	(20 dpa)	ΔV	10 ppm He

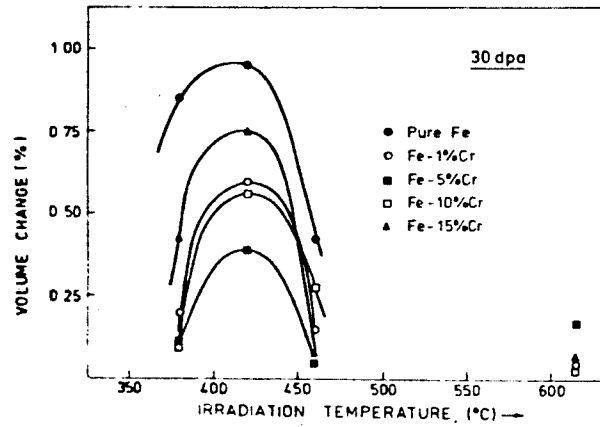


Fig. X-C-18. Swelling as a function of temperature for binary iron-chromium alloys compared with pure iron. (43)

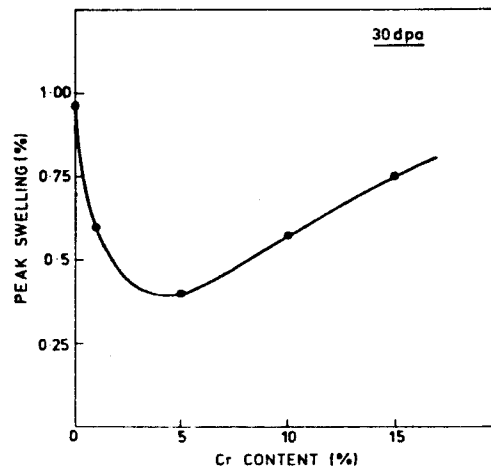


Fig. X-C-19. Magnitude of peak swelling as a function of chromium content for binary iron-chromium alloys. (43)

irradiation of 405 SS (15 Cr, 0.3 Ni, 0.6 C, 0.15 Al) to 400°C with 2.3×10^{22} n/cm² ($E > 0.1$ MeV) revealed no swelling at all.⁽³⁸⁾

Irradiation of HT-9 to 1.1×10^{23} n/cm² (48 dpa) over the range of temperature from 400°C-600°C reveals that the swelling rate is less than 0.01% per dpa compared to a value of 0.35% per dpa for 316 SS (Figure X-C-20).⁽⁴⁵⁾ Such studies do not simulate the helium production in HT-9 correctly, but HT-9 doped with 1% Ni is now being inserted into thermal fission reactors to obtain the necessary higher helium concentrations.

X-C-8-d. Effects of Neutron Irradiation on the Tensile Properties of Ferritic Steel

There are three results reported in the literature that are pertinent to the use of HT-9 in WITAMIR-I. An early study by Garr et al.⁽³⁸⁾ showed that 405 SS, which was given a 1 hour anneal at 980°C before irradiation, suffered relatively little loss in ductility after being irradiated to 2.3×10^{22} n/cm² (10 dpa) at 400°C-425°C (see Table X-C-6). The total elongation remained at 11.2% at 500°C while the uniform elongation dropped from 9.4% to 4.0%.

In another study⁽⁴⁵⁾ where HT-9 was irradiated to 1.1×10^{23} n/cm² (48 dpa) at 550°C the total elongation remained above 10% when tested at 550°C and 670°C.

Finally, Smidt et al.⁽³⁷⁾ found that irradiation to ~ 5 dpa at 419°C resulted in very little reduction of ductility during post-irradiation room temperature tests. They found that after such an irradiation, there was still 6.3% uniform elongation and 13.9% total elongation remaining.

In summary, it appears that irradiation in the 420°C-550°C temperature range to damage levels of 5 to 48 dpa did not seriously affect the ductility and it is quite possible HT-9 may retain adequate ductility up to the 100 dpa level. Experiments with higher helium content are required to see if the ductility is seriously degraded at the levels characteristic of WITAMIR-I (~ 225 appm He/year).

X-C-8-e. In Reactor Creep of HT-9

There are basically two sources of information in this area, both from Paxton et al.^(39,44). They found that the in reactor creep of HT-9 is superior to CW 316 SS, especially in the 550°C to 650°C range (much higher than we propose to use HT-9). Figure X-C-21 shows that at stress levels of 70 MPa (~ 10 ksi) the total creep at 550°C after 2×10^{23} n/cm² (~ 90 dpa) is ~ 2%. This is reduced to ~ 1% at 450°C. Paxton et al.^(39,44) show that at hoop

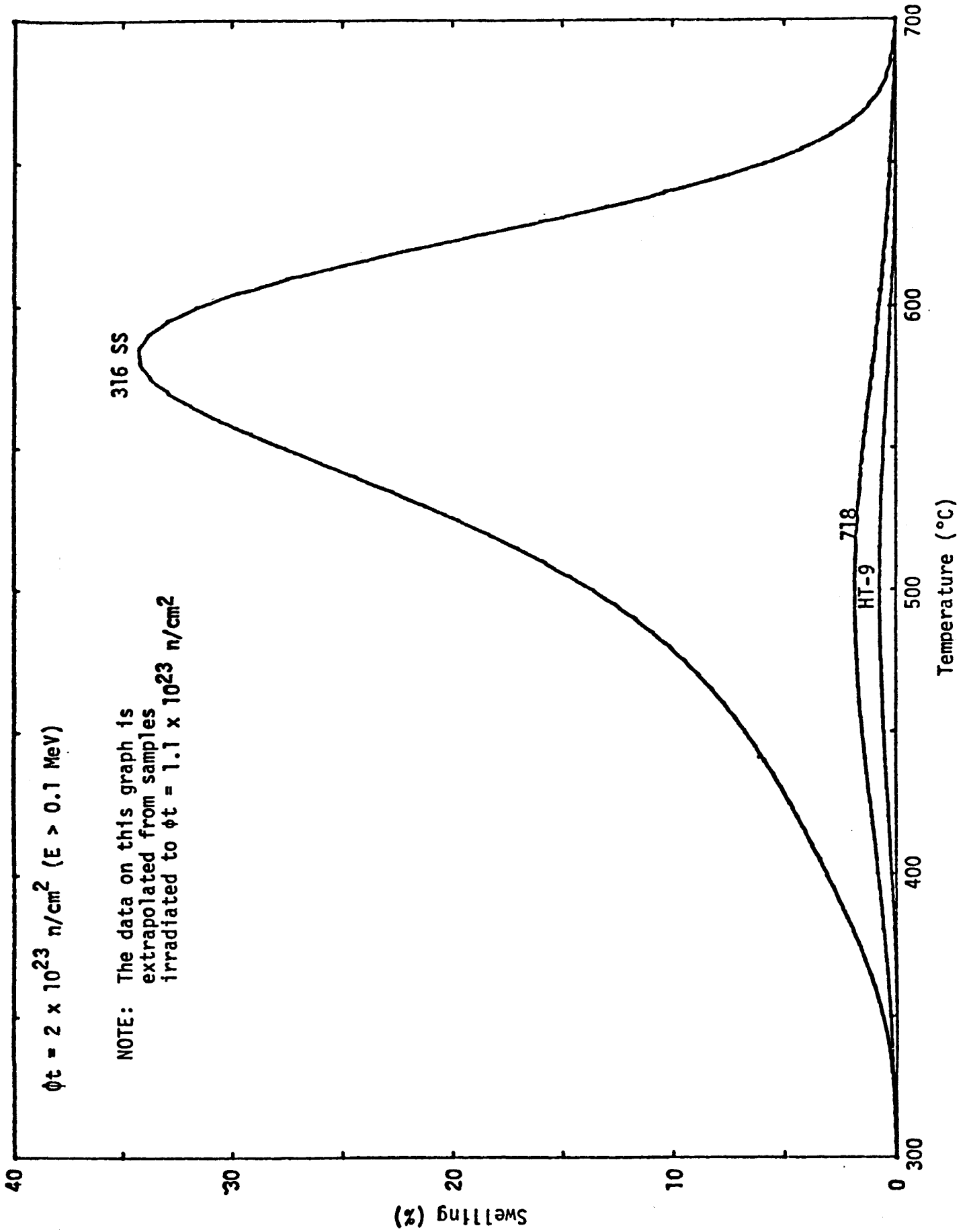


Fig. X-C-20. Swelling of candidate CTR materials during neutron irradiation. (45)

Table X-C-6
 Pertinent Data of Irradiation Effects on
 Tensile Properties of Ferritic Steel

<u>Material</u>	<u>T_{irr}/Test</u>	<u>dpa^(a)</u>	<u>Appm He^(b)</u>	<u>Yield Stress-MPa</u>	<u>Tensile Stress-MPa</u>	<u>Uniform Strain-%</u>	<u>Total Strain-%</u>
405 SS	---/500	Control	---	346	712	9.4	11.2
(38)	425/500	10	3.9	461	516	4.0	11.2
HT-9	550/550	48	19	N.R.	N.R.	N.R.	14.
(45)	550/670	48	19	N.R.	N.R.	N.R.	> 10
HT-9	---/253	Control	---	653	823	7.5	16.4
(37)	419/25	5	2	971	1037	6.3	13.9

N.R. = Not Reported

(a) use 4.4 dpa per 10^{22} n/cm² (E > 0.1 Mev)

(b) use 0.39 appm He/dpa

All data in Figure X-C-21 was not released by the Department of Energy at time of publication. However, see references 39, 44, and "In-reactor Creep Measurements", E.R. Gilbert and B.A. Chin, Nucl. Technology (to be published), for basis of curves.

stresses of 70 MPa and 2×10^{22} n/cm², diametral strains of only 0.4% are observed at 605°C.

It has also shown that HT-9 is rather insensitive to irradiation at low stresses. Figure X-C-22 reveals that insensitivity over the temperature range of 440°C to 600°C. At the maximum stress of ~ 37 MPa (p. VIII-14) and for a Young's modulus of 1.6×10^5 MPa at 500°C we see that

$$\log \left[\frac{\dot{\epsilon}}{\exp(-Q/RT)} \right] = -4.7$$

at 525°C and for $Q = 20$ kcal/mole, $\dot{\epsilon} = 6.35 \times 10^{-11}$ sec⁻¹. Hence it takes 5 years to produce a 1% creep strain.

X-C-8-f. DBTT Shift During Neutron Irradiation

Practically all the information available on DBTT shifts for ferritic steels has been obtained through low temperature (< 300°C) and low fluence (< 10^{21} ncm⁻² or ~ 0.5 dpa) data. Past experience has shown that if ferritic steels are irradiated under these conditions, significant DBTT shifts can occur. Figure X-C-23 gives data from seven different fluence levels on A 302B ferritic steel.⁽⁴⁸⁾ It can be seen that the DBTT is ~ 100°C at irradiation temperatures of 288°C up to 2.2×10^{19} n/cm² (0.01 dpa). This DBTT shift can be partially recovered and the 50% recovery time is 17 hr at 400°C or ~ 2 hr at 440°C.⁽⁴⁸⁾

Odette et al.^(48,49) have developed models that predict very little DBTT shift if the steels are irradiated above 350°C (Figure X-C-24). This indicates that operation in the 350°C to 500°C range may anneal out the defects causing the DBTT shift as fast as they are produced.

There is only one set of data on the HT-9 alloy at high temperatures and fluences. Smidt et al.⁽³⁷⁾ found that the transition temperature shifted from ~ 0°C (as received) to ~ 100°C after a 5 dpa (1×10^{22} n/cm²) irradiation at 427°C (Figure X-C-25). Presumably this shift will drop rapidly at higher temperatures so operation up to 530°C may not be a problem. Indeed, British scientists have found there is no DBTT shift following neutron irradiation at 450°C.⁽⁵⁰⁾ However, it is not known how much the DBTT shift will be after irradiation in the low temperature region (~ 350°C), but it is expected to be a maximum of ~ 40°C per dpa. Damage accumulated in that temperature region may have to be removed by periodic annealing.

All data in Figure X-C-22 was not released by the Department of Energy at time of publication. However, see references 39, 44, and "In-reactor Creep Measurements", E.R. Gilbert and B.A. Chin, Nucl. Technology (to be published), for basis of curves.

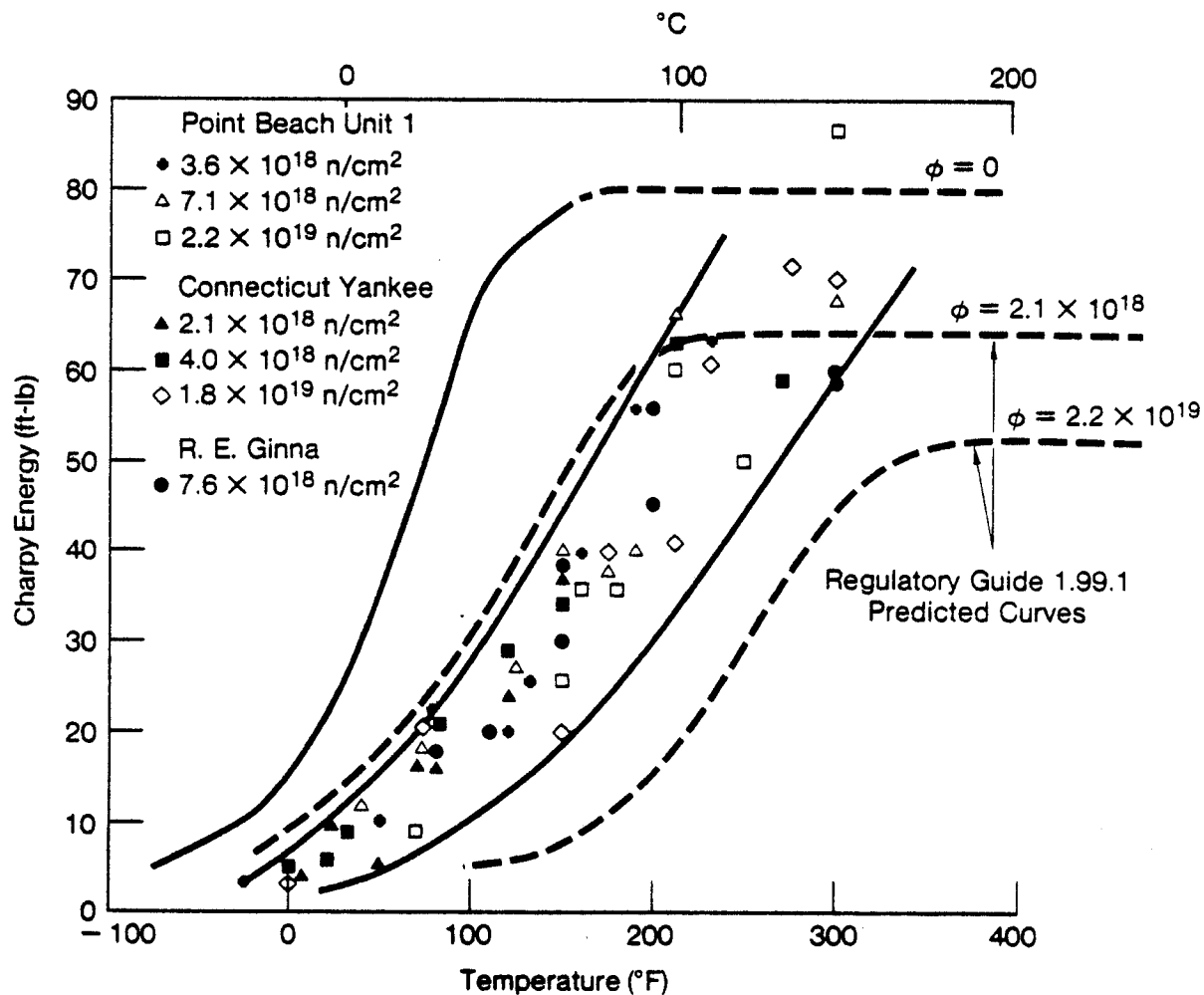


Fig. X-C-23. Low flux irradiation results for ASTM Reference A 302 B (0.20 weight percent Cu, 0.011 weight percent P) from three reactors. Charpy data for seven fluence levels with predicted Charpy curves (Regulatory Guide 1.99.1) for the lowest and highest levels. (48)

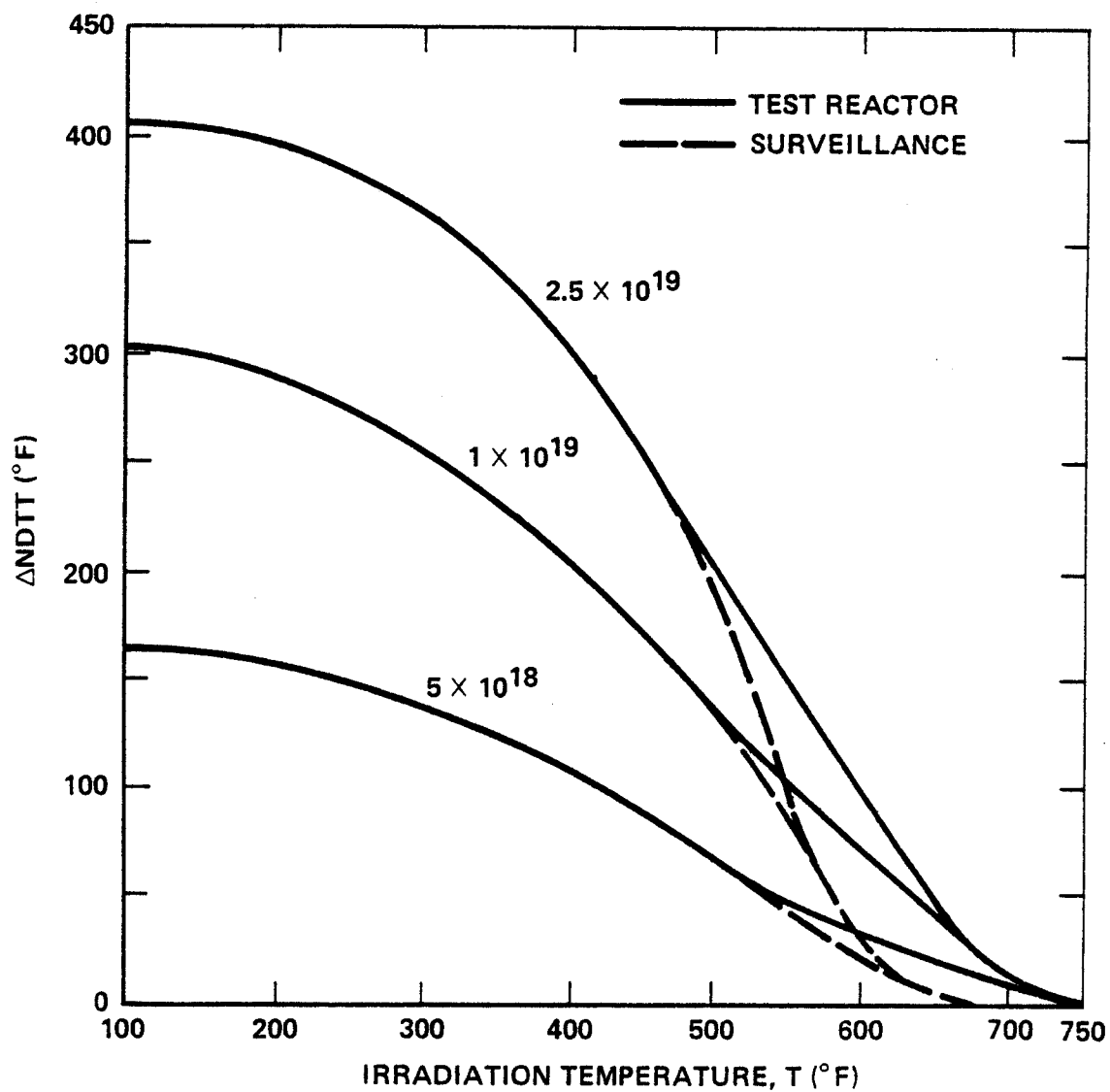


Fig. X-C-24. Predicted Temperature Dependence of NDTT Shifts for Test and Surveillance Conditions at Selected Fluences (49)

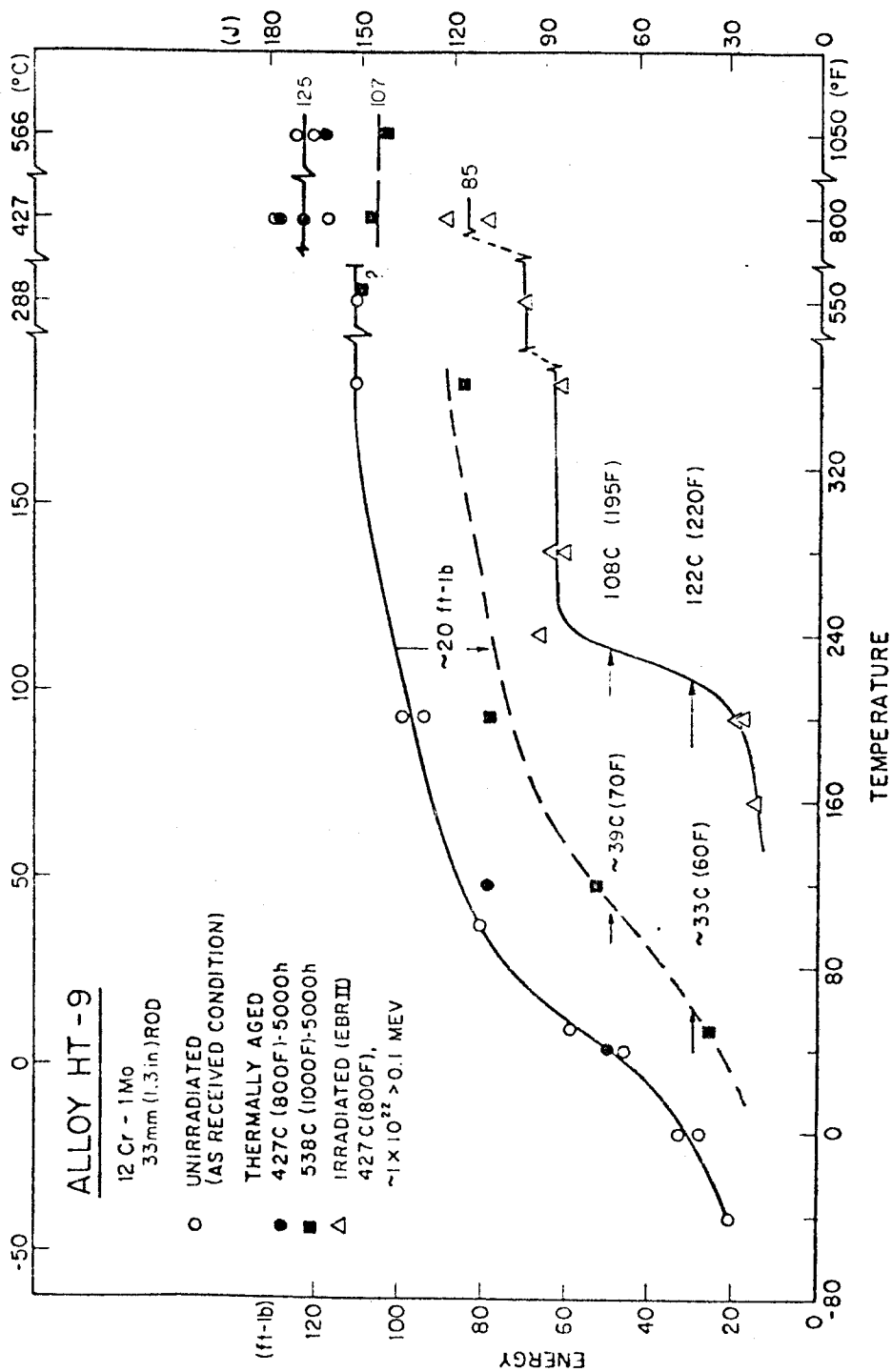


Fig. X-C-25. Charpy-V energy values as a function of test temperature for HT-9 in the as-received condition, after 5000 hr aging at 427°C, after 5000 hr aging at 538°C, and after irradiation to a neutron fluence of 1.1×10^{22} n/cm² ($E > 0.1$ Mev) at an irradiation temperature of $419^\circ\text{C} \pm 15^\circ\text{C}$ in the EBR-II reactor⁽³⁷⁾.

X-C-9. Expected Irradiation Performance of HT-9 in WITAMIR-I

In this section we will attempt to extrapolate the data discussed in previous sections to WITAMIR-I conditions. The reader is certainly aware by now that the uncertainties in this extrapolation process are very large and certainly more experimental and theoretical data is required before one could confidently predict component lifetimes. Nevertheless, we will proceed with our projections based on what we now know.

It appears that the swelling rate in the HT-9 alloy will be in the range of $0.01\% \text{ dpa}^{-1}$ or smaller over the 350°C - 530°C operating range. At the maximum damage rate of 32.4 dpa per operating year, this amounts to a maximum swelling rate of $\sim 0.32\%$ per year. The effect of much higher helium concentrations (typical of WITAMIR-I, $\sim 225 \text{ appm He}$ per year) on this swelling is unknown and close attention should be paid to experiments currently in U.S. thermal neutron fission reactors.

It appears that adequate total elongation values ($> 10\%$) can be retained at 550°C (20°C higher than the maximum in WITAMIR-I) and at dpa values corresponding to ~ 1.5 years of operation. However, the corresponding He levels are low ($\sim 19 \text{ appm}$) compared to what would be produced in WITAMIR during that time ($\sim 340 \text{ appm}$). On the positive side, there is no experimental evidence that the higher helium values will in fact cause a precipitous change in the ductility. At this time it is possible that the exposure levels could be increased by a factor of 3-4 before serious ductility problems arise.

The creep rates appear to be in the range of 0.2% per year and are not greatly affected by irradiation in the WITAMIR-I operating range. Since the creep rate depends on the damage rate, which is about the same in WITAMIR-I ($1.3 \times 10^{-6} \text{ s}^{-1}$) as in EBR-II ($\sim 1 \times 10^{-6} \text{ s}^{-1}$) where the data was taken, we feel somewhat more confident in predicting the 0.2% per year value.

Finally, the DBTT shift does not seem to be a problem for irradiations above 450°C . In fact, the only region of the WITAMIR blanket felt to be a problem is that area which operates between 350°C and 400°C . In that region we will take the conservative estimate that the DBTT increase is $\sim 20^{\circ}\text{C}$ per dpa. If we wish to limit the DBTT to a value below the freezing point of $\text{Pb}_{83}\text{Li}_{17}$ ($\sim 280^{\circ}\text{C}$) then we need to begin to anneal out the damage after roughly 4 months of operation. This can be done by reversing the flow in the blanket, subjecting the previously cold inlet section (330°C) to the hot outlet coolant (500°C). If we can remove 95% of the damage per anneal at long

annealing times (> 5 times the 50% defect removal time), then the frequency of flow reversal will have to increase such that after 5 years the flow reversals will have to occur approximately every month. Such a scheme should be more closely examined in future studies.

We have summarized our preliminary estimate of life limiting features of HT-9 for operation in WITAMIR in Table X-C-7. It can be seen that the most life limiting feature is the in reactor creep if a design value of 1% is maintained. However, this may actually be too pessimistic because:

- 1) the structure does not rely on close tolerances and could stand more than 1% creep strains;
- 2) if we reverse the coolant flow to alternately apply the highest temperatures at the top and bottom, we may be able to extend the life by a factor of 2 to 10 years over the 5 year limit.

Due to the uncertainties, we will keep the design value at 1% and assume the 5 year life because the other limits may be adversely affected once high helium containing material is tested. A reassessment of the life limiting features should be made periodically.

Table X-C-7
Life Limiting Features of HT-9 in WITAMIR-I

<u>Property</u>	<u>Design Value</u>	<u>Predicted Value</u>	<u>Predicted Operating Life-y</u>
Swelling	3%	0.01%/dpa	10
Ductility	> 2% (total)	< - 0.02%/dpa	10
In Reactor Creep	1%	0.2%/y	5
DBTT Shift	< 235°C (Residual)	20°/dpa (95% removal during anneal) Frequency of anneal < 12 y ⁻¹	10

X-D. Magnet MaterialsX-D-1. Metallic Materials

The main problem for WITAMIR is the increase in resistivity of stabilizers in the central cell and barrier coil regions. From the data in the neutronics section and X-B we find the following critical values:

<u>Area</u>	<u>Material</u>	<u>Dpa per Op. Year*</u>	<u>Limit - dpa⁽¹¹⁾</u>	<u>Op. Years to Reach Limit</u>
Central Cell	Al stablizer	5×10^{-7}	1.4×10^{-5}	28
	NbTi S/C	5×10^{-7}	7×10^{-2}	Long time
Barrier Coil	Cu stabilizer	4×10^{-6}	7×10^{-5}	17.5 years
	Nb ₃ Sn	4×10^{-6}	2×10^{-3}	Long time

*Operating year is 2.4 MW/m^2 at 80% P.F.

The Al stabilizer can probably be used for the lifetime of WITAMIR, but if necessary, one anneal at room temperature would be sufficient to remove most of the radiation damage in the stabilizer. The copper stabilizer in the region of the barrier coil facing the central cell would appear to reach its useful lifetime in roughly half the anticipated lifetime of WITAMIR. A full reactor lifetime could be achieved by two methods:

- A) Increasing the thickness of Cu stabilizer in the first 10 cm of the barrier coil such that the resistance increase for a given resistivity increase would still conform with the stability criteria.
- B) Anneal the Cu at room temperature twice during the reactor lifetime. It is known that $\sim 85\%$ of the irradiation induced resistivity increase will anneal out at room temperature. If the anneal were performed at 10 years and 20 years then at the 30th year, the resistivity increase in the copper would still be only 74% of the allowable value.

Either one of these solutions appears feasible and perhaps the latter would be the most economical because the warmups could be performed during the maintenance periods.

X-D-2. Organic Materials

The maximum dose to the organic insulating material in the WITAMIR central cell magnets is:

thermal insulator - 1.7×10^7 rads/op. year

epoxy - 1.2×10^7 rads/op. year

A recent analysis of organic materials for INTOR⁽¹¹⁾ reveals that reasonable limits might be $\sim 10^{13}$ rad for mylar thermal insulation and 10^9 to 10^{10} rads for epoxy electrical insulation. There is some optimism that the 10^{10} rad limit might be reasonable but for this study we will assume a limit of 5×10^9 rads. However, even at the latter value, it is possible to project 40 years of WITAMIR operating life for the epoxy insulation and this could be extended further by slight increases in the shield thickness. Therefore, it is reasonable to project a full 30 year life for WITAMIR central cell magnets.

Based on the factor of 2 higher displacement damage in the stabilizer of the barrier coils (compared to the solenoidal coils) it is quite possible that the gamma exposure to the electrical insulator of the barrier coil could be a factor of two higher also. This would place the useful lifetime of the coil at somewhat less than that (~ 20 years) of the required value. However, if the upper limit of 10^{10} rads were applied the situation would again be more favorable for the barrier magnet. With the uncertainty in the allowable limit we think that there is no serious problem in that coil at this time but future designs could be modified slightly to rectify that situation.

X-E. Response of other Materials in WITAMIRX-E-1. Boron Carbide Absorber

Because of the low operating temperature, the main problem in this material will probably be a volume change due to the accumulation of He from the $^{10}\text{B}(n, \alpha)$ reaction. As stated in a previous analysis of UWMAK-III⁽⁶⁾, significant volume changes do not occur until the ^{10}B reactions reach the 10^{21} cm^{-3} level. At that point, volume changes of $\sim 5\%$ might have to be accommodated. Since the maximum burnup, even after 30 years, is only $0.2 \times 10^{21} \text{ cm}^{-3}$ it is anticipated that there will be no particular problem with the B_4C over the lifetime of the WITAMIR device.

X-E-2. Components for Direct Convertor

In contrast to the components of the central cell where the main problem was associated with the bulk damage produced by neutrons, the main problem in the direct convertor is due to surface damage. The high flux of charged

particles (hydrogen, deuterium, tritium, and helium) will cause both sputtering and blistering to occur. An estimate of the useful lifetime of the components is given below.

X-E-2-a. Erosion Due to Sputtering

The particle fluxes and sputtering coefficients associated with those particles is summarized in Table X-E-1. The erosion rates of the grids are < 0.03 mm per operating year or < 1 mm over the lifetime of WITAMIR. Likewise the erosion rates of the plates are < 0.2 mm per operating year or < 6 mm over the reactor lifetime. Compared to grid and plate thicknesses of 3 mm and 8 mm respectively, we see that it is quite possible that, barring any other mode of failure, the components of the direct convertor might last the lifetime of WITAMIR. It is worthwhile noting that two things make such long life possible, first the low sputtering coefficient of materials like Ta and W, and secondly, the energy of the particles is so high that the absolute erosion rate is quite low.

X-E-2-b. Erosion Due to Blistering

There have been no known blistering studies on Ta-10W but there is a considerable amount of information on Nb which should be applicable. The main features of previous work on refractory metals at low temperature are that there seems to be a fluence which induces exfoliation (normally around 10^{18} cm^{-2} for helium and $\sim 10^{20}$ for hydrogen isotopes) and the exfoliated layer has a thickness corresponding to the range of the ion in the material. In a worst case condition one could calculate that a layer equal to the particle range is removed from the surface every 10^{18} ions cm^{-2} and that the erosion proceeds monotonically with fluence. We will use such "worst case" conditions to assess the magnitude of the problem.

The range of the various particles in Ta is given in Table X-E-2.

The major problems for the grids comes from the helium blistering which may occur at ~ 12 mm per operating year, or ~ 400 times the maximum sputtering rate. It is possible that there may be another 7 mm per operating year due to blistering by hydrogen isotopes but that remains more speculative.

The helium atoms also cause rather high blistering erosion rates on the collector plates. Again, values near 14 mm per year appear possible but the contribution from hydrogen isotopes drops to less than 1 mm per year.

Table X-E-1

<u>Particle</u>	<u>Energy - keV</u>	<u>Flux - cm⁻²s⁻¹</u>	<u>Sputtering Coefficient^(a)</u>	<u>Erosion rate, cm/op. year</u>
<u>Grid</u>				
H	960	2×10^{13}	$< 5 \times 10^{-5}$	$< 10^{-6}$
D	350	7.9×10^{15}	$< 3 \times 10^{-4}$	< 0.001
T	350	7.9×10^{15}	$< 5 \times 10^{-4}$	< 0.002
He	1000, 4000	$1.2 \times 10^{14(b)}$	$< 10^{-4}$	$< 10^{-4}$
			Total - Grid	< 0.003
<u>Plate</u>				
H	640	2×10^{13}	$< 8 \times 10^{-5}$	$< 10^{-6}$
D	35	7.9×10^{15}	$< 2.4 \times 10^{-3}$	< 0.008
T	35	7.9×10^{15}	$< 4 \times 10^{-3}$	< 0.01
He	3300	8×10^{13}	$< 3 \times 10^{-5}$	$< 10^{-5}$
He	300	4×10^{13}	$< 10^{-3}$	$< 2 \times 10^{-4}$
			Total - Plate	< 0.02

(a) Private communication, D. L. Smith, ANL.

(b) Divided roughly 2:1 for the 4000 and 1000 keV particles.

Table X-E-2Summary of Blistering Parameters for Direct Convertor Plates

<u>Particle</u>	<u>Energy - keV</u>	<u>Range - Microns</u>	<u>Seconds to Achieve Limit</u>	<u>Maximum Erosion Rate - mm/op. year</u>
H	640 (plate)	4.0(a)	5×10^6	0.02
	960 (grid)	5.5(a)	5×10^6	0.028
D	35 (plate)	0.28(b)	12,700	0.56
	350 (grid)	1.8(b)	12,700	~ 3.5
T	35 (plate)	0.35(b)	12,700	0.70
	350 (grid)	1.8(b)	12,700	~ 3.5
He	3300 (plate)	6(a)	12,400	12.1
	4000 (grid)	7(a)	12,400	14.3

(a) Ref. 51

(b) Ref. 52

Since the grids and plates are only 3 and 8 mm thick to begin with it is obvious that they will have a relatively short lifetime. If we say that the undamaged thickness should not be less than 1 mm, then the grids must be changed every 2 months due to helium blistering effects. The plates may last 6 months. While such a solution is possible, since the direct convertor is in a zone of low neutron production and it probably could be changed in less than a day of scheduled maintenance, such a possibility is not relished and ways of reducing this erosion rate must be found. One possible mitigating factor would be the observation by Roth et al.⁽⁵³⁾ who observed experimentally that the exfoliation rate does not continue at such high values for very long. It appears that the surface eventually becomes so "porous" that the helium is released as fast as it is injected. If this mechanism were to prevail, then even helium induced blistering would not cause excessive erosion.

X-F. Conclusions

From the previous analysis it appears that materials lifetime in WITAMIR is quite favorable. The exception to this statement would be the first few rows of coolant tubes in the blanket and the grids/collector plates in the direct convertor. The materials which appear to have a lifetime of at least 30 years are:

- Pb and B₄C shield materials.
- Ferritic steel shield structural material.
- Ferritic steel reflector region.
- Solenoidal central cell coil materials.
- Barrier coil materials (stabilizer, insulator, superconductor).
- Recircularizing coil materials.
- Yin-Yang coil materials.

Those which have shorter lifetimes are:

- First row coolant tubes (5 years of operation at 2.4 MW/m² at 80% P.F.).
- Grids/collector plates (< 2-6 months if linear blistering erosion model is assumed).

The materials limitation which has the most direct effect on the economics is probably the in reactor creep of the ferritic steel coolant tubes in the central cell. However, even here the experimental data is rather sparse and relatively little effort has been invested in finding metallurgical changes that might extend useful design lifetimes. It is conceivable that improvements by a factor of the two could be attained through a concentrated

research effort and thereby extend the useful life to 10 years. At this point there is relatively little effect on the economics by another factor of two improvement over the 5 year life but such a possibility should be investigated.

The situation with respect to blistering erosion of direct convertor grids or plates could be clarified with a year or two of modest experimental effort. The authors of this study strongly recommend that such experimental programs be implemented as soon as possible.

In conclusion, we feel that the materials problems for WITAMIR-I are far less severe than for our previous tokamak reactor designs. Since materials limitations have been at the heart of the concern for the commercial deployment of tokamak reactors, perhaps future research programs should pay more attention to the materials required for mirror reactors.

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XI. Tritium Systems

XI-A. Overview

This section describes the tritium systems of WITAMIR-I. The tritium pathways are summarized in Fig. XI-A-1, while the tritium inventories in the various reactor components are given in Table XI-A-1.

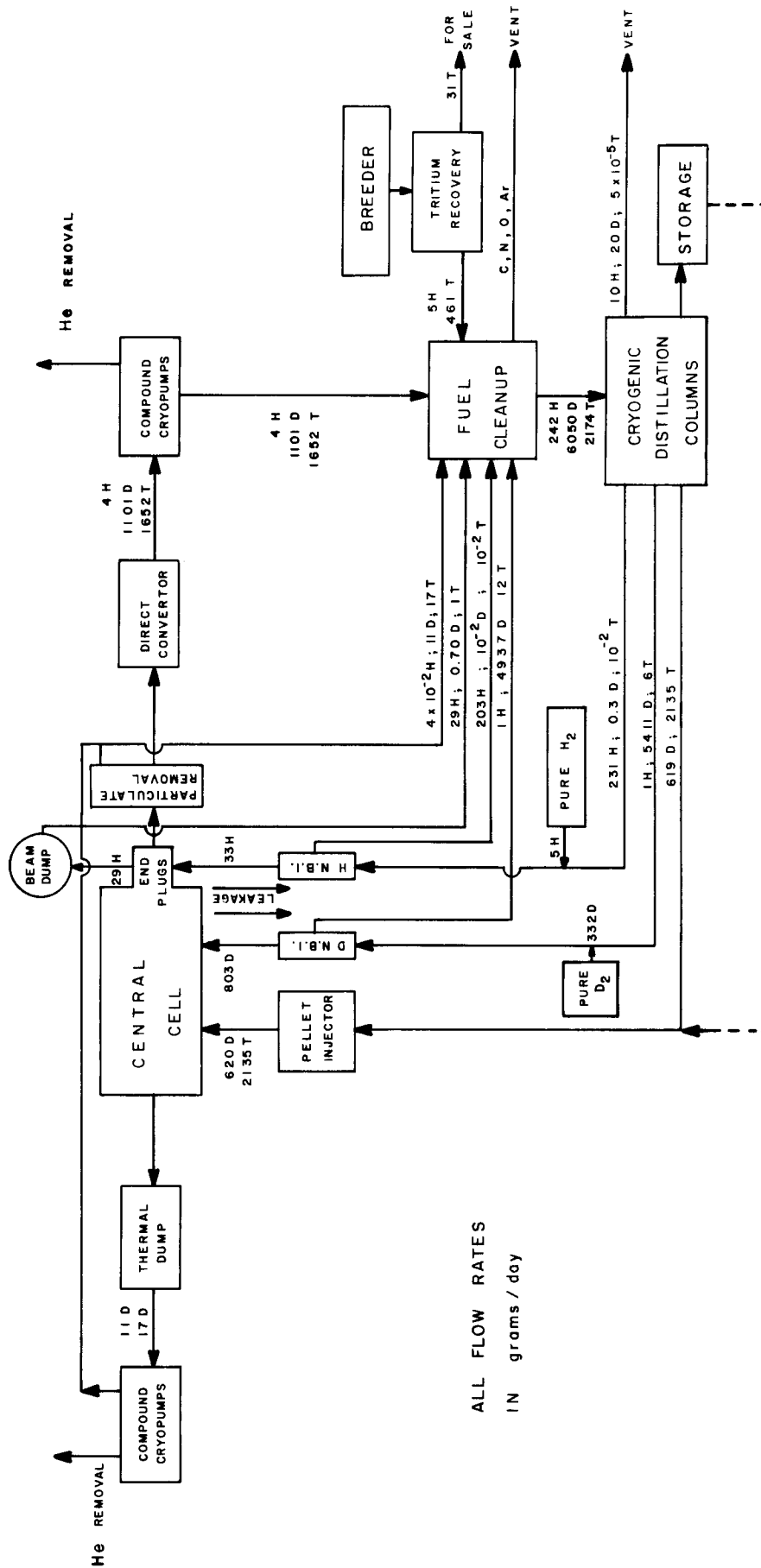
The central cell is fueled by DT pellets (70 atom % T) and deuterium neutral beam injectors and consumes 307 g D and 461 g T/d. The end plugs contain a proton plasma which is sustained by hydrogen neutral beam injectors (16.5 g/d per plug). Only 13% of the protons (2.1 g/d) are deposited in the plugs with the remainder traveling through the plasma and striking a beam dump. In addition, there is 0.20 kg of uninjected protium from the beam lines to be reprocessed. The fraction of DT fuel burned is 21.6%; thus 1.67 kg T and 1.11 kg D/d must be handled by the reactor exhaust processing equipment. In addition, 4.93 kg of uninjected deuterium from the beam lines must be reprocessed.

The hydrogen isotopes exit the reactor chamber axially with ~99% striking a direct convertor and the remainder leaking to a thermal dump. The alpha particles (614 g/d) have approximately a 50:50 distribution. The Ta-10W direct convertor ($T_{inv} = 2$ g) and thermal dump are pumped by compound cryopumps with on-line times of 4 hours and have a tritium inventory, T_{inv} , of 279 g. In regenerating the pumps the helium is separated from the hydrogen isotopes. The hydrogen isotopes are subsequently sent to a fuel cleanup unit ($T_{inv} = 20$ g) where impurity atoms are removed. The purified hydrogen isotopes then enter the cryogenic distillation columns ($T_{inv} = 40$ g) where the hydrogen isotopes are separated into an HD stream for venting to the atmosphere, a high purity D₂ stream for the deuterium neutral beam injectors ($T_{inv} = 1$ g), a DT (30:70 atom %) stream for the pellet maker ($T_{inv} = 83$ g) and a high purity H₂ stream for the protium neutral beam injectors. This results in a total tritium inventory in the fueling and exhaust cycle of 425 g.

One day's fueling supply of tritium (2135 g) is maintained in storage in the event of any plant tritium-handling equipment malfunction. The tritium is stored on uranium beds.

Tritium is bred at a rate of 492 g/d in the liquid lithium-lead eutectic, Li₁₇Pb₈₃. This alloy (8.4×10^6 kg) serves both as the coolant and breeder and is consequently circulated (4.4×10^8 kg/hr) throughout the power cycle.

HYDROGEN ISOTOPE PATHWAYS IN WITAMIR-I



ALL FLOW RATES
IN grams / day

Fig. XI-A-1

Table XI-A-1
Tritium Inventory in WITAMIR-I

Fueling and Exhaust System (g)		425
Subtotals (g)		
Plasma	0.1	
Direct Converter	2.3	
Cryopumps	279	
Fuel Cleanup	20	
Distillation Columns	40	
Pellet Injector	83	
N.B.I. Cryopumps	1	
Blanket System (g)		
Subtotals (g)		205
Breeder	101	
Sparge Tank	1	
Uranium Beds	103	
Storage (g)		2135
Total (g)		2765

A steady state concentration of 0.012 wppm tritium in $\text{Li}_{17}\text{Pb}_{83}$ ($T_{\text{inv}} = 101 \text{ g}$) results in a T_2 pressure of 1×10^{-4} torr. A sidestream ($4.4 \times 10^6 \text{ kg/hr}$) of the breeder/coolant is circulated to a tritium extraction unit. Here the $\text{Li}_{17}\text{Pb}_{83}$ is sparged with an inert gas, such as helium (3.5 l(STP)/s), while Roots blowers maintain a vacuum (10^{-2} torr) on its surface. Thus the volumetric throughput of gas the pumps must handle is $2.9 \times 10^5 \text{ l/s}$ at 300 K. The tritium-containing inert sparge gas is circulated over uranium beds (50 kg) to remove 99% of the tritium. The tritium is then merged with the reactor exhaust gas. The tritium inventory in the inert gas purge tank is 1 g and on the uranium beds, 103 g, for a total blanket system inventory of 205 g.

An alternative method of recovering tritium is to contact the lithium-lead eutectic with a molten salt (LiF-LiCl-LiBr) in a centrifugal annular contactor. The tritium is preferentially distributed in the salt phase (98%), and the salt is then electrolyzed for 5 minutes to recover 30% of the tritium. The electrolytically generated tritium is swept out of the molten salt with an inert gas stream and recovered, as before, on uranium beds. The tritium inventory in the molten salt extractor unit is 7 g giving a total blanket system inventory in this case of 211 g.

Hydrogen isotope recovery from liquid lithium-lead alloys has not been experimentally verified for either the inert gas purging or the molten salt extraction scheme. If both schemes work, the inert gas purging would be preferred as it is the cleaner extraction scheme. The molten salt extraction scheme has the problem of introducing halides into the $\text{Li}_{17}\text{Pb}_{83}$.

Tritium permeation through the HT-9 steam generator tubes is a problem due to the relatively high tritium blanket pressure (1×10^{-4} torr). Therefore, double-walled tubes are used in which the annulus of the tubes is continually purged with an oxidizing atmosphere (1 atm helium containing 1 torr oxygen). Tritium losses to the steam, under these conditions, are expected to be much less than 10 Ci/d. The tritiated water in the sweep gas is condensed out on cold surfaces and eventually merged with the reactor exhaust.

XI-B. Fueling and Exhaust System

WITAMIR-I is a steady-state tandem mirror fusion device. During normal operation the central cell produces 3000 MW of fusion power from an equimolar deuterium-tritium plasma. The plasma in the end cells is composed of protium and does not produce power but rather serves as a barrier to deuterium and tritium loss from the central cell. This barrier method of minimizing the

loss of fuel from the power-producing plasma is very efficient. Thus, the fraction of fuel burned is high, 21.6%, and only 1.7 kg of tritium and 1.1 kg of deuterium must be handled by the exhaust system per day. This burnup is in agreement with the 18% value found in an earlier tandem mirror study by Lawrence Livermore Laboratory.⁽¹⁾ Thus, tandem mirrors possess burn fractions much higher than other magnetic confinement devices ($\leq 5\%$) and comparable to those of inertial confinement reactors (20-40%).⁽²⁾ Since the relationship between the fractional burnup, f_b , and the unburned tritium that must be pumped out the chamber per day, T_p , is $f_b \equiv T_b / (T_b + T_p)$, where T_b is the amount of tritium burned per day, the high burnups in tandem mirrors represent one of their most attractive features. The fueling and exhaust system characteristics of WITAMIR-I are given in Table XI-B-1.

XI-B-1. Fueling

Fueling of the central cell is by frozen deuterium and tritium (30 atom % D : 70 atom % T) pellet injection and by deuterium neutral beams (Table XI-B-1). This method of fueling, whereby tritium is injected wholly by means of pellets, has a significant advantage over tritium neutral beam fueling used in previous mirror reactor design studies.^(1,3,4) Neutral beam injection is an inefficient fueling method, with the amount of fuel circulated around the beam lines being as much as ten times larger than that actually injected into the chamber.⁽¹⁻⁴⁾ The deuterium and tritium not injected become isotopically contaminated and must be reprocessed. This additional tritium flow has a significant impact on tritium handling systems and negates the advantage gained by the high burnup fraction. For this reason no tritium neutral beams are employed in WITAMIR-I. An assumption that must be emphasized at this point is that the fractional burnup and tritium handling values given in Table XI-B-1 assume 100% of the material in the pellet is ionized and is deposited within the plasma.

Neutral beams are used to inject 56% of the deuterium fuel. Here it is assumed that the gas feed to the neutral beam system is converted to energetic neutral atoms with an efficiency of 14%. Thus, a total of 5.7 kg of deuterium is handled by the beam lines per day, of which only 0.80 kg is injected into the central cell with the remaining 4.9 kg being recycled through the fuel reprocessing system. Backstreaming of atoms from the plasma in the reactor chamber into the injectors occurs at a rate assumed to be equal to about 1% of the injection rate. The result is that 6 g of tritium per day stream up the

Table XI-B-1
Fueling and Exhaust System Characteristics

	Tritium	Deuterium	Protium	Helium
Fraction Burned (%)	21.6	21.6	0	---
Fuel Injected (g/d)				
Pellet	2135	625	0	---
Neutral Beam	0	803	33*	---
Fuel Burned (g/d)	461	307	0	---
Fusion Ash (g/d)	---	---	small	614
Exhaust Pumped (g/d)	1674	1116	33*	614

*4.2 g/d is deposited in the plasma; 28.8 g/d is handled by the beam dump.

beam ports. This tritium must be pumped by the deuterium neutral beam cryopumps.

Neutral beams are also used to inject protium into the end cells at a rate of 4.2 g per day. However, since only 12.8% of the injected protium is trapped in the end cells (with the remainder passing to a beam dump), the neutral beam injection rate must be increased to 33 g/d. The gas efficiency is once again assumed to be 14% and the plasma backstreaming to be 1% with the result that 202 g of uninjected protium from the beam lines becomes contaminated with 3.8×10^{-5} g of tritium. The quantity of tritium streaming up the beam ports is extremely low because the DT density in the end plugs is roughly a factor of 5×10^4 less than in the central cell.

The uninjected neutral beam deuterium and protium can be processed simply.⁽⁵⁾ These gases, in two separate streams, pass through charcoal absorption traps at 75 K to remove impurities. The flow-through D₂ and H₂ streams then go directly to the distillation columns of the isotope separation system (Section XI-B-2-d). Regeneration of the charcoal traps at 350 K and 1 torr releases a variety of impurities along with some coadsorbed hydrogen isotopes. All these are then sent to the mainstream fuel cleanup unit⁽⁵⁾ (Section XI-B-2-c).

XI-B-2. Reactor Exhaust Processing

The hydrogen isotopes and helium ash flow axially through the end cells and are deposited at one end in a direct convertor and at the other end in a thermal dump. It is assumed here that 99% of the hydrogen isotopes flow to the direct convertor with only 1% leaking to the thermal dump. The alpha particles have roughly a 50:50 distribution. Both ends of the reactor are pumped with compound cryopumps. The deuterium and tritium are then sent to the fuel cleanup unit, the isotope separation unit and ultimately to the pellet maker and neutral beam lines for reinsertion into the plasma.

Protium, contaminated with small quantities of deuterium and tritium, from the beam dump must also be recovered. This issue has not been addressed, though once the 28.8 g of protium is recovered it is sent to the fuel cleanup unit (Section XI-B-2-c).

XI-B-2-a. Direct Convertor

The direct convertor material is Ta-10W. The 350 keV hydrogen isotope ions striking the grid have a penetration depth of 1.3 μm , while the 35 keV ions striking the plate have a penetration depth of 0.15 μm . The grid and

plate surface areas are 16.7 and 167 m², respectively. If Ta-10W is assumed to behave similarly to niobium, then the tritium concentration in the layer penetrated by the ions is 1×10^{19} atoms/cm³. Thus, the tritium inventory is 1.1 g in the grid and 1.2 g in the plate. These inventories assume tritium is present only in the layer penetrated by the ions.

XI-B-2-b. Compound Cryopumps

The hydrogen isotopes and helium are pumped from the surface of the direct convertor and the thermal dump with compound cryopumps. It has been demonstrated that a compound pump can simultaneously pump hydrogen isotopes and helium. The separation of the hydrogen on the condensation panel from helium on the cyrosorption panel is extremely sharp. Thus, separation of the hydrogen isotopes from the helium ash is accomplished at this step. The helium panel is first regenerated, after which the cryocondensation panel is warmed to remove the hydrogen isotopes from the pump.⁽⁶⁾ The hydrogen isotopes are then sent to the fuel cleanup unit.

Two banks of pumps are required for WITAMIR-I. One bank is on-line while the second is being regenerated. The on-line time is 4 hours and the tritium inventory tied up in the pumps is 0.28 kg.

XI-B-2-c. Fuel Cleanup

The fuel cleanup unit serves to purify the hydrogen isotopes from any tritiated water, methane, ammonia, argon or any other species that would freeze out at the liquid hydrogen temperatures employed in the isotope separation system.⁽⁵⁻⁷⁾ Impurity levels must be maintained below 1 ppm to ensure safe, long-term operation of the isotope separation system.⁽⁷⁾

The tritiated water and ammonia are condensed out of the hydrogen isotope stream, after which the low melting impurities are separated from the hydrogen molecules by adsorption on molecular sieve beds at 75 K. The recovered impurities are then catalytically oxidized to form tritiated water and tritium-free compounds. The water is finally condensed out and electrolyzed to recover the tritium.⁽⁷⁾

The tritium contaminated deuterium and protium from the neutral beam lines needs only to be circulated over liquid nitrogen cooled charcoal beds before being sent to the isotope separation system. The total tritium inventory in the fuel cleanup unit should be roughly 20 g.⁽⁵⁾

XI-B-2-d. Isotope Separation System

Cryogenic distillation columns are used to separate the hydrogen isotopes into 4 streams: HD waste which is vented to the atmosphere, high purity D_2 for the deuterium neutral beam injector, a D-T stream containing 70% tritium for use in the pellet injectors and high purity H_2 for use in the end plug neutral beam injectors. The cryogenic distillation columns are modeled on those in the literature^(3,6,8) and operate as shown in Fig. XI-B-1. The tritium inventory in the distillation columns is estimated to be 40 g.

XI-B-2-e. Storage

A certain quantity of tritium must be maintained in storage in the event of a temporary malfunction of any of the tritium handling equipment, such as the fuel cleanup unit, the isotope separation system or the blanket tritium extraction system. This tritium should be in a purified state with only a minimal time required to release it from storage and bring it to the cryogenic temperatures of the pellet maker.

The tritium is stored on uranium beds in WITAMIR-I. A problem exists with the formation of helium-3 if the tritium is stored for extended periods of time⁽⁹⁾ and a falling film T_2/He separator⁽⁵⁾ may be required to remove helium from the tritium before insertion into the reactor chamber. Tritium equivalent to one day's pellet injection rate (2,135 g) is stored on the uranium beds.

XI-C. Blanket System

The tritium breeding material in WITAMIR-I is a liquid lithium-lead eutectic of composition 17 atom % Li : 83 atom % Pb operating between 329 and 500°C. The eutectic also serves as the heat transport agent and is circulated through the steam generator tubing made of double-walled HT-9 ferritic steel (~ 11% chromium). To design the tritium extraction system and to estimate the loss of tritium to the environment, it is necessary to know the thermodynamic and kinetic properties of the lithium-lead-hydrogen isotope system and of the HT-9-hydrogen isotope system.

XI-C-1. Properties of Blanket Materials

First, the thermodynamics of the $Li_{17}Pb_{83}$ eutectic are examined. The liquid lithium-lead system has been well studied by both electrochemical⁽¹⁰⁻¹²⁾ and vapor pressure⁽¹³⁾ techniques at temperatures ranging from 477 to 800°C. The activity of lithium in molten $Li_{17}Pb_{83}$ as a function of temperature is given in Figure XI-C-1. The activity of lithium is seen to be low

WITAMIR-I FUEL ISOTOPE SEPARATOR

(ALL FLOW RATES IN g/d ;

T_i = STEADY-STATE TRITIUM INVENTORY)

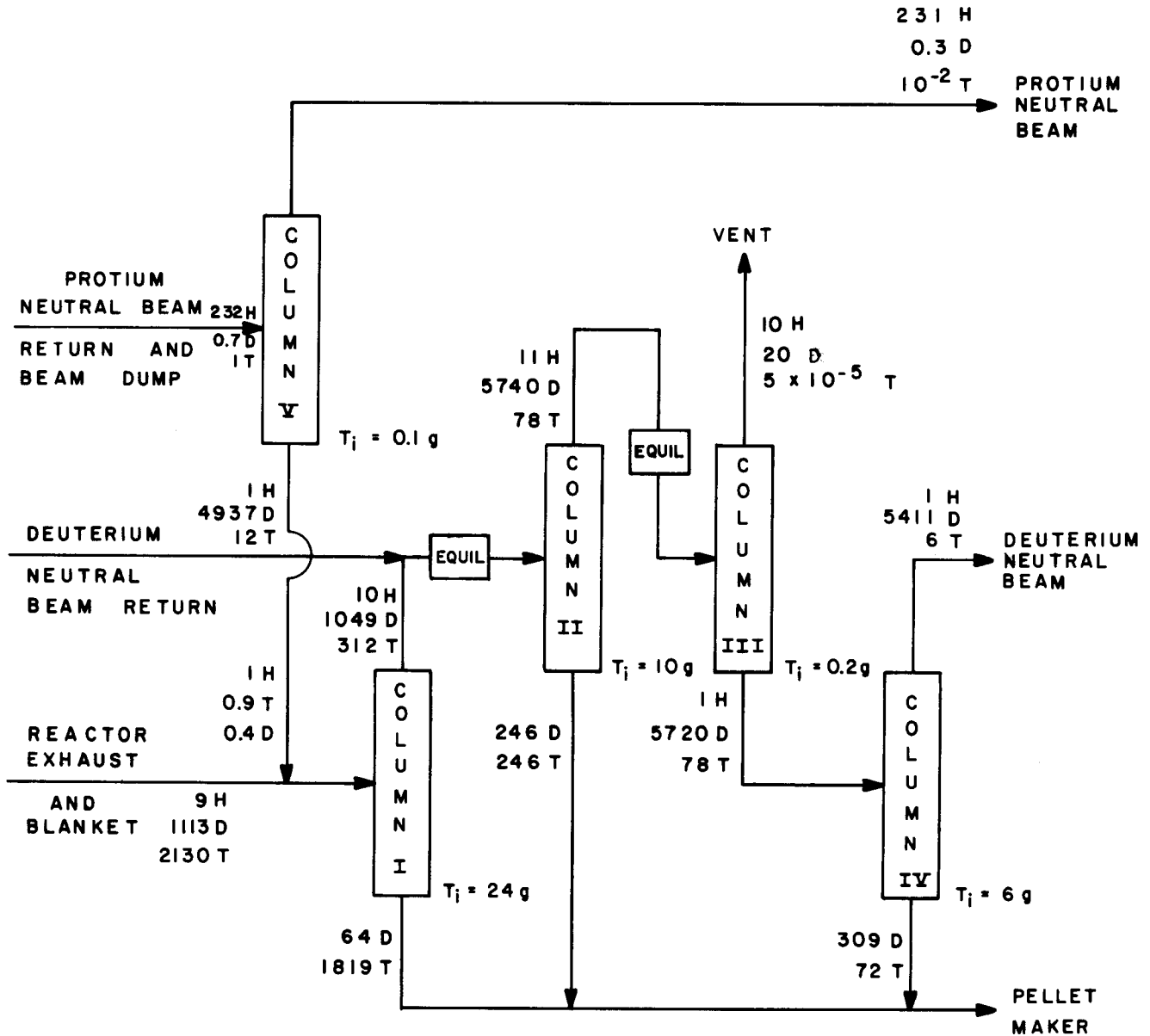


Fig. XI-B-1

LITHIUM ACTIVITY IN 17 Li: 83 Pb

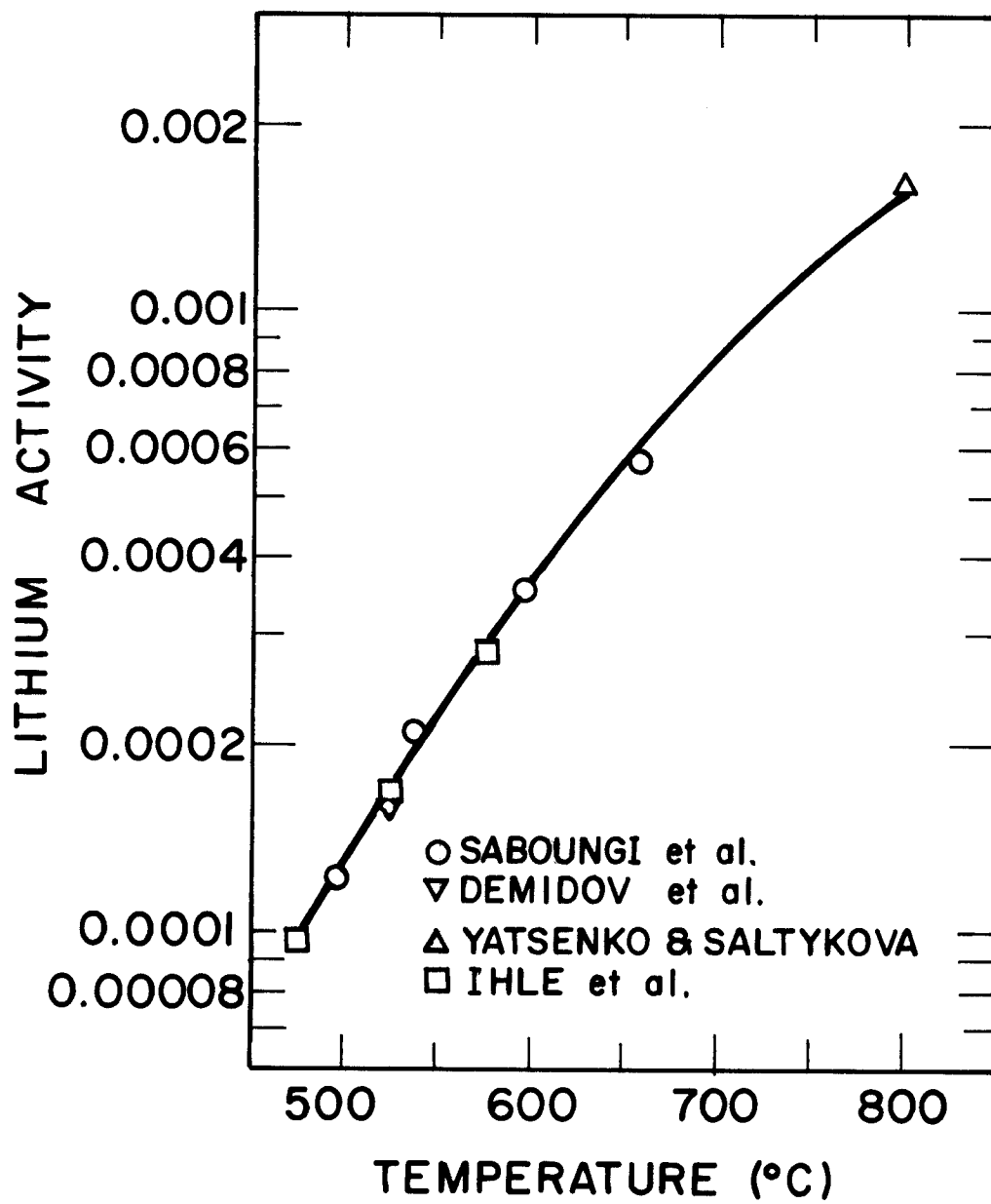


Fig. XI-C-1

and it is expected that the chemical properties of $\text{Li}_{17}\text{Pb}_{83}$ will differ substantially from pure lithium. The activity of lead is almost equal to its atomic fraction,⁽¹⁰⁻¹²⁾ ranging from 0.79 at 800°C to 0.82 at 523°C. Thus, it appears that the chemical behavior of $\text{Li}_{17}\text{Pb}_{83}$ would more closely approximate that of lead than lithium.

The vapor pressures of pure liquid lithium⁽¹⁴⁾ and lead⁽¹⁵⁾ are given by the following equations:

$$P_{\text{Li}}(\text{torr}) = \exp(18.4 - 18750/T(\text{K}))$$

and
$$P_{\text{Pb}}(\text{torr}) = \exp(17.86 - 22300/T(\text{K})) .$$

These pressures must be multiplied by the activity of the component in the alloy to yield the pressures of lithium and lead above the alloy (Table XI-C-1).

The densities of liquid lithium-lead alloys of various compositions have been investigated at temperatures from 424 to 777°C.⁽¹⁶⁾ Interpolation of these data yield the values for $\text{Li}_{17}\text{Pb}_{83}$ given in Table XI-C-1.

XI-C-1-a. Tritium Solubility in $\text{Li}_{17}\text{Pb}_{83}$

Only one study⁽¹³⁾ has been conducted on the thermodynamics of the molten lithium-lead-hydrogen isotope system. The data from this study are given in Fig. XI-C-2. One of the conclusions of this study is that the solubility of deuterium in lithium-lead alloys is lower than pure lithium due to the low activity of lithium in lithium-lead alloys. The applicability of these data to the conditions existing in WITAMIR-I are limited. There is only one experimental point at 17 atom percent lithium, and this is at a temperature of 767°C - or hundreds of degrees higher than those existing in WITAMIR-I. The problem is then to extrapolate the value in Fig. XI-C-2 to the WITAMIR-I operating conditions. Fig. XI-C-3 summarizes the solubility of hydrogen in both liquid and solid metals;⁽¹⁷⁻²⁰⁾ the point for $\text{Li}_{17}\text{Pb}_{83}$ is also given. (The data for pure lead are poor^(19,20) but are included in Fig. XI-C-3 because of their applicability to the question of hydrogen solubility in $\text{Li}_{17}\text{Pb}_{83}$.) There is a definite trend - metals with high solubilities dissolve hydrogen exothermically, while metals with low solubilities dissolve hydrogen endothermically. It appears that $\text{Li}_{17}\text{Pb}_{83}$ belongs to an intermediate category of metals in which hydrogen solubility shows only a weak dependence on

Table XI-C-1

Properties of Liquid $\text{Li}_{17}\text{Pb}_{83}$ at the WITAMIR-I Blanket Temperatures

	<u>329°C</u>	<u>500°C</u>
Activity		
Lithium	9.8×10^{-6}	1.3×10^{-4}
Lead	0.83	0.81
Pressure (torr)		
Lithium	2.9×10^{-11}	3.7×10^{-7}
Lead	3.9×10^{-9}	1.4×10^{-5}
Density (g/cm^3)	9.48	9.38

SOLUBILITY OF DEUTERIUM IN LIQUID Li-Pb ALLOYS

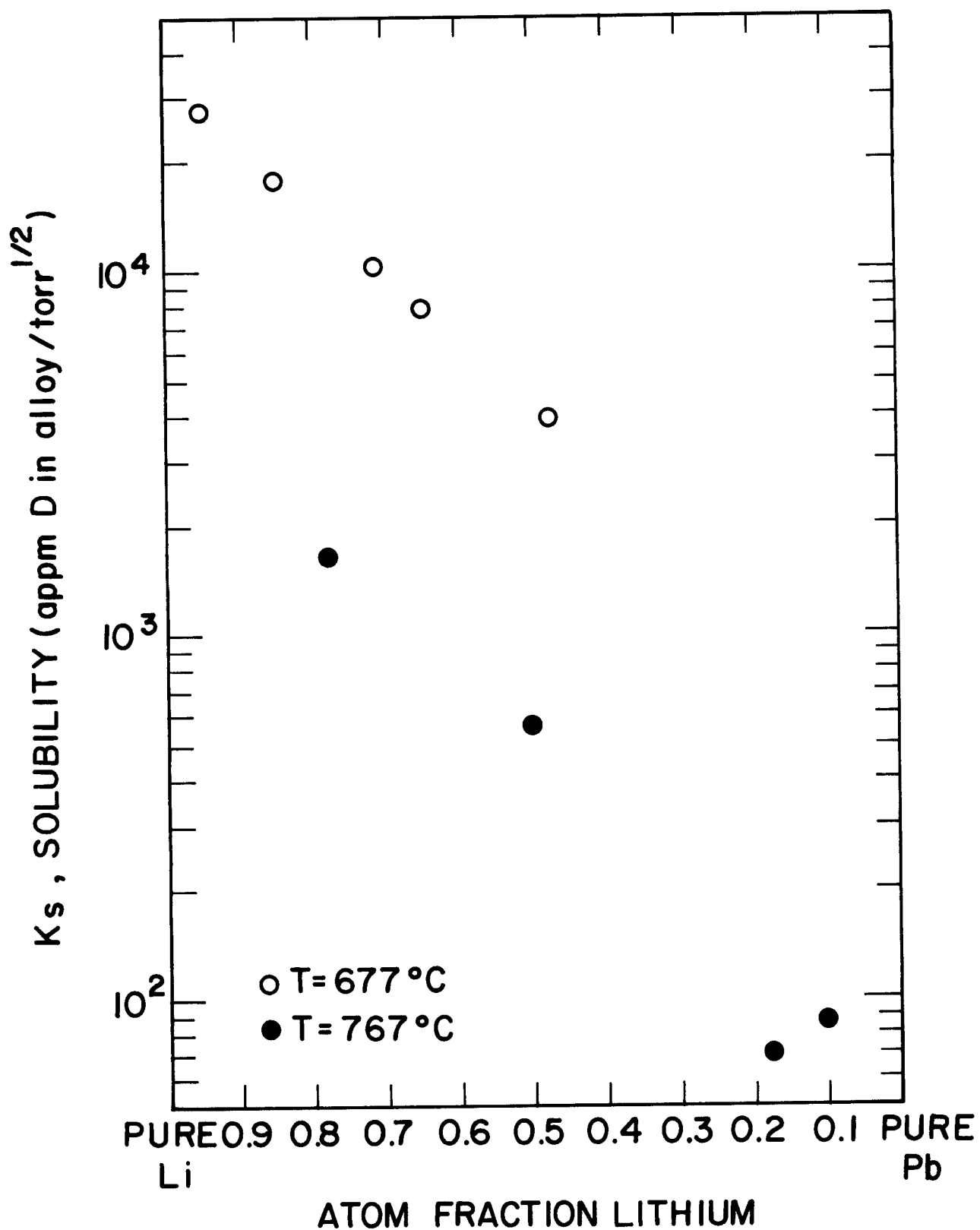


Fig. XI-C-2

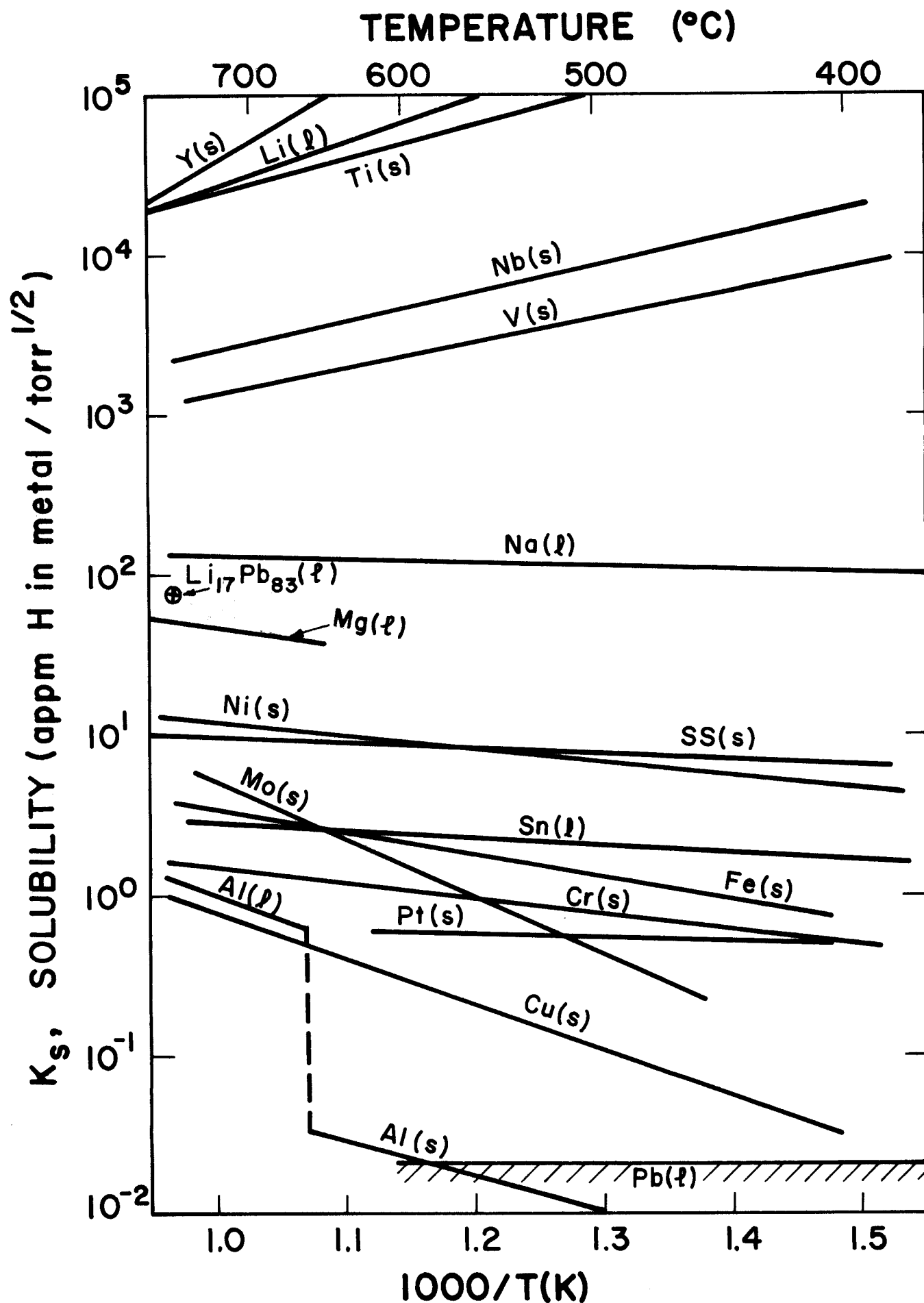


Fig. XI-C-3 THE SOLUBILITY OF HYDROGEN IN SELECTED METALS AND ALLOYS AS A FUNCTION OF TEMPERATURE.

temperature. Because it is difficult to predict whether this dependence will be positive or negative, it is assumed here that the solubility of hydrogen is independent of temperature. The Sieverts constant for tritium solubility in liquid $\text{Li}_{17}\text{Pb}_{83}$ for the WITAMIR-I conditions is then 70 appm T in $\text{Li}_{17}\text{Pb}_{83}/\text{torr}^{1/2}$ or 1.2 weight ppm/ $\text{torr}^{1/2}$.

XI-C-1-b. Tritium Diffusion in $\text{Li}_{17}\text{Pb}_{83}$

The diffusion of hydrogen isotopes in lithium-lead alloys has not been studied. The work that has been done on the diffusion of hydrogen in pure liquid metals is summarized in Fig. XI-C-4. The aluminum work⁽²¹⁻²³⁾ represents the line of best fit for 3 studies, the copper work⁽²³⁻²⁷⁾ 7 studies, the iron⁽²⁸⁻³⁰⁾ work 10 studies, the nickel work^(23,26,27,29,30) 4 studies and the lithium,⁽³¹⁾ silver,⁽²⁷⁾ magnesium⁽³²⁾ and cobalt⁽³⁰⁾ work one study each. It is interesting to note that all the diffusivity data with the exception of that for lithium, silver and magnesium are of the same order of magnitude. The lithium, silver and magnesium data are based on only one study each, and the scatter in diffusivity values found by different investigators can be substantial, as illustrated by the work on copper⁽²³⁻²⁷⁾ (Fig. XI-C-5). Thus, it is possible that further experiments on lithium, silver and magnesium would show the hydrogen diffusivities to be nearer to $10^{-3} \text{ cm}^2/\text{s}$ than indicated by Fig. XI-C-4.

Diffusion studies of hydrogen in liquid iron alloys,⁽²⁸⁻³⁰⁾ nickel alloys^(23,30) and magnesium alloys⁽³²⁾ have also been conducted. In each case the values found reinforce the conclusion that the diffusion coefficient of hydrogen in liquid metals is generally of the order of $10^{-3} \text{ cm}^2/\text{s}$. Therefore, tritium is assumed to have a diffusion coefficient of $10^{-3} \text{ cm}^2/\text{s}$ in liquid $\text{Li}_{17}\text{Pb}_{83}$.

XI-C-1-c. Tritium Solubility, Diffusivity and Permeability in HT-9

The hydrogen isotope solubility, diffusivity and permeability in HT-9 ferritic steel are discussed together. Data is scarce in these three areas, but if it is assumed⁽³³⁾ that:

$$\text{Permeability} = \text{Solubility} \times \text{Diffusivity},$$

then the available data can be used to determine self-consistent solubility, diffusivity and permeability equations.

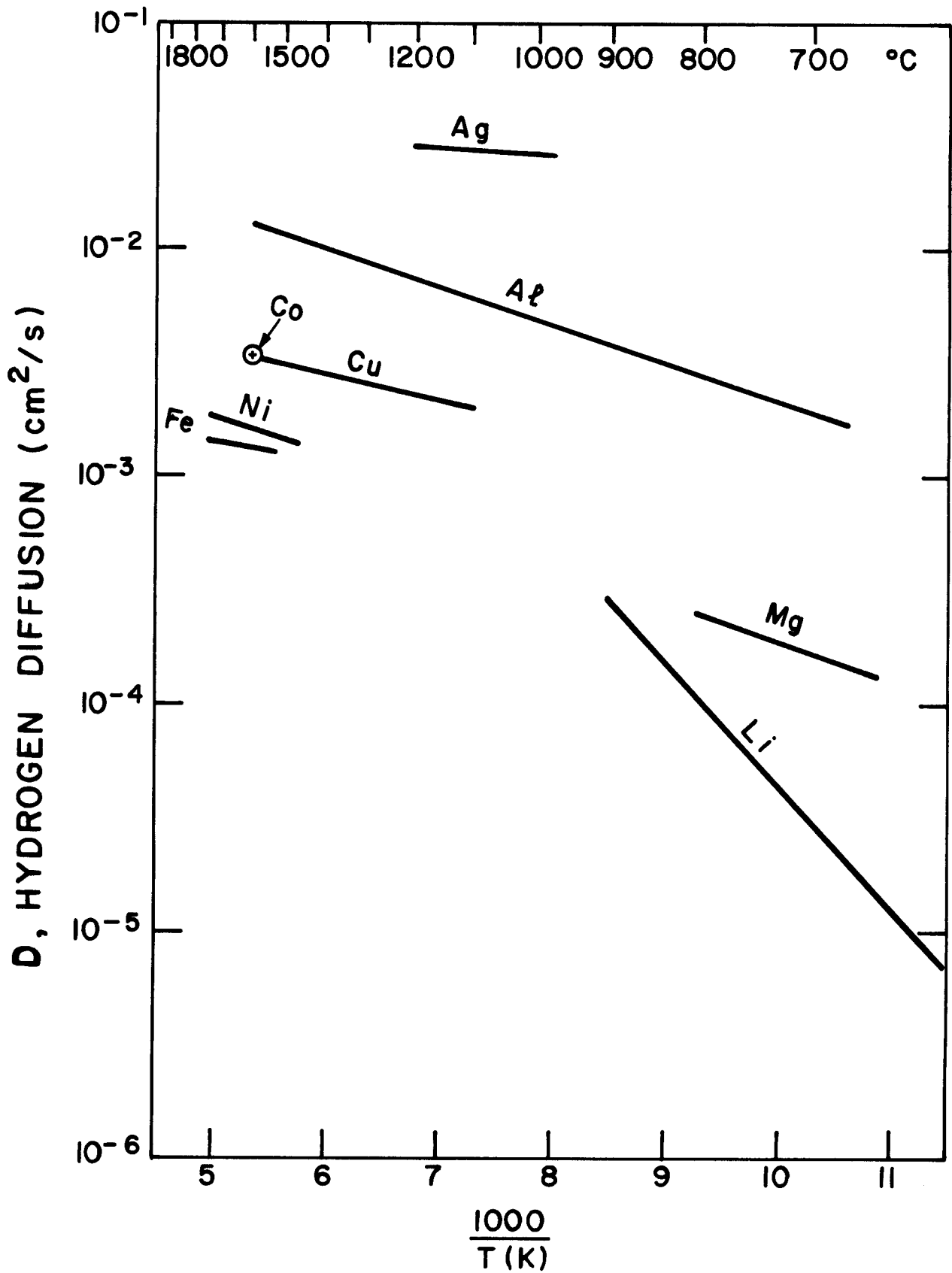


Fig. XI-C-4. The Diffusion of Hydrogen in Liquid Metals as a Function of Temperature.

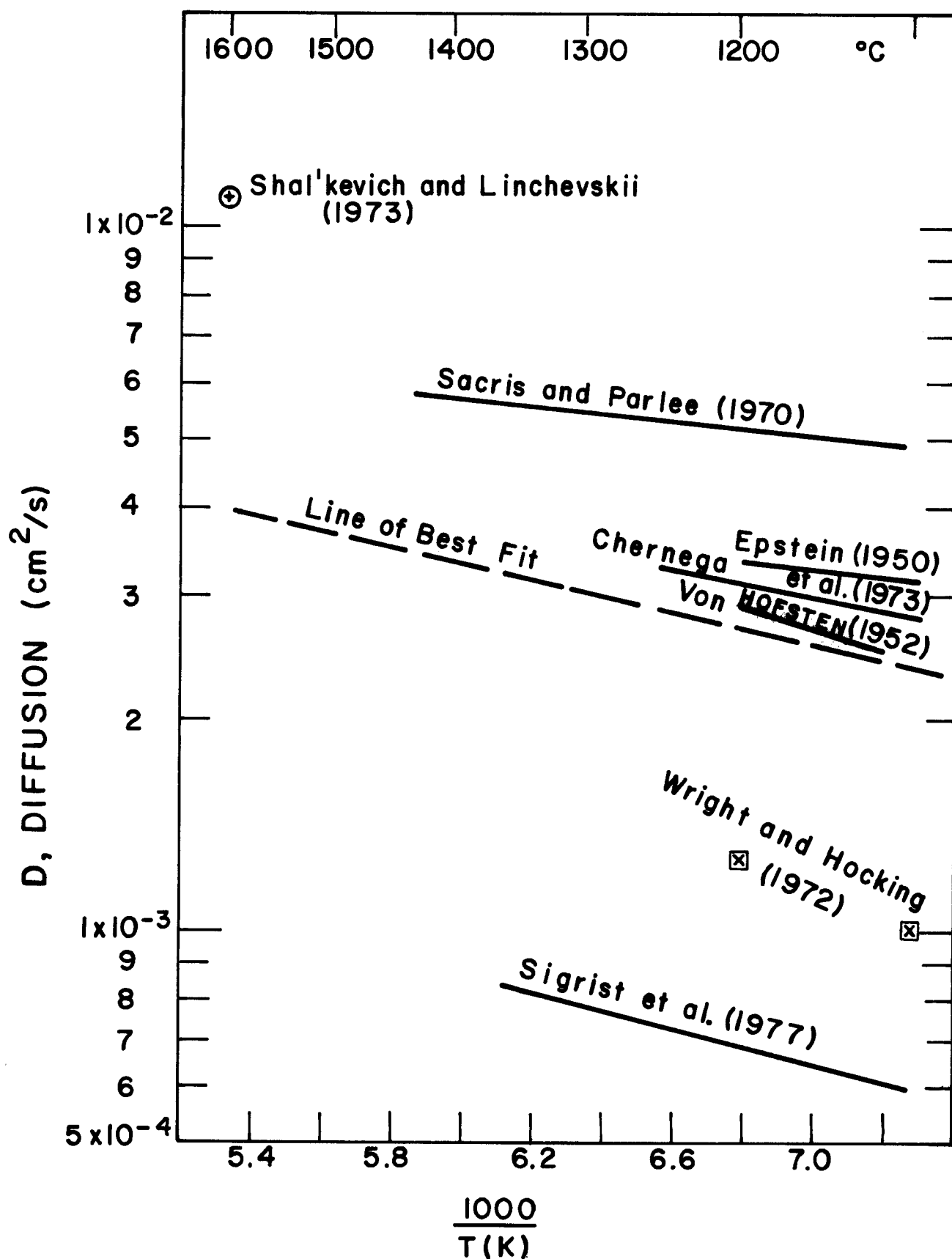


Fig. XI-C-5. The Diffusion of Hydrogen in Liquid Copper as a Function of Temperature.

Fig. XI-C-6 and XI-C-7 summarize the data on hydrogen diffusion^(34,35) and solution^(35,37) in 11 Cr-Fe. In Table XI-C-2 data⁽³⁸⁻⁴⁹⁾ for hydrogen isotope permeation in chromium-ferritic steels are given. The data for steels containing between 8 and 13% chromium are in good agreement (Figure XI-C-8). In the three graphs (Figs. XI-C-6 - XI-C-8) the dotted lines represent the "best-fit" lines drawn such that the hydrogen solution, diffusion and permeation data are consistent. To convert to tritium data the diffusion and permeation equations given in the figures must be divided by $\sqrt{3}$.⁽⁵⁰⁾

XI-C-2. Tritium Recovery

Tritium is bred at a rate of 0.49 kg/d in the lithium-lead eutectic and must be extracted at this rate. In determining the optimum steady state concentration of tritium in the breeder, two factors must be taken into consideration. A high concentration eases the requirements on the tritium extraction system; a low concentration eases the requirements of the tritium containment scheme. In WITAMIR-I, a tritium concentration of 0.012 wppm, which results in a tritium pressure of 10^{-4} torr, represents a compromise between these two conflicting considerations.

The breeding material also acts as the coolant and is thus circulated from the blanket region to heat exchangers. Such an arrangement naturally suggests diverting small quantities of the circulating $\text{Li}_{17}\text{Pb}_{83}$ to a tritium extraction unit. In Table XI-C-3, characteristics of the breeding and cooling material pertinent to the design of a tritium extraction unit utilizing a sidestream of coolant material are given. Two extraction schemes, inert gas purging and molten salt extraction, can now be discussed. The first one is the method of choice for WITAMIR-I with the second serving as an alternative scheme.

XI-C-2-a. Inert Gas Purging for Tritium Extraction

Hydrogen can be recovered from liquid metals by exposing the metal to a vacuum at a pressure less than the equilibrium hydrogen partial pressure above the metal.⁽⁵¹⁾ In the following discussion it is assumed that 1% of the primary coolant flow is diverted to the tritium extraction unit and 39% of the tritium must be recovered before returning the $\text{Li}_{17}\text{Pb}_{83}$ to the heat transfer cycle in order to recover tritium at the rate at which it is bred (5.7×10^{-6} kg/s). Thus, a vacuum of $<3.7 \times 10^{-5}$ torr must be maintained. The quantity of tritium that must be extracted from the alloy is 9.5×10^{-4} mol T_2/s . This is a volumetric flow rate of 1.2×10^6 μ/s at 500°C and 3.7×10^{-5} torr. This

HYDROGEN DIFFUSION IN 11Cr-Fe

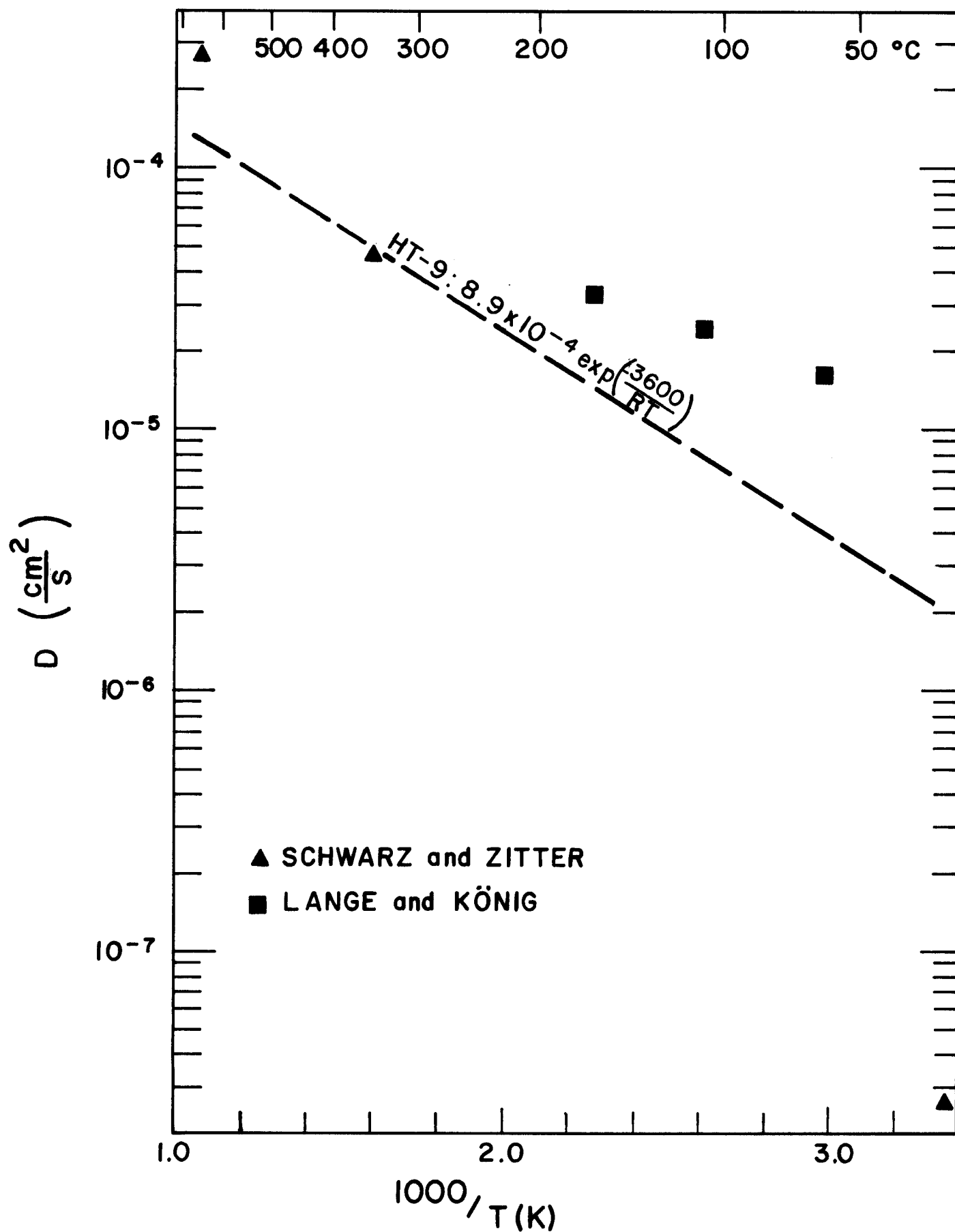


Fig. XI-C-6

HYDROGEN SOLUBILITY IN 11 Cr-Fe

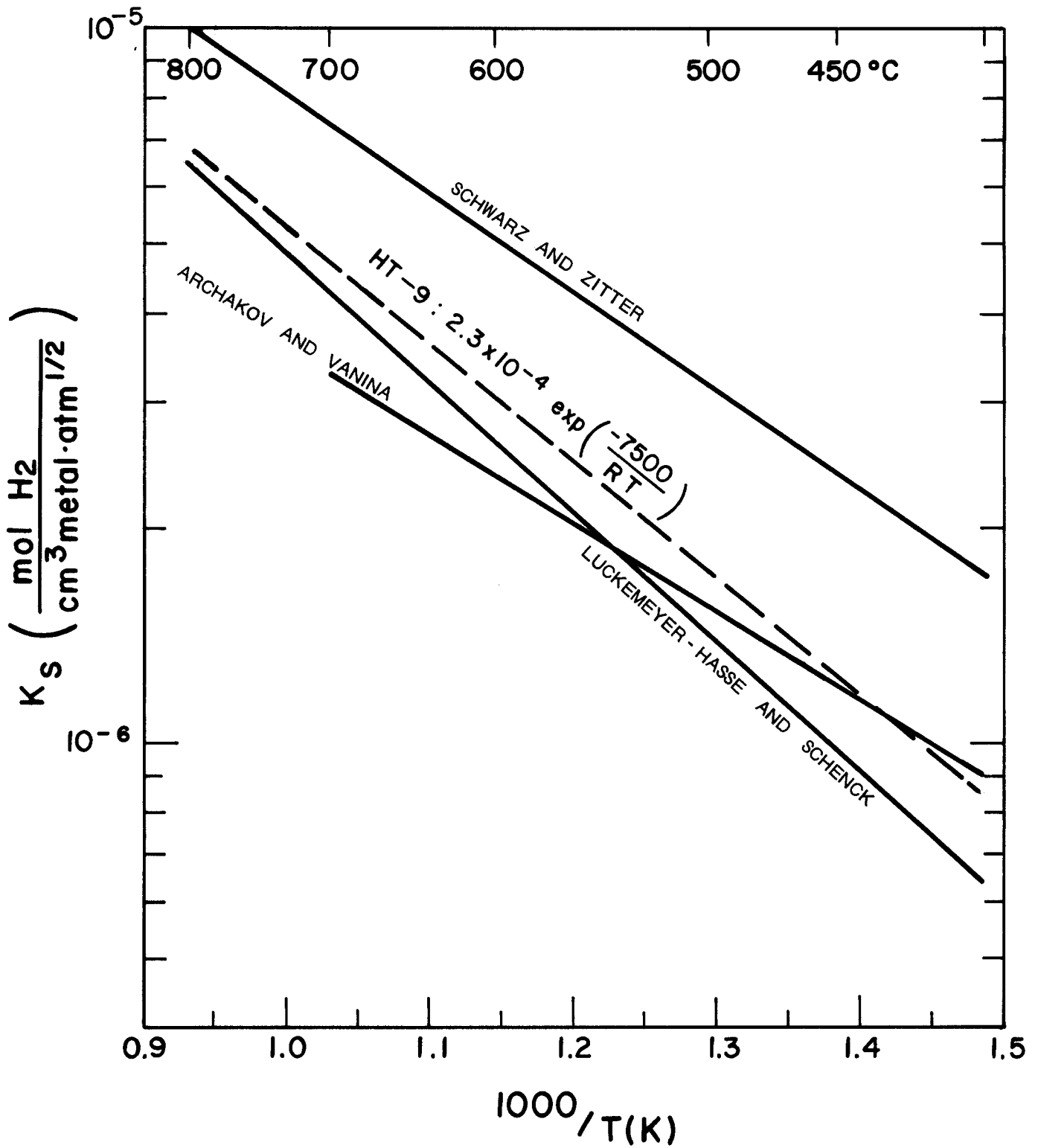


Fig. XI-C-7

TABLE XI-C-2

Hydrogen Permeation Parameters and Alloy Compositions of Chromium Ferritic Steels

<u>Alloy</u>	<u>Wt%Cr</u>	<u>Wt % Other</u>	<u>Permeability Constant</u> $\frac{\text{mol H}_2 \cdot \text{mm}}{\text{m}^2 \cdot \text{d} \cdot \text{atm}^{1/2}}$	<u>Activation Energy</u> (cal/mol)	<u>Temperature</u> $(^\circ\text{C})$	<u>Pressure</u> (atm)	<u>Ref.</u>
α -Fe	0	0	1,120	8,400	126-900	0.03-300	(38)
Fe-1.38 Cr	1.38	0.04 C, 0.49 Mn, 0.027 S, 0.33 Si	1,400	9,200	355-693	1	(39)
Croloy	2	1 Mo, 0.3 Ni	1,520*	11,210*	277-727		(40)
Croloy	2.01	0.08 C, 0.40 Mn, 0.96 Mo, 0.12 Ni, 0.014 S, 0.14 Si	944*	9,350*	400-500	3×10^{-20} - 1.7×10^{-19}	(41)
Fe-2.51 Cr	2.51	0.04 C, 0.57 Mn, 0.0265 S, 0.37 Si,	1,820	10,210	252-741	1	(39)
Fe-2.68 Cr	2.68	0.25 C, 0.90 Mn, 0.24 Mo, 0.24 Si	2,220	10,770	400-800	1	(42)
Fe-4.25 Cr	4.25	0.05 C, 0.53 Mn, 0.034 S, 0.25 Si	245	10,230	235-727	1	(39)
Kh 5	5.1	0.04 C, 0.16 Mn, 0.011 S, 0.10 Si	2,050	8,670	300-700	1	(43)
Croloy 9M	8-10	0.9 - 1.1 Mo	1,770	10,990	538-775	1	(44)
Fe-12.36 Cr	12.36	0.05 C, 0.11 Mn, 0.023 S, 0.09 Si	3,790	11,780	350-600	50-200	(45)
410 SS	12.6	0.13 C	549	9,780	309-992	1	(46)
406 SS	13		1,840*	12,200*	277-727		(40)

TABLE XI-C-2 (continued)

403 SS	13.0	0.5 Si	0.051 0.106	---	250 290	1 1	(47)
405 SS	14.5	0.1 - 0.3 Al, 1.00 Si	0.034	---	250	1	(47)
Kh-18	18.15	0.06 C, 0.16 Mn, 0.012 S, 0.17 Si	3,210	10,090	300-800	1	(43)
446 SS	25		14,080	14,580	680-1195	1.07	(48)
E Brite	26	1 Mo	2,940*	12,300*	277-727		(40)
Fe-27.45 Cr	27.45	0.06 C, 0.20 Mn	7,150	12,970	450-1100	1	(49)

*Tritium data.

HYDROGEN PERMEATION THROUGH CHROMIUM FERRITIC STEELS

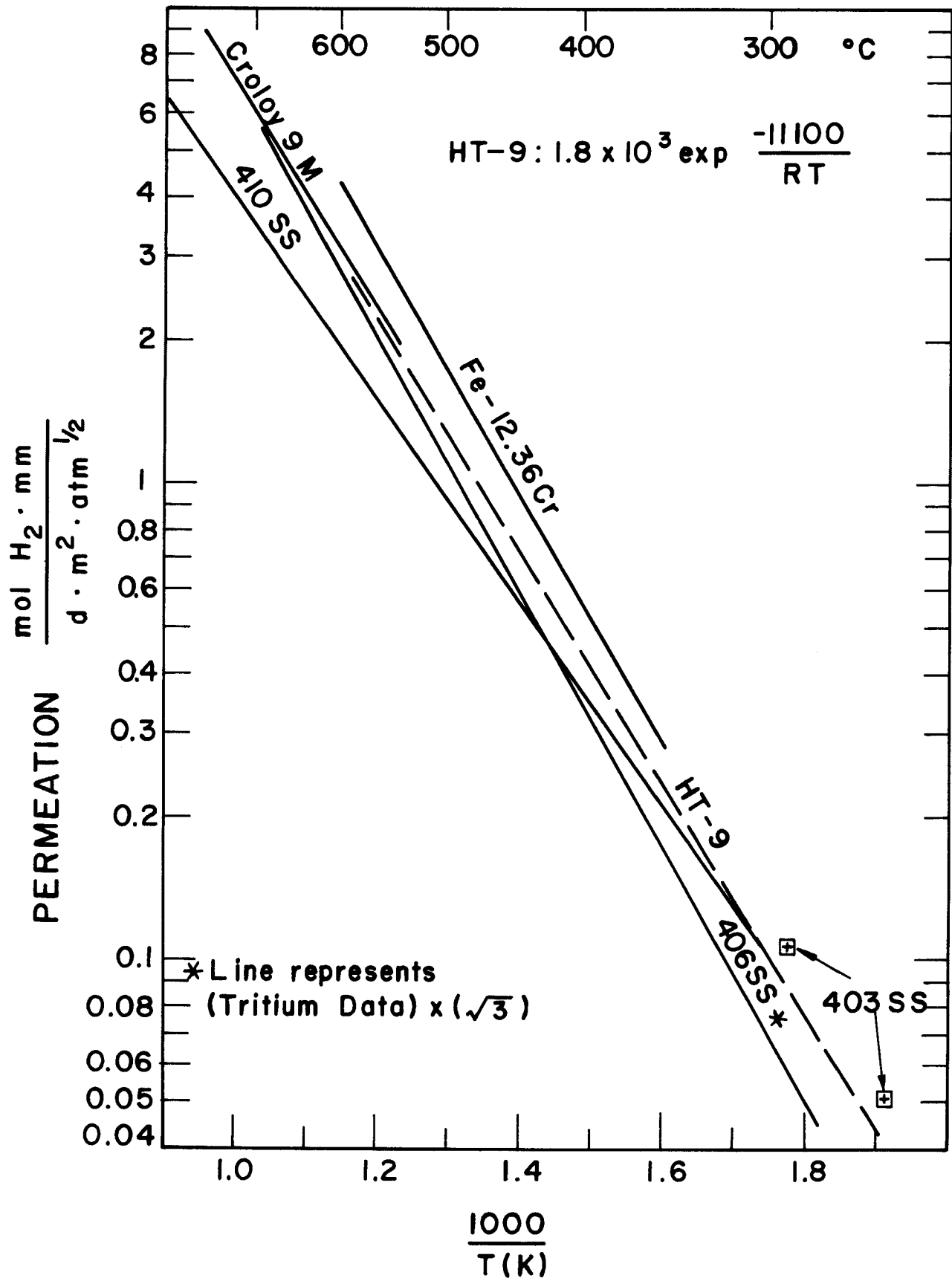


Fig. XI-C-8

Table XI-C-3
Breeder and Coolant Characteristics

Blanket Temperature (°C)	
Inlet	329
Outlet	500
Tritium Concentration (wppm)	
	0.012
Inventory (kg)	
Tritium	0.10
Li ₁₇ Pb ₈₃	8.4×10^6
Tritium Breeding	
Ratio	1.07
Rate (kg/s)	5.7×10^{-6}
Flow Rate (kg/s)	
Tritium	1.5×10^{-3}
Li ₁₇ Pb ₈₃	1.2×10^5

high flow rate at such a low pressure, while not impossible, is impractical and costly.

The pumping requirement can be relaxed by introducing inert gas bubbles, such as helium, at the bottom of the melt, while at the same time pumping on the melt's surface.^(52,53) Either of two conditions may then govern tritium removal. In the first case, the mass transfer of tritium in the purge bubble is reached during the rise of the bubble through the liquid metal. In the second case, the mass transfer of tritium from the liquid metal to the inert purge gas bubble limits the rate of tritium extraction and equilibrium conditions are not reached.

For the case where equilibrium between tritium in the liquid phase and the inert gas bubble exists, Geller⁽⁵⁴⁾ has derived the basic relation whereby the quantity of purging gas required to reduce the dissolved gas in the liquid metal to a predetermined minimum may be calculated. The equation for a purging gas of infinitesimal bubble size is given as:

$$F_g = \frac{0.00373x}{t} [PK_s^2 \left(\frac{1}{T_f} - \frac{1}{T_i} \right) + T_i - T_f]$$

where F_g is the amount [ℓ (STP)/s] of purging gas required. P , the external pressure on the alloy, is maintained at 10^{-2} torr by Roots blowers. A residence time, t , of 82 seconds for the $Li_{17}Pb_{83}$ is assumed. The other parameters in the above equation are given as follows:

- x is the amount (kg) of $Li_{17}Pb_{83}$ in the extractor = 10^5 kg,
- K_s is the solubility of tritium in $Li_{17}Pb_{83}$ = 1.2 wppm/torr^{1/2},
- T_f is the final concentration of tritium = 0.0073 wppm,
- T_i is the initial concentration of tritium = 0.012 wppm, and
- 0.00373 converts kg T to ℓ of gas.

For the above parameters, F_g must be 3.5 ℓ /s at STP. This is equivalent to 2.9×10^5 ℓ /s at 10^{-2} torr and 300 K. This is a relatively large, but not infeasible pumping speed.

A calculation was made to check that the extraction process was indeed equilibrium, and not mass transfer, controlled. Pehlke⁽⁵⁵⁾ gives the expression for such a case as:

$$\ln y = - \left(\frac{DA_s}{\delta} + \frac{3000 k_T F_g h}{r_b v_t} \right) \frac{t}{V_m}$$

where y is the fraction of tritium remaining in the $\text{Li}_{17}\text{Pb}_{83}$, 0.61, after time t , 82 s, in the extractor.

D is the diffusion coefficient of tritium in $\text{Li}_{17}\text{Pb}_{83} = 10^{-3} \text{ cm}^2/\text{s}$,

A_s is the surface area of the melt $= 2.2 \times 10^4 \text{ cm}^2$,

δ is the boundary layer thickness $= 10^{-3} \text{ cm}$,

F_g is the flow rate of the purge gas at 773 K and 1 atm $= 9.9 \text{ l/s}$,

h is the height of the melt above the exit of the bubble from the delivery mechanism $= 500 \text{ cm}$,

r_b is the bubble radius $= 0.2 \text{ cm}$,

v_t is the terminal rise velocity of a bubble in the melt $= 15 \text{ cm/s}$,

V_m is the volume of $\text{Li}_{17}\text{Pb}_{83}$ in the extractor $= 1.1 \times 10^7 \text{ cm}^3$, and

k_T is the mass transfer coefficient.

Of the above parameters, A_s , δ , h , r_b and v_t require further elaboration as to how they were estimated. The surface area, A_s , and height, h , were obtained assuming a tank radius of 82 cm and eutectic depth of 500 cm. The boundary layer thickness, δ , is in the region of 4×10^{-4} to $2 \times 10^{-3} \text{ cm}$ for liquid lead at 700°C ⁽⁵⁶⁾ and is estimated to be $1 \times 10^{-3} \text{ cm}$ here. The bubble radius, r_b , is assumed to be twice the radius of the orifice,⁽⁵⁵⁾ $\sim 0.1 \text{ cm}$, through which the gas is delivered to the melt. The terminal rise velocity, v_t , is equal to $c(gr_b)^{1/2}$ where g is the acceleration due to gravity and c is 2.54⁽⁵⁵⁾ and 1.15⁽⁵⁷⁾ for bubbles in liquid aluminum and steel, respectively, and is estimated to be ~ 1 for $\text{Li}_{17}\text{Pb}_{83}$. For the above conditions, the mass transfer coefficient must be $> 9 \times 10^{-3} \text{ cm/s}$ for equilibrium conditions to prevail in this degassing scheme. Generally, transfer coefficients higher than this are found. A value of $3.9 \times 10^{-2} \text{ cm/s}$ for hydrogen in liquid aluminum at 700°C ⁽⁵⁵⁾ and of 1 cm/s for hydrogen in liquid steel at 1600°C ⁽⁵³⁾ give an idea of the order of magnitude for the hydrogen mass transfer coefficient in liquid metals. Therefore, it appears equilibrium conditions will prevail.

Roots blowers, sometimes called "mechanical booster pumps," are used in WITAMIR-I to maintain the surface of the alloy at 10^{-2} torr. The lithium and lead vaporized must be condensed on cooled baffles before they reach the pumps. The quantity of lithium and lead condensed on the baffles, drained and reunited with the alloy can be roughly approximated by multiplying the moles of T_2 removed per day (82) by the ratio of lithium and lead pressures (Table

IX-C-2) to the final tritium pressure (3.7×10^{-5} torr). This calculation gives 0.82 moles of lithium (6 g) and 31 moles of lead (6,400 g) per day.

The vacuum pumping system is designed with primary blowers to raise the pressure from that of the extraction unit (10^{-2} torr) to atmospheric pressure. The inert gas : tritium (3.5 μ /s : 0.021 μ /s) stream is passed over molecular sieve beds at 75 K to freeze out any tritiated water and tritium-free compounds.

The purified helium : tritium stream is then sent to uranium beds where the tritium is gettered at room temperature. Experiments⁽⁵⁸⁾ have shown that uranium traps can effectively remove deuterium (0.5 to 4.0 vol %) from flowing argon streams with purification factors (concentration of deuterium into trap $\div$ concentration of deuterium out of trap) exceeding 100. The traps used in WITAMIR-I are of a similar geometry to the Lawrence Livermore developmental flow-through uranium trap or dual-flow uranium trap.⁽⁵⁹⁾ Extrapolating from the experiments on argon : deuterium streams in the developmental flow-through traps⁽⁵⁸⁾ yields the conclusion that a trap containing 10 kg of uranium could handle 0.88 μ /s of helium : tritium and maintain an inert gas purification factor of 100 for T/U atomic ratios of less than 0.33. When the trap composition reaches $UT_{0.33}$ it must be regenerated at 375-400°C.⁽⁵⁹⁾

WITAMIR-I utilizes 5 of the above 10 kg uranium traps. Four are on line in parallel to handle the 3.5 μ /s of inert purge gas while the fifth is being regenerated. The trap on-line time is 8 hours, with a regeneration time of only 2 hours. The kinetics of the decomposition of uranium hydride^(60,61) and deuteride⁽⁶²⁾ have been studied and found to follow a zero order rate process. These data indicate that the detritiding of 10.04 kg of $UT_{0.33}$ should occur in a matter of minutes at 375°C so that the 2 hour regeneration times assumed in WITAMIR-I are easily achievable. The tritium (and any protium) recovered from the regeneration of the uranium beds is then merged with the hydrogen isotopes from the exhaust in the fuel reprocessing system (Section XI-B and Fig. XI-A-1).

XI-C-2-b. Molten Salt Extraction - Alternate Tritium Recovery Method

The possibility exists of extracting tritium from $Li_{17}Pb_{83}$ with a molten alkali halide salt, such as LiF-LiCl-LiBr(22-31-47 mol %). This process has shown considerable promise for removing hydrogen isotopes at the ppm level from liquid lithium.^(63,64) In this method, liquid lithium and a molten salt are contacted and the LiH is preferentially distributed in the salt phase.

The salt is then electrolyzed to yield H_2 at an emf of ~ 0.9 volts, a voltage which is below that of the decomposition potential of the salt (>2 volts). The hydrogen is recovered from the melt by sweeping the porous stainless steel hydrogen electrode with a circulating stream of argon and subsequently trapping the tritium from the argon with a getter.⁽⁶³⁾

It has been estimated that the ratio of the tritium content in the salt to the tritium content in the lithium material will be 50 to 100 times more favorable for $Li_{17}Pb_{83}$ than for pure lithium.⁽⁶⁵⁾ Since the volumetric distribution coefficient,

$$D_v = \frac{(\text{wppm impurity in salt}) \times (\text{density of salt})}{(\text{wppm impurity in lithium material}) \times (\text{density of lithium material})},$$

has been found to be 3.1 for protium and 3.9 for deuterium in liquid lithium,⁽⁶⁴⁾ a volumetric distribution coefficient of 200 is assumed for tritium between $LiF-LiCl-LiBr$ and $Li_{17}Pb_{83}$.

The quantity of the blanket processed per unit time, X , is given by:⁽⁶³⁾

$$X = \left(\frac{R_b}{I_{ss}} \cdot \frac{\epsilon D_v \eta + 1}{\epsilon D_v \eta} \right) I_{LP}$$

where R_b = tritium breeding rate,
 ϵ = efficiency of tritium recovery from the salt,
 η = efficiency factor which accounts for nonequilibrium tritium distribution during contacting,
 I_{ss} = steady-state tritium inventory, and
 I_{LP} = $Li_{17}Pb_{83}$ inventory.

It has generally been assumed^(63,66) that v is equal to unity, yielding $D_v \eta = 200$; X is almost independent of ϵ for values of $\epsilon > 0.05$. Therefore, high values of ϵ do little to minimize the quantity of $Li_{17}Pb_{83}$ processed. However, there is an impetus towards low values of ϵ because the average current density in the electrolysis tanks increases as the recovery efficiency, ϵ , decreases. At high current densities the total electrode area, size of the electrolysis tanks and quantity of salt is smaller than at the lower current densities.⁽⁶³⁾

Table XI-C-4 illustrates the molten salt extraction scheme within the WITAMIR-I constraints for $\epsilon = 0.90$ and $\epsilon = 0.30$. Data given by Calaway⁽⁶³⁾ show that electrolysis must be carried out for 30 minutes at an average density of $\sim 8 \text{ A/m}^2$ to recover 90% of the tritium at an initial hydride concentration of 1 wppm (corresponding to a tritide concentration of 3 wppm) in the salt. The data indicate that to recover only 39% of the tritium requires 5 minutes of electrolysis at 30 A/m^2 for an initial tritide concentration of 3 wppm. Since the average current density is proportional to the tritium concentration, the density needed under the WITAMIR-I constraints are those densities given above multiplied by the ratio of the initial WITAMIR-I tritide concentration to 3 wppm. Table XI-C-4 contains the assumption that the contactor units are high-capacity annular centrifugal contactors (25 cm diameter by 45 cm in height) capable of processing 23,000 liters of fluid ($\text{Li}_{17}\text{Pb}_{83}$ plus salt) per hour.⁽⁶⁶⁾ It is also assumed that the electrolysis units are modular in design with each unit processing no more than one minute's quantity of effluent salt from the contactors. Thus, the electrolysis is a batch operation but the salt inventory and electrode area are not significantly higher than would be the case for a continuous process. The conclusion to be drawn from Table XI-C-4 is that operating the molten salt extraction system at a low value of ϵ is preferred. Since some of the parameters of this extraction scheme are either unknown (η), or known poorly (D_v), it is not reasonable to optimize the system any further. Thus, WITAMIR-I operates at a recovery efficiency of 30%.

In the experiments performed by Calaway,⁽⁶³⁾ the flow rate of argon dispersed through the salt by the hollow porous hydrogen electrode was $\sim 0.01 \text{ l/s}$. (This argon removed hydrogen as H_2 after the hydride ion was electrochemically oxidized.) The quantity of hydrogen liberated in a 5 minute period was $4 \times 10^{-7} \text{ kg}$ (equivalent to $1 \times 10^{-6} \text{ kg}$ tritium). Scaling this up to the WITAMIR-I parameters would suggest an argon flow rate of roughly 15 l/s , containing a tritium concentration of about 0.14 vol %. As in the inert gas purge scheme, uranium beds would getter the tritium from the inert gas.

A potentially severe problem with the molten salt extraction scheme is the solution of LiF , LiCl and LiBr in $\text{Li}_{17}\text{Pb}_{83}$. These dissolved halides, if transported back to the blanket region, would adversely affect both the corrosion characteristics and radioactivity of $\text{Li}_{17}\text{Pb}_{83}$. The extent of solution of halides in $\text{Li}_{17}\text{Pb}_{83}$ is not known at this time, though data on other molten

Table XI-C-4
Molten-Salt Extraction Unit Parameters for Various Values of the
Efficiency of Tritium Recovery from the Salt (ϵ)*

	<u>$\epsilon = 0.30$</u>	<u>$\epsilon = 0.90$</u>
CONTACTOR UNIT		
Li ₁₇ Pb ₈₃ Processed (fraction/s)	0.0000574	0.0000568
Flow Rate (kg/s)**		
Li ₁₇ Pb ₈₃	482	477
Salt	114	112
Tritium Concentration (wppm)		
Entering Contactor		
Li ₁₇ Pb ₈₃	0.012	0.012
Salt	0.07	0.0056
Leaving Contactor		
Li ₁₇ Pb ₈₃	0.00020	0.000066
Salt	0.12	0.056
Inventories (kg)		
Li ₁₇ Pb ₈₃	1,700	1,700
Salt	400	390
Tritium	0.0001	0.00002
Number of Contactor Units	17	17
Total Electrical Power (kW)	63	63
ELECTROLYSIS UNIT		
Salt Residence Time (s)	300	1800
Inventories (kg)		
Salt	48,000	220,000
Tritium	0.0067	0.0067
Current Density (A/m ²)	1.7	0.30
Electrode Area (m ²)	130	630
Electrolysis Power (kW)	0.2	0.2

*The extraction system temperature is 500°C.

**The densities of Li₁₇Pb₈₃ and salt are 9.4 and 2.2 g/cm³, respectively. Thus, the volumetric flow rate of each material is 52 ℓ /s for $\epsilon = 0.30$ and 51 ℓ /s for $\epsilon = 0.90$.

salt-alloy systems, summarized in Section X-B-4 of NUWMAK,⁽⁶⁷⁾ suggest that the problem may be trivial. Weston et al.⁽⁶⁴⁾ have proposed including a specially designed cold trap to recover halides from the lithium breeding material before the lithium is returned to the blanket. In light of the potential problems with halide buildup in the blanket, the "clean" inert gas purge tritium recovery scheme is the extraction method of choice in WITAMIR-I.

XI-D. Tritium Containment Systems

The total tritium inventory in WITAMIR-I is 2.8 kg or 2.7×10^7 Ci. A design goal is to limit tritium losses to the environment to ~ 10 Ci/d. Tritium is being processed at a rate of 2.1×10^7 Ci/d (1.6×10^7 Ci from the exhaust and 4.8×10^6 Ci from the breeder), thus the tritium losses must be limited to one part in two million. This is below current operating practice (one part in five thousand⁽⁶⁸⁾). However, the Tritium Systems Test Assembly (TSTA), which will simulate the reactor fuel and exhaust cycle of a magnetic fusion reactor, is expected to process 1×10^7 Ci/d with tritium losses confined to 200 Ci/yr.⁽⁶⁹⁾ If the TSTA operates as expected, then losses from the fueling and exhaust cycle of WITAMIR-I can be expected to be limited to < 1 Ci/d by using containment schemes similar to those found both in the TSTA and in a previous conceptual reactor design.⁽⁸⁾

In general, tritium control is achieved by a three stage system. Primary containment consists of the walls of the components which are in direct contact with tritium. Secondary containment is provided by local enclosures surrounding major tritium handling equipment, such as double-walled piping and components, gloveboxes or other large volumes housing tritium-wetted components and integral vacuum jackets around cryogenic components. Tertiary containment is considered to be the reactor hall, rooms containing tritium processing equipment and the reactor building itself. The tertiary containment system includes the tritium effluent removal system and the emergency tritium containment system. The tritium effluent removal system is designed to operate on routine tritium losses while the emergency tritium containment system is only used under off-normal conditions. Further details of these systems can be found in the literature.^(5,6,8,69)

The tritium escape route which appears to be the most difficult to control is permeation from the $\text{Li}_{17}\text{Pb}_{83}$ breeder/coolant through the walls of the steam generator to the steam. Once tritium diffuses into the steam, it is

isotopically diluted to such an extent that separation is difficult with current techniques, and it is here considered lost to the environment.

The steam generator tubes are of double-walled construction (Chapter XIII). The annulus is purged with helium which serves to sweep the tritium that permeates the first tube wall to a tritium recovery unit. The discussion that follows first assumes that all the metal surfaces are clean and the tritium permeability is characterized by the intrinsic permeability of the alloy; this should result in a calculated value for the tritium permeation rate that is higher than the actual one expected under the WITAMIR-I operating conditions. Next, the following discussion takes into account the expected metal surface conditions to arrive at a more realistic value for the expected tritium loss.

The equation given in Fig. XI-C-8 for tritium permeation, P , through clean HT-9 is:

$$P = \frac{1.8 \times 10^3}{\sqrt{3}} \exp \left(- \frac{11100}{RT} \right) \frac{\text{mol } T_2 \cdot \text{mm}}{d \cdot \text{m}^2 \cdot \text{atm}^{1/2}} \quad .$$

The WITAMIR-I parameters needed to estimate the tritium permeation rate from the $\text{Li}_{17}\text{Pb}_{83}$ to the annulus of the tube are a tritium pressure above the breeder of 1×10^{-4} torr, a steam generator tube area of $1.6 \times 10^4 \text{ m}^2$, a tube wall thickness of 1 mm and an average tube temperature of 400°C (673 K). The above equation then yields a permeation rate of 9 g/d (87,000 Ci/d) through clean tube surfaces. The discussion in the following paragraph concerning the tritium loss from the annulus to the steam requires a permeation rate of a factor of 9 less than this. Methods of achieving lower losses to the annulus include coating the surface facing the $\text{Li}_{17}\text{Pb}_{83}$ with a liquid metal corrosion resistant, hydrogen permeation resistant aluminide layer,⁽⁷⁰⁾ coating the annulus surfaces with phosphoric acid⁽⁷⁰⁾ or oxidizing the annulus with steam.^(41,71) Experiments on the effects of in situ steam-formed oxide coatings on the tritium permeation rate through ferritic steels are given in Table XI-C-5. Since the purge gas is an oxidizing one, the surfaces should remain in an oxidized condition. Thus, it seems possible to limit the tritium permeation rate to the annulus to 1 g/d.

Tritium diffusion through the first wall of the steam generator is then estimated to be 1 g/d, or $1.5 \times 10^{10} \text{ T/cm}^2\text{-sec}$. To prevent this tritium from entering the cooling water, it has to be swept away from the gap by a purge

Table XI-C-5. Permeation Barrier Factors of Ferritic and Austenitic Steels Produced by Steam Oxidation of the Downstream Side

	Ferritic <u>Fe-2-1/4 Cr-1 Mo</u>	Ferritic <u>SS 406</u>	Austenitic <u>SS 316</u>
Reference	(41)	(71)	(71)
Temperature (°C)	472	660	660
Steam Pressure (atm)	2	0.94	0.94
Factors by Which Permeabilities are Reduced			
0.33 d	10	13	---
1 d	25	72	1.6
6 d	100	144	2.8
24 d	170	291	3.8
40 d		434	4.3

gas. To maintain a nominal purge gas flow rate, while minimizing tritium diffusion to the water side, a low partial pressure of oxygen, assumed to be 1 torr, is kept in the purge gas flow to convert T_2 to HTO, where H comes from diffusion from the water side. The tritium diffusion to the water side can thus be minimized. The HTO can then be condensed out on a cold surface and sent to the fuel cleanup unit (Section XI-B-2-c) where it is electrolyzed.

A set of parameters for tritium diffusion calculations is summarized in Table XI-C-6. This calculation is not optimized. However, the purge gas velocity, volumetric flow rate, and tritium loss rate all appear to be acceptable. The oxidation of the tritium to HTO significantly reduces the free tritium partial pressure to $\sim 10^{-16}$ torr. Therefore, obtaining a low diffusion driving force is the most effective method of reducing tritium diffusion.

Table XI-C-6. Summary of Tritium Diffusion Calculations
(in the Steam Generator)

Total tritium diffusion from the LiPb side	1 g/day
Steam generator surface area	$1.6 \times 10^4 \text{ cm}^2$
Tritium diffusion rate	$1.5 \times 10^{10} \text{ T/cm}^2\text{-sec}$
Oxygen partial pressure in the purge gas	1 torr
Maximum HTO partial pressure	.1 torr
Average temperature	400°C
Purge gas velocity	$2 \times 10^{-3} \text{ cm/sec}$
Purge gas channel width	.0025 cm
Purge gas channel length	1 cm
Required purge gas flow rate	1.5 liter/sec
HT partial pressure in the gap	$1 \times 10^{-16} \text{ torr}$
Total tritium diffusion to water side	10^{-2} Ci/day

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XII. Assembly and Maintenance

XII-A. Introduction and Maintenance Philosophy

The tandem mirror concept employs the positive electrostatic potential of two standard mirrors to plug the end losses from a long solenoid. This solenoid, hereafter referred to as the central cell, produces essentially all the fusion power, and is the region which is subjected to the highest neutron flux and consequently, the highest radiation damage.

The central cell in a tandem mirror consists of a long cylindrical blanket and shield region surrounded by discrete solenoids uniformly spaced. In the case of WITAMIR-I, the blanket is comprised of several banks of seamless tubes running from the top of the reactor to the bottom. A high lead content $\text{Li}_{17}\text{Pb}_{83}$ eutectic is circulated through the blanket to remove the nuclear and surface heating, and to breed tritium. A single set of supply and return headers is needed for each module. Fig. XII-A-1 is a cross section of the central cell showing the blanket.

There are 33 blanket modules in the central cell, each 463 cm long. They are supported on rails fixed to the reflector part of the shield and are capable of being translated axially within it, as can be seen in Fig. XII-A-1. This feature will be relied on in the plans for the maintainability of the reactor blanket. Routine blanket module changeout will constitute the bulk of the maintainability within the central cell. However, provision will also be made for maintaining the central cell coils and the shield.

In order to avoid making seals between adjacent blanket modules, we have decided to adopt the idea introduced in NUWMAK⁽¹⁾ which has since caught on in several other designs, namely that of providing a vacuum seal at the back of the shield. There are several possibilities for implementing such an idea. It is possible to have two seals in series with the space between pumped out. It is also possible to contain the central cell within an evacuated tunnel in which the pressure need not be any lower than ~70 torr. In this design we chose to have the central cell tunnel evacuated. The vacuum system needed here doubled as the emergency detritiation system which can be used in the event of a T_2 release within the reactor building, and therefore, does not add substantially to the cost of the reactor.

The general philosophy for maintaining the various elements of the central cell is to provide access at predetermined locations along the cell. Access hatches in the roof of the tunnel dedicated for servicing blanket

CROSS SECTION OF WITAMIR I CENTRAL CELL

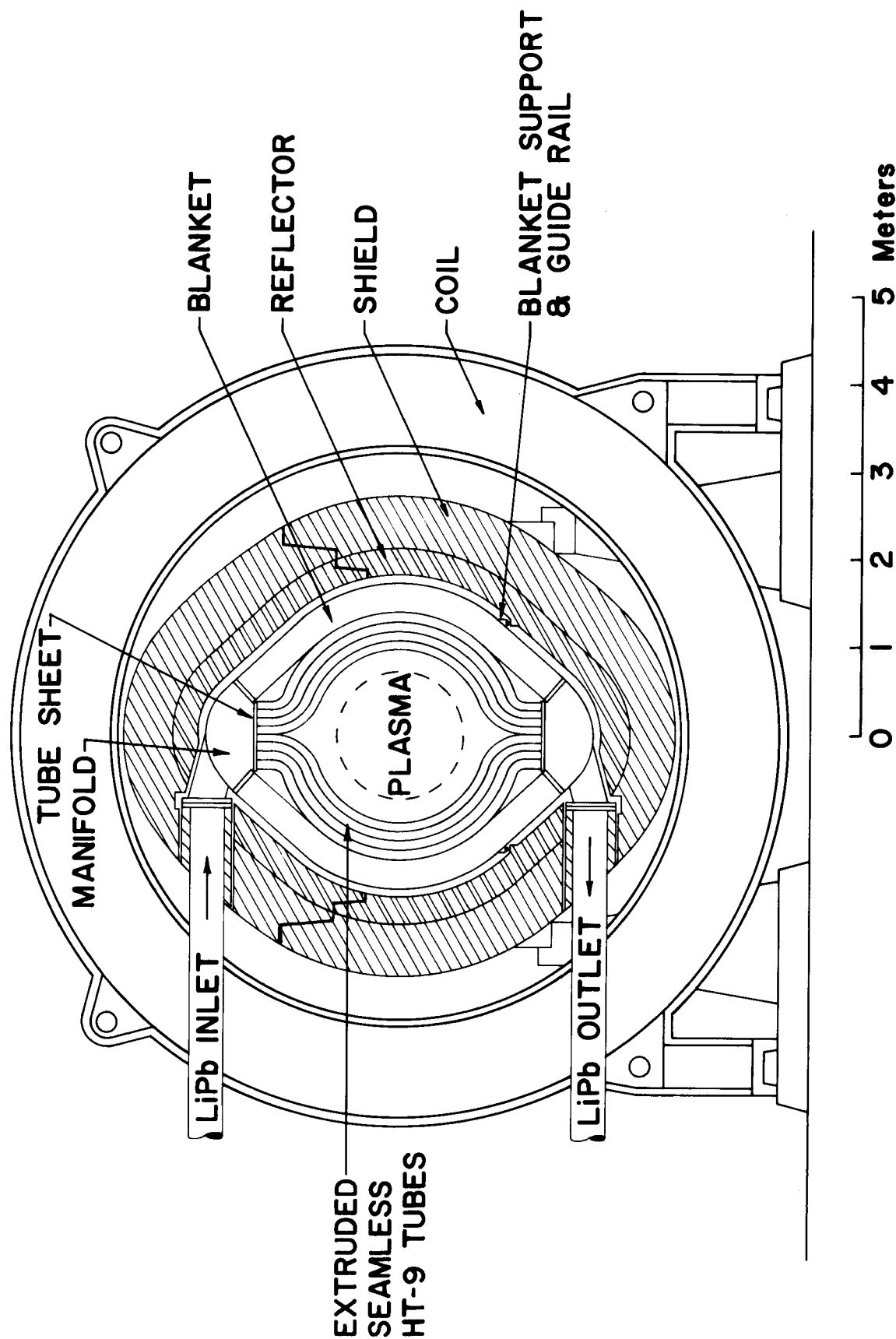


Fig. XII-A-1

modules will be spaced ~23 m apart such that they will coincide with every fifth blanket module. Thus, there will be seven access hatches used for servicing and maintaining the blanket modules. Five blanket modules will be capable of being maintained from a single access hatch, as shown in Fig. XII-A-2. To make it possible to extract a full sized blanket module between central cell coils, it is necessary to displace the two coils immediately below the access hatch apart the width of one coil. To do this, enough flexibility has to be built into the service lines for the coils such as the liquid helium supply line and the vapor exhaust line. Once the coils have been separated, the upper part of the shield/reflector can be unbolted and lifted out through the hatch. As soon as the breeding/cooling material is drained from the blanket module, the supply and return lines are disconnected and the module is allowed to cool down, it too can be lifted out through the access hatch. Figure XII-A-3 shows a blanket module being removed from the reactor. Other blanket modules in either direction can now be translated axially to the extraction point and removed in the same way.

It is envisaged that a 500 tonne gantry crane capable of handling the heaviest component in the reactor will travel along the top of the tunnel as shown in Fig. XII-A-4. Positioning itself over an access hatch, the crane manipulators will unfasten the seal on the hatch door and remove it, thus gaining access to the central cell. The capacity of the crane is dictated by the mass of the barrier coil which weighs ~ 430 tonnes.

In the following sections we will discuss the maintenance procedures for the blanket modules, the central cell coils and the shield, and maintenance of the end plug components. Emphasis will be made on routine blanket maintenance.

XII-B. Blanket Maintenance

XII-B-1. Routine Blanket Maintenance

Although initial indications are that the martensitic alloy HT-9 has superior radiation damage characteristics to 20 CW 316 stainless steel, the blanket modules will, nevertheless, have to be replaced at least once in the projected 30-year lifetime of a reactor. The downtime needed to replace the blanket modules has to be minimized in order to keep the cost of electricity down. Furthermore, it may be beneficial to replace a fraction of the blanket modules on a annual or bi-annual basis in such a way that all the modules will have been replaced at the projected end of life for the blanket material.

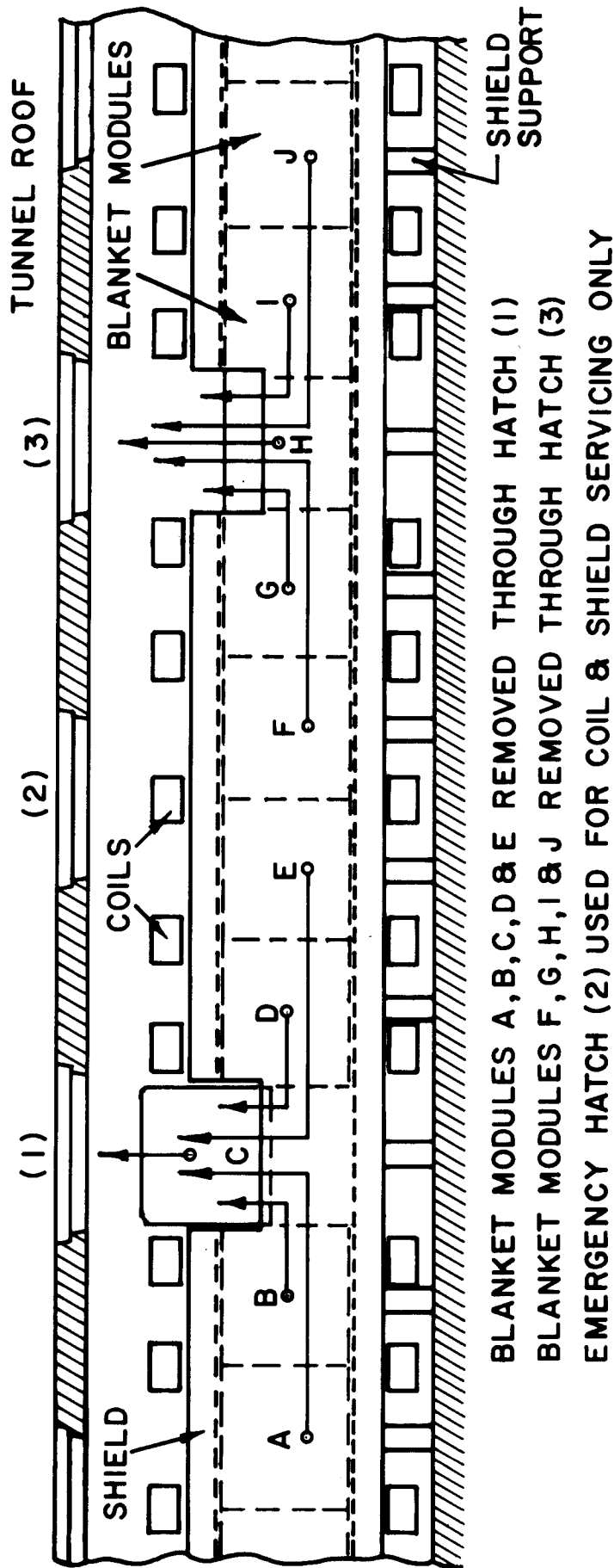
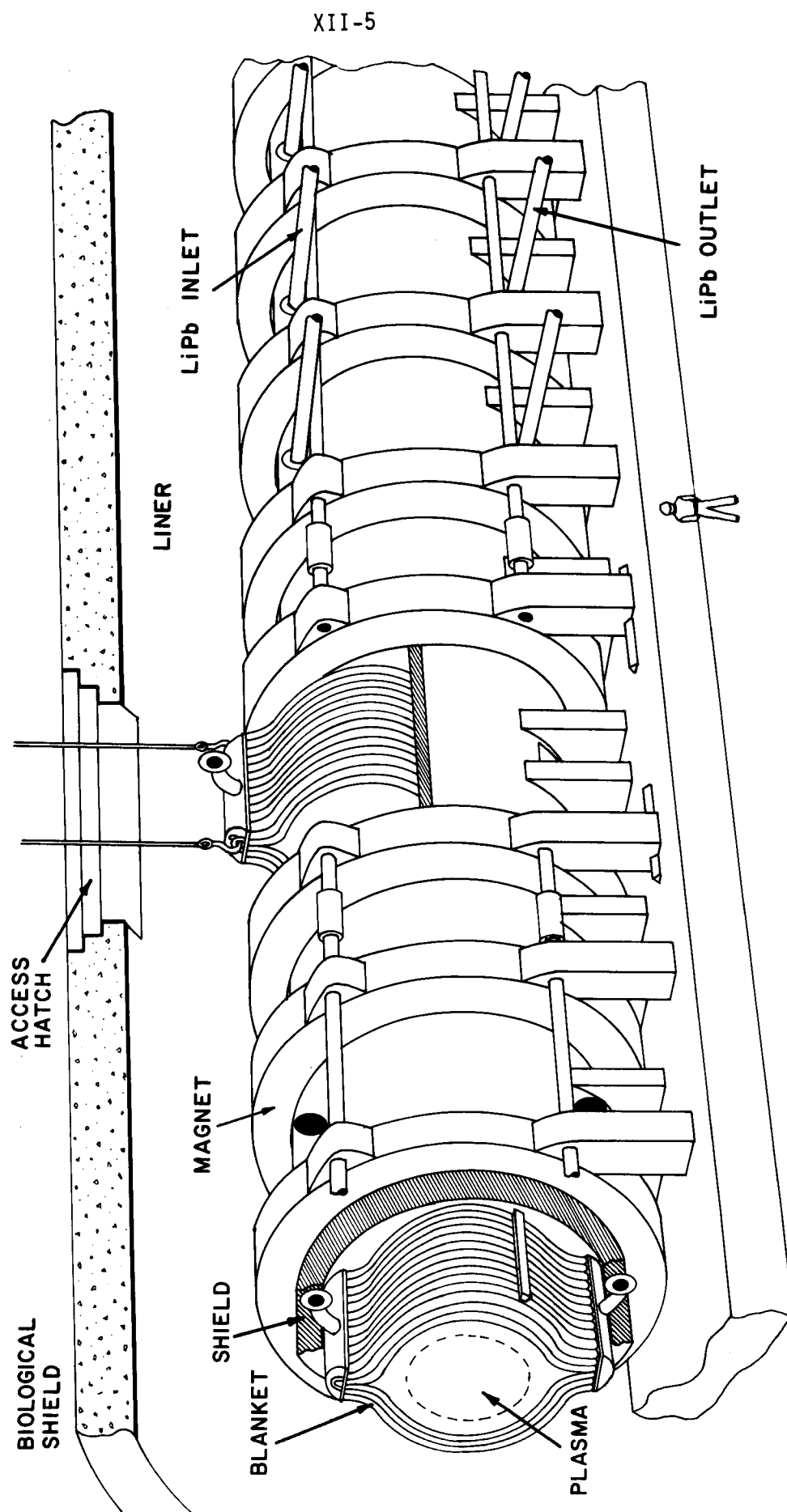


Fig. XII-A-2. Section of the WITAMIR-I central cell tunnel showing the blanket removal scheme.

Fig. XII-A-3

ISOMETRIC VIEW OF WITAMIR I
CENTRAL CELL



PERSPECTIVE VIEW OF WITAMIR I
A WISCONSIN CONCEPTUAL TANDEM MIRROR
FUSION POWER REACTOR DESIGN

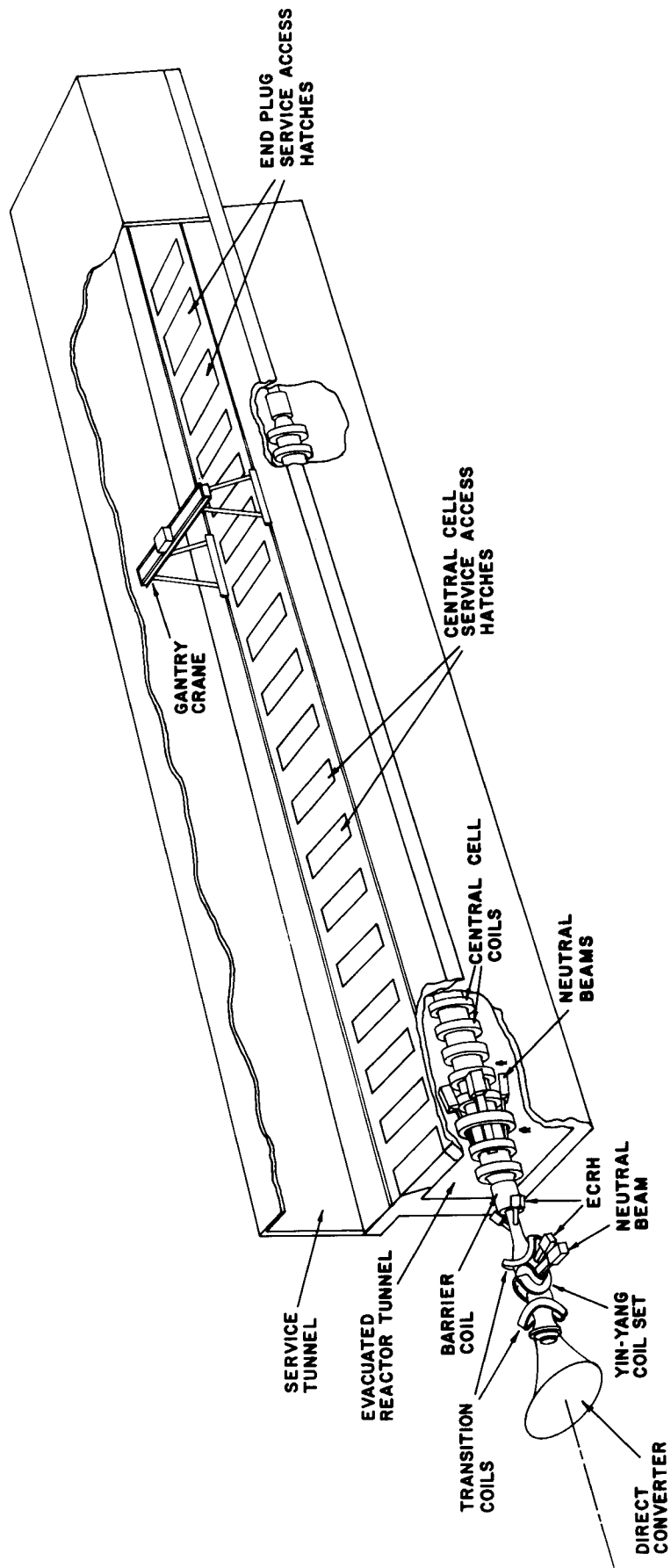


Fig. XII-A-4

Thus, for example, if for a given neutron wall loading the projected blanket material lifetime is 9 years, then one-third of the modules can be replaced every three years.

The four most difficult problems that have to be addressed in extracting and replacing blanket modules in a reactor are:

- 1 - Providing adequate accessibility
- 2 - Disconnecting and connecting coolant lines
- 3 - Making vacuum joints between adjacent blanket modules
- 4 - Moving large awkward objects (the blanket modules) which are sometimes quite heavy.

Each one of these questions will be addressed separately and the unique solution adopted for WITAMIR-I will be discussed.

XII-B-2. Providing Adequate Accessibility

This is one area in which the tandem mirror has a clear advantage over tokamaks because of its simple geometry in the central cell. As we have mentioned earlier, the full length of the central cell will be accessible through discrete hatches in the roof of the tunnel. There will be seven such hatches at ~ 23 m intervals, with each hatch dedicated to servicing five blanket modules (four modules will be serviced by each of the end hatches). An additional eight hatches will be available for maintenance of the shield and central cell coils as will be discussed later.

Aside from the obvious steps, such as emptying the blanket modules of breeding material and allowing them to cool down, the following additional steps have to be made in order to provide access to the blanket modules:

a. The supply header to the blanket module immediately below the access hatch has to be disconnected and removed.

b. The two central cell coils immediately below the access hatch are separated from each other the width of a coil. This is accomplished by actuating pneumatic or hydraulic cylinders which are connected to the coils in four locations as shown in Figs. XII-A-1 and XII-A-3. The coils ride on rails permanently fixed to the floor of the tunnel. Since the coils do not contact the blanket or shield at any point, there would be no problem in moving them. Although the current in the coils will have been discharged, they will be maintained at cryogenic temperatures.

c. The top half of the shield with the reflector attached to it will be removed next. Since the outer surface of the shield serves as a secondary

vacuum barrier, it will be necessary to undo the clamps or bolts to disengage the seal. The upper half of a shield module with the attached reflector weighs on the order of 150 tonnes. It will be lifted through the access hatch and stored in the service tunnel for the duration of the blanket replacement operation. The first blanket module is now completely exposed and can be extracted from the reactor (see Fig. XII-A-3).

XII-B-3. Disconnecting and Connecting Coolant Lines

The breeding/cooling material chosen for WITAMIR-I, $\text{Li}_{17}\text{Pb}_{83}$, is an excellent solder. We feel we should take advantage of this fact in designing the means for connecting and disconnecting the coolant lines. The plan is to make a coupling with a wide enough flange such that the $\text{Li}_{17}\text{Pb}_{83}$ will form a frozen barrier at the outer periphery. In effect, the flange is a fin which is not insulated and thus will have a cold outer edge. The header itself will have trace heaters on it which will be used to preheat it before charging with the $\text{Li}_{17}\text{Pb}_{83}$. There will be a heater on the flange edge which will be turned on only when the seal has to be melted.

In a practical sense, for this scheme to work entirely automatically by remote control, the following sequence of events will have to take place:

Disconnecting a Header

1. The breeding/cooling material is drained from the blanket.
2. The heater on the flanges is turned on, melting the frozen $\text{Li}_{17}\text{Pb}_{83}$ zone.
3. A mechanical latch is actuated which releases the pressure between the two flange faces and disengages them.
4. The header is withdrawn horizontally to provide clearance for the axial translation of the blanket modules.

Connecting a Header

1. The blanket module is moved into place and located correctly with optical instruments.
2. The header is inserted horizontally and guided for proper mating of the connector with a conical surface which is built into the flanges. The flange on the module side will be appropriately fluxed.
3. The mechanical latch is actuated, engaging the two flanges and applying a predetermined amount of pressure.

4. As the breeding material is pumped into the blanket, the heater on the flange is turned on momentarily to allow the $\text{Li}_{17}\text{Pb}_{83}$ to flow between the flanges. The heater is then turned off and the frozen seal is established.

XII-B-4. Joints Between Blanket Modules

Making vacuum tight joints between adjacent blanket modules is one of the most difficult tasks faced by a reactor designer. In WITAMIR-I, as in NUWMAK⁽¹⁾, we have chosen not to have seals between modules but instead provide a vacuum barrier at the back of the shield, as is discussed in section XII-A-1. The blanket module itself is transparent to the flow of gas as is the joint between modules. Modules are simply butted against each other. Shield modules are sealed to each other between blanket access hatches. Immediately below an access hatch, the upper part of the shield is removable. The seal, which can be an organic one, will be attached to the removable shield section. The point at which the headers enter the shield also has to be sealed. We envisage that a welded bellows can be used between the header and the shield which will allow horizontal translation of the header without compromising the seal.

XII-B-5. Removing Blanket Modules

It has often been quoted that one of the difficulties in maintaining fusion reactor blankets is the need to move large, unwieldy objects which are sometimes quite heavy. This is certainly true. In WITAMIR-I we have attempted to mitigate this problem in two ways:

- a. Reducing the mass of the blanket module
- b. Limiting the motions of the blanket modules to straight guided axial translation and straight vertical lifting.

The mass of a drained blanket module is ~ 33 tonnes. Moving the module axially on guide rails is a relatively simple task. Lifting a 33 tonne module vertically in a straight line by an overhead crane is also deemed relatively straightforward.

XII-C. Maintaining Central Cell Coils

Although the failure of a central cell coil is going to be very rare, we have provided contingency plans for such an eventuality.

This is done by providing additional access hatches at intermediate locations between the routine maintenance hatches. Figure XII-C-1 shows such a hatch and the arrows indicate the movements needed to extract the various central cell coils which weigh on the order of 90 tonnes each. Obviously,

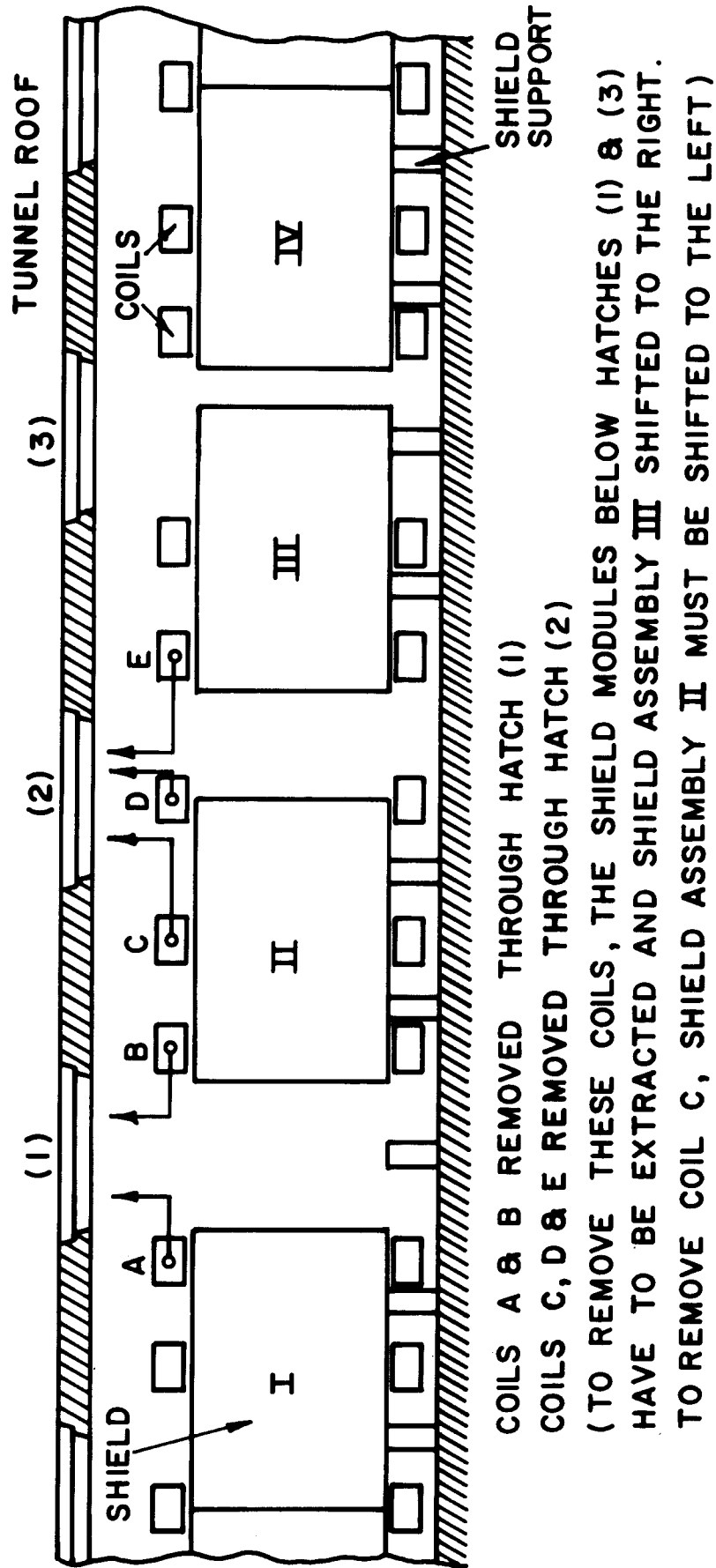


Fig. XII-C-1. Section of the WITAMIR-I central cell tunnel showing coil servicing scheme.

this implies the disassembly and movement of blanket and shield modules. It will be assumed that the overhead crane capacity will be 500 tonnes, enough to remove one-half of a shield module. In this way, provision is made to extract and replace any coil within the central cell.

XII-D. Shield Maintainability

There is very little that can go wrong with the shield except for developing coolant leaks. In most cases such leaks can most probably be fixed in situ. However, if the need arises for replacing a portion of the shield, then it is possible to do that through the access hatches described in the previous sections.

The mass of a shield module is 440 tonnes. A shield module is composed of an upper and a lower half as shown in Fig. XII-A-3. The lower half of the shield weighs ~ 290 tonnes and can be easily handled by the overhead crane. Figure XII-D-1 shows one possible way for extracting the shield modules.

XII-E. Maintainability of the End Plug

The end plugs in WITAMIR-I do not have blanket sections since the amount of DT fusion in them will be almost non-existent. The only maintenance of a routine nature will be that of the ion sources in the neutral beams and the gyrotrons in the ECRH. Most of the neutral beams are actually contained within the central cell tunnel and can thus be reached from the central cell access hatches. The 500 keV neutral beam lines and the ECRH systems, however, are in the end plug regions. These regions are accessible through special hatches as shown in Fig. XII-A-4.

Maintaining the ion sources and the ECRH gyrotrons will necessitate fairly frequent access (possibly on a monthly basis). It is therefore felt that some access, other than through an overhead hatch, will be needed. Since these functions are going to be performed by remote control, presumably by machines specifically designed for that purpose, the need is for an access through which spare parts can be introduced into the reactor tunnel and used parts removed. Such an access (or accesses) can be easily provided through appropriate airlocks at any number of locations. In the event a major component of a neutral beam or ECRH fails, the overhead access hatches can be used for removing and replacing them.

The end plug coils are all steady state superconducting magnets, which like the central cell coils, are not likely to require routine servicing and are designed to last the lifetime of the reactor. In the event of a

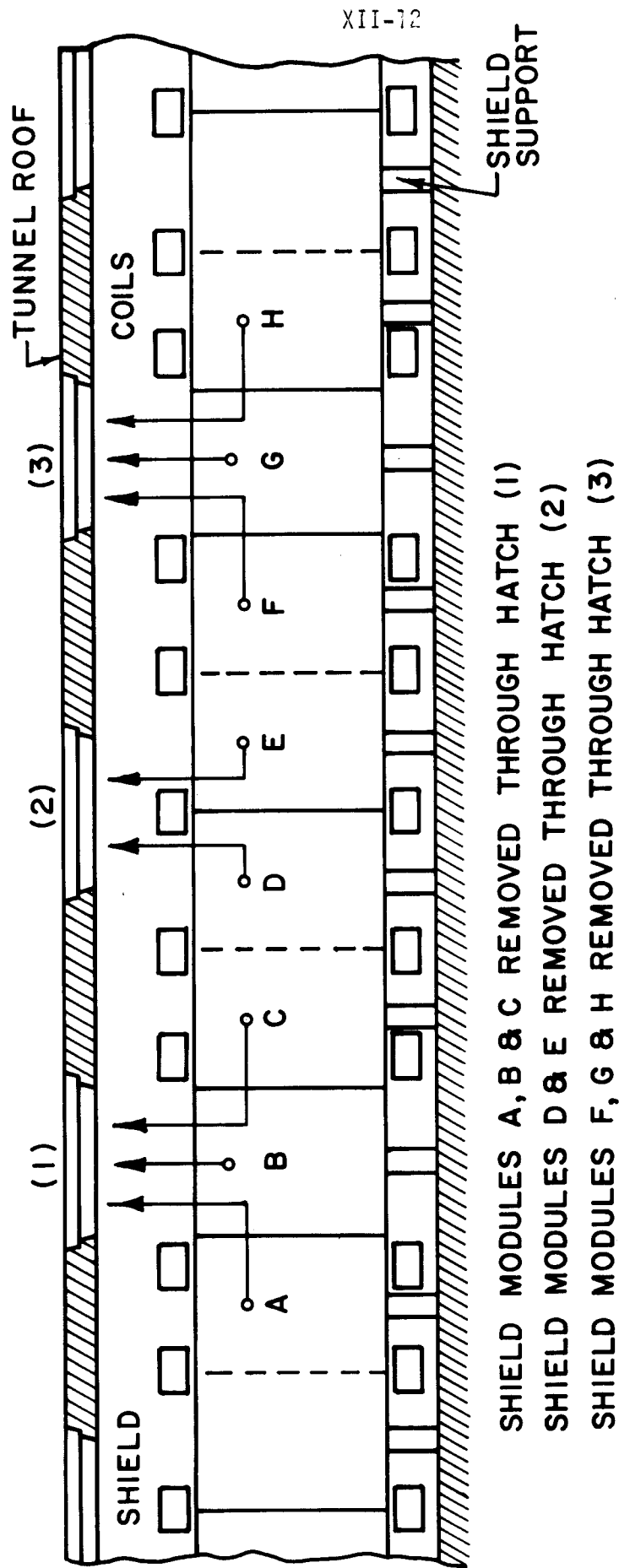


Fig. XII-D-1. Section of the WITAMIR-I central cell tunnel showing shield servicing scheme.

catastrophic failure of one of these coils, provision for replacing it has been made. The overhead gantry crane has been sized to handle the heaviest of the end plug coils, the barrier coil. The access hatches in the end plug region have also been sized to allow the removal of these coils.

Unlike the central cell coils, disengaging the end plug coils from their support structure and the shield is very complicated. A failure of an end plug coil will, therefore, necessitate a rather extensive shutdown of the reactor. As mentioned earlier, such occurrences are very unlikely. Moreover, minor failures such as vacuum leaks, etc. can be repaired in situ. This is possible because of the open geometry of the end plug in which each coil is accessible for servicing from all sides.

Reference for Chapter XII

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XIII. Balance of Plant - Power Cycle

XIII-A. Introduction

The only purpose of a power reactor is to produce electric power as safely and cheaply as possible. Such a goal sounds simple in principle, but in fact is quite complex in practice. Not only does such a goal involve the choice of coolants which should be compatible with structural material and tritium breeding, containment and recovery schemes, but it also involves a complex balance of flow rates, temperatures, pressure drops and pumping power.

The basic power cycle for WITAMIR-I is shown in Fig. XIII-A-1. It utilizes $\text{Li}_{17}\text{Pb}_{83}$ as the primary coolant which transfers its heat through a double walled steam generator to form steam. The steam condition is 16.5 MPa, 482°C with 482°C reheat. This offers a gross thermal efficiency of 42%, which compares favorably to a high temperature gas cycle but at 400°C lower temperature.

Earlier fusion reactor design studies suggested the use of an intermediate loop to provide an extra tritium diffusion barrier, to provide a thermal energy storage medium, and to minimize the possibility of water/primary coolant reactions. The addition of the intermediate loop not only complicates the power cycle design, but also increases the difference between the blanket operating temperature and the steam temperature. This increases the capital cost of the reactor system and reduces its thermal efficiency. Recent reactor designs, first NUWMAK and similarly STARFIRE, try to eliminate the intermediate loop. The combination of $\text{Li}_{17}\text{Pb}_{83}$ as the coolant, the double walled steam generator and the steady-state operation of a TMR eliminate the need of an intermediate loop for WITAMIR-I.

XIII-B. Primary Cooling Loop

The primary coolant of the blanket is $\text{Li}_{17}\text{Pb}_{83}$, which is chosen for its good neutronic properties, low tritium solubility and chemical inertness with water. The primary coolant is fed to a double walled steam generator. The double wall design provides a diffusion barrier for tritium.

A schematic of a typical primary coolant system is shown in Fig. XIII-B-1. Table XIII-B-1 lists the critical temperatures, pressures and flow rates for that system. There are six major subsystems in this loop and they are:

- Coolant cleanup
- Steam generator

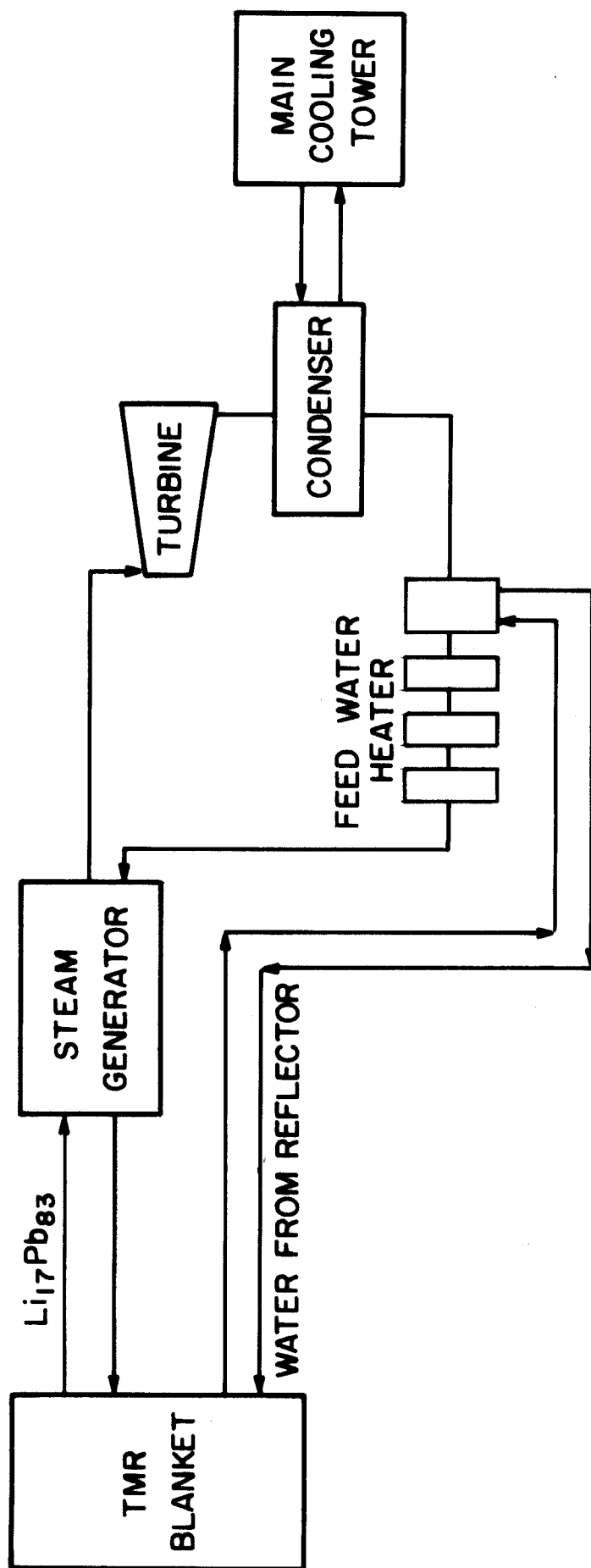


FIGURE XIII -A-1 SCHEMATIC POWER CYCLE DESIGN FOR WITAMIR-I

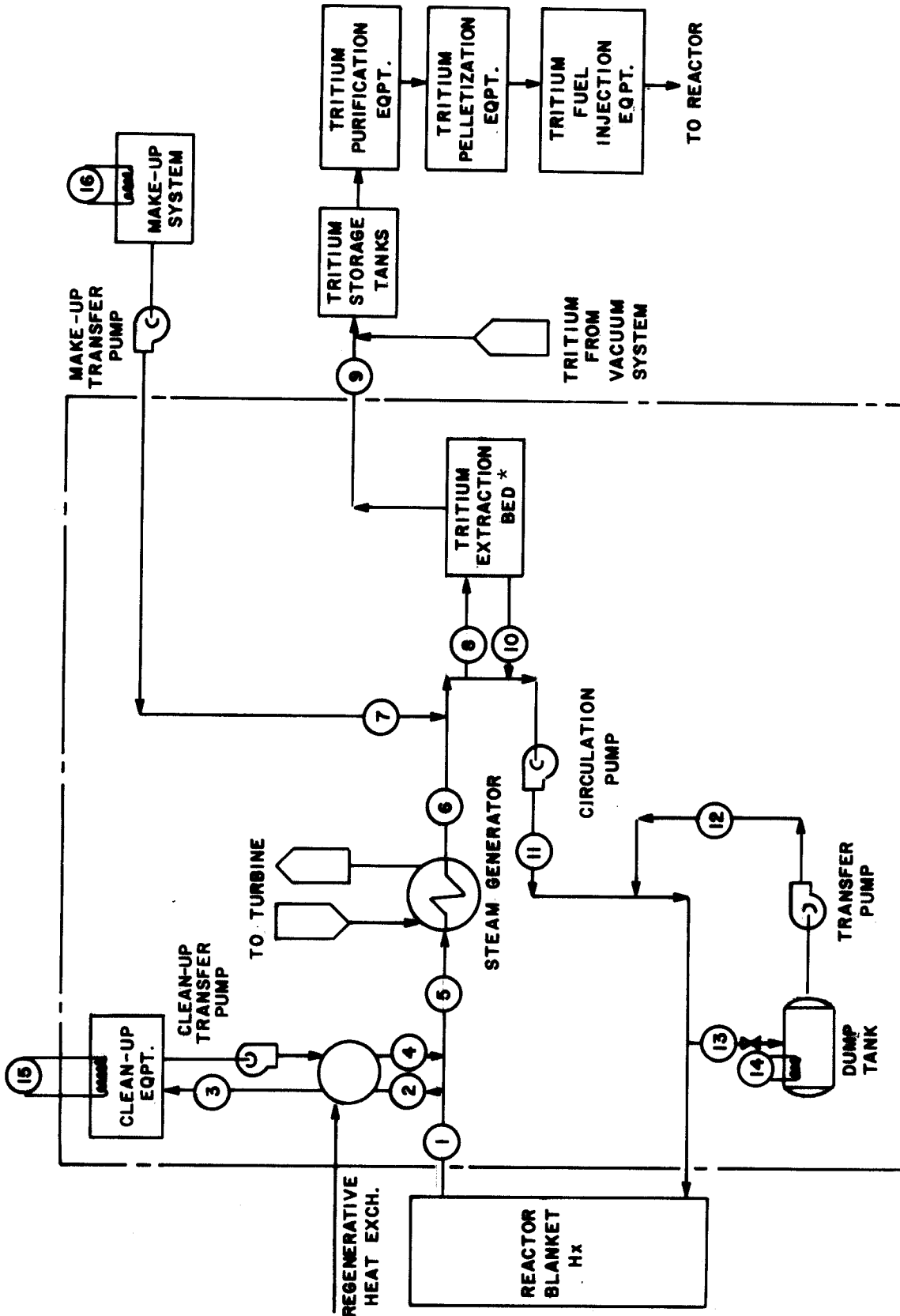


FIGURE XIII-B-1 PRIMARY COOLANT SYSTEM FOR WITAMIR-I

* Can be located either up or downstream of the steam generator, depending on the tritium recovery scheme.

Table XIII-B-1

Data Summary for Figure XIII-B-1

<u>System Location</u>	<u>Position</u>	<u>Temp °C</u>	<u>Flow, kg/hr</u>	<u>Pressure, MPa</u>
Reactor Outlet	1	500	4.4×10^8	.3
To Regenerator Hx	2	500	4.4×10^6	.3
To Coolant Cleanup Eqpt.	3	650	4.4×10^6	.25
From Regenerator Hx	4	525	4.4×10^6	.3
To Steam Generator	5	500	4.4×10^8	.25
From Steam Generator	6	329	4.4×10^8	.20
To Tritium Extraction Bed	8	329	8.8×10^6	.14
From Tritium Extraction Bed	10	329	8.8×10^6	.14
Inlet to Reactor	11	329	4.4×10^8	1.45

- Coolant makeup
- Tritium extraction bed
- Coolant circulation pump
- Coolant dump tank and storage.

The coolant comes from the reactor at a temperature of 500°C and a pressure of 0.3 MPa. It then passes the cleanup system where 1% of the main flow (4.4×10^6 kg/hr) is diverted for purification. The primary purpose of the cleanup system is to remove corrosion products and oxygen that may have leaked into the system. This may be accomplished by a hot trap process at 645°C to reduce the solubility of these elements. A regenerative heat exchanger is needed to recover 80% of the heat. The next step is to pass the coolant through the steam generator. The design of the steam generator will be discussed later.

The coolant makeup system represents a minor part of the overall power cycle and will not be treated in detail here. Basically, it will be used to analyze the composition of the coolant, and to supplement Li lost through leakage and evaporation; or ^6Li to replace that burned up to produce tritium.

The operation of the tritium extraction bed has been described in the tritium section. The only point to note here is that approximately 20% of the coolant flow is passed through the extraction bed and, via regenerative heat exchangers, there is essentially no temperature change involved with this operation.

The main coolant pump is assumed to handle 4.4×10^8 kg/hr of $\text{Li}_{17}\text{Pb}_{83}$ and it increases the coolant pressure from .14 to 1.45 MPa. It is assumed that liquid metal pump technology from LMFBRS will be applicable in this area.

Finally, provisions have been made to hold all of the $\text{Li}_{17}\text{Pb}_{83}$ from the primary coolant pump in the event that the reactor module needs to be withdrawn for repair. It should be noted that since $\text{Li}_{17}\text{Pb}_{83}$ melts at 235°C, special heaters will have to be installed in all makeup and storage tanks to keep $\text{Li}_{17}\text{Pb}_{83}$ from freezing.

XIII-C. Steam Generator

A unique feature of the steam generating system of a D-T fusion reactor is to provide a tritium diffusion barrier. An effective way to reduce tritium mobility is to change the tritium into a compound, such as HTO . Since tritium dissolves in sodium in the atomic form, the sodium intermediate loop is not an effective tritium diffusion barrier. In this study, we propose to use a

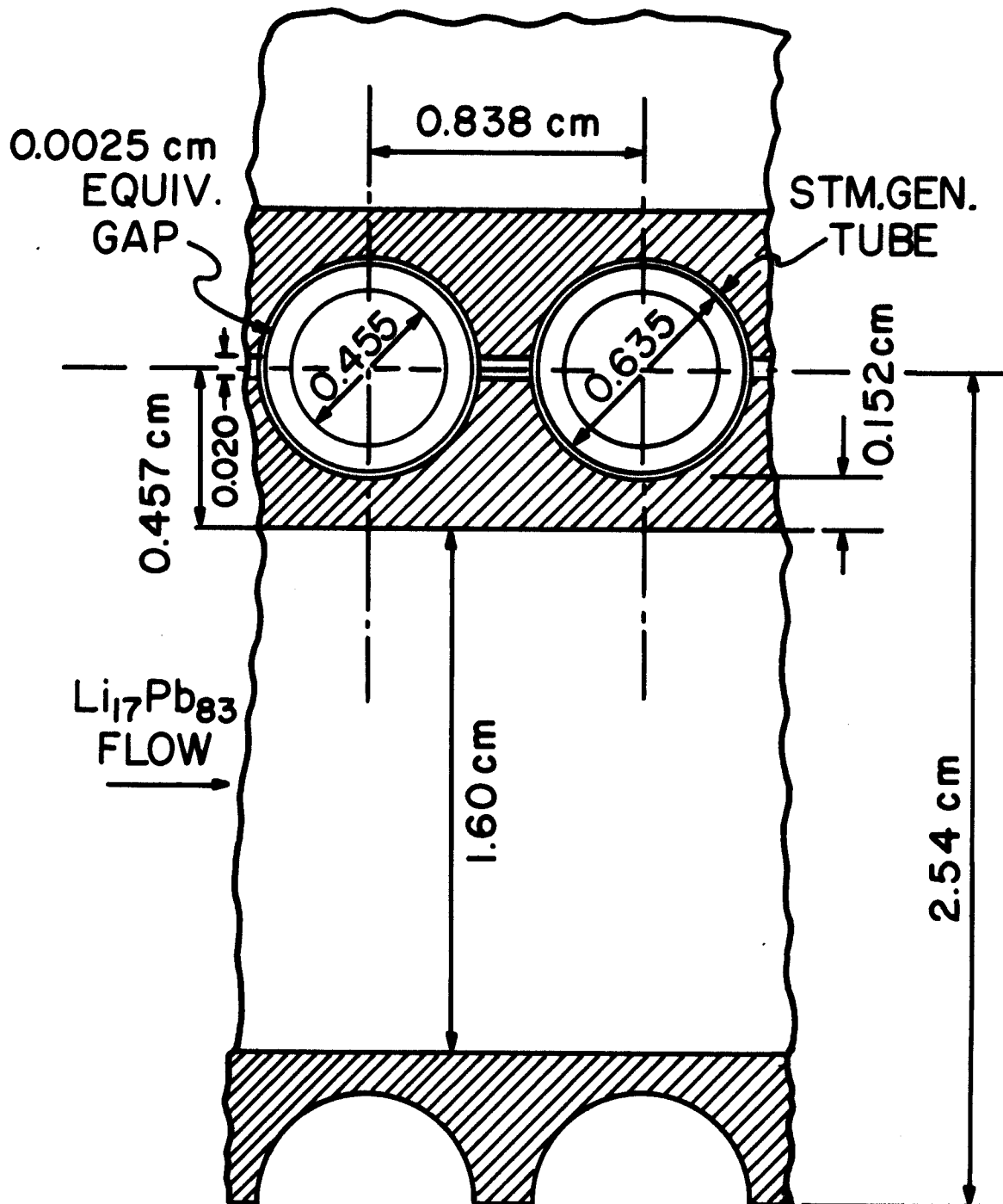
double walled steam generator to reduce tritium leakage to the steam side. A low flow rate of helium purge gas, with 1 torr of oxygen partial pressure, is passed through the gap to convert the tritium to HTO or T_2O and to carry it to the tritium recovery system. The design utilizes once through steam generator modules with vertical 0.635 cm OD, 0.089 cm thick tubes which fit into semi-circular grooves on the sides of horizontal rectangular primary coolant platens (see Fig. XIII-C-1).

There is a purge gas channel in between the two coolant tubes as shown in Fig. XIII-C-1. The purge gas will be introduced through one of the channels, pass through the gap circumferentially around the coolant tube, and exit through an adjoining purge gas channel. The gap is of average 25 micron width which can be maintained by the irregularities on the surface of two contacting metals, as shown in Fig. XIII-C-2.

The temperature-enthalpy diagram of the steam generator is shown in Fig. XIII-C-3. The turbine generator cycle is shown in Fig XIII-C-4. The resulting gross thermal efficiency is 42%. The important parameters of the WITAMIR-I steam cycle are summarized in Table XIII-C-1.

The power balance diagram for WITAMIR-I is shown in Fig. XIII-C-5, with an estimated net cycle efficiency of 39.4%.

FIGURE XIII-C-1
HORIZONTAL SECTION THRU
 $\text{Li}_{17}\text{Pb}_{83}$ PLATENS AND
STEAM GEN. TUBES



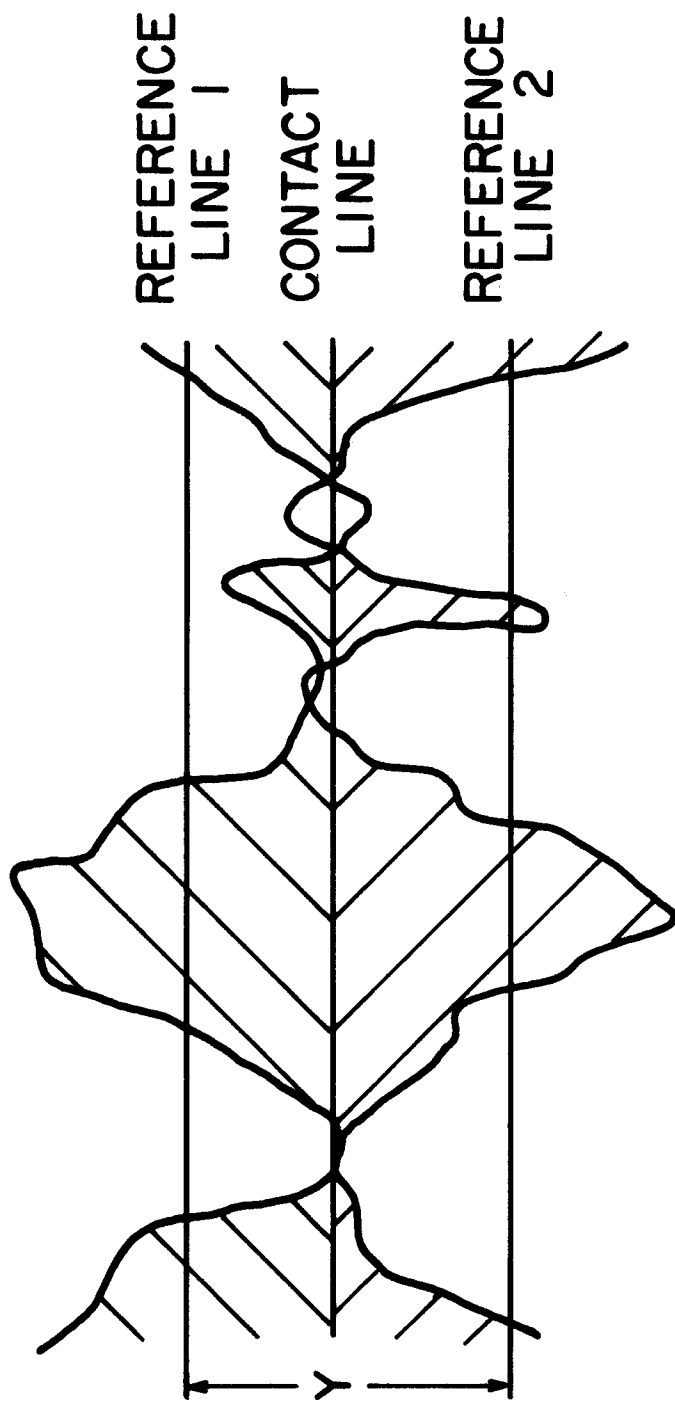


FIGURE XIII-C-2 PROFILES OF CONTACTING SURFACES

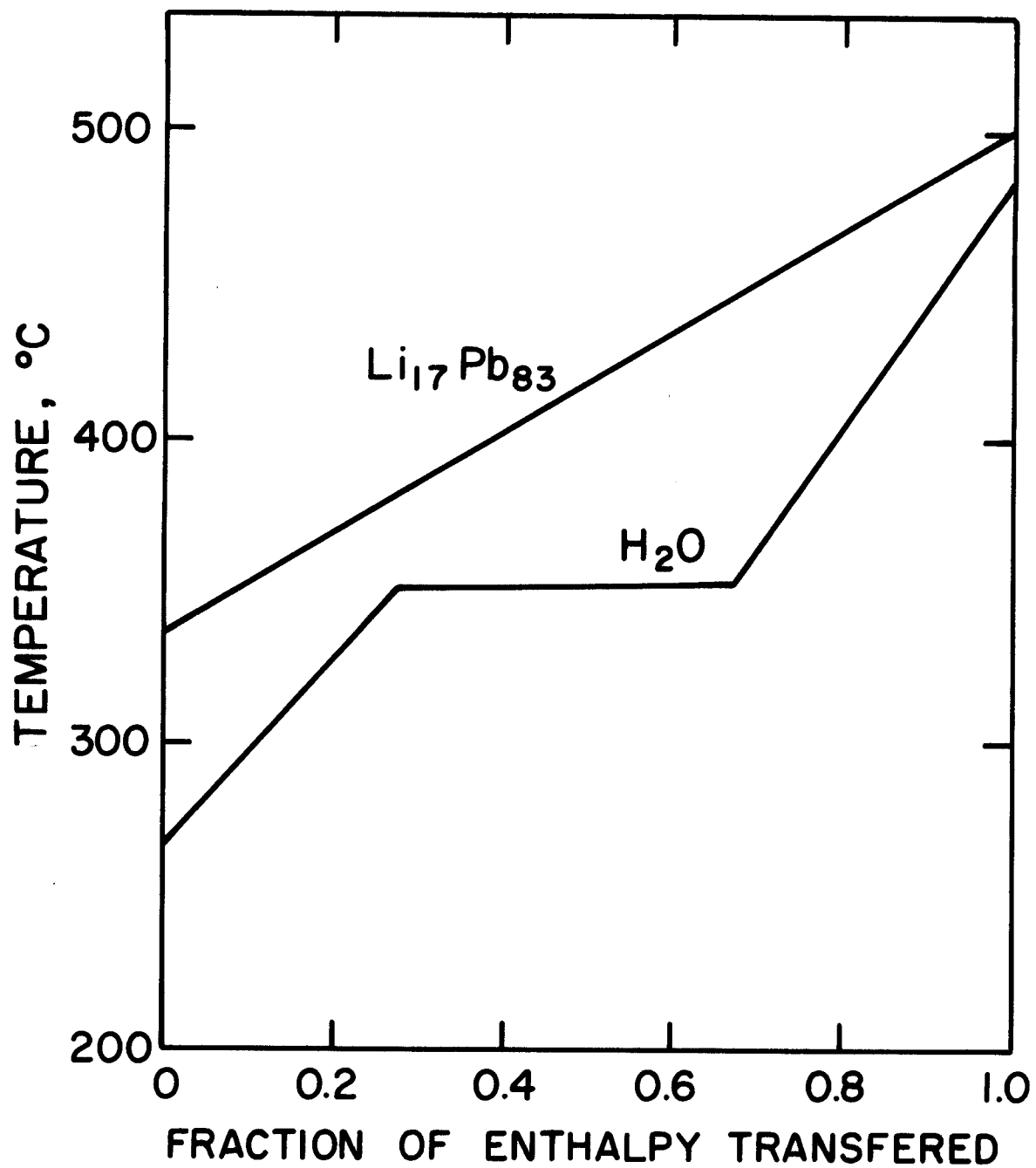
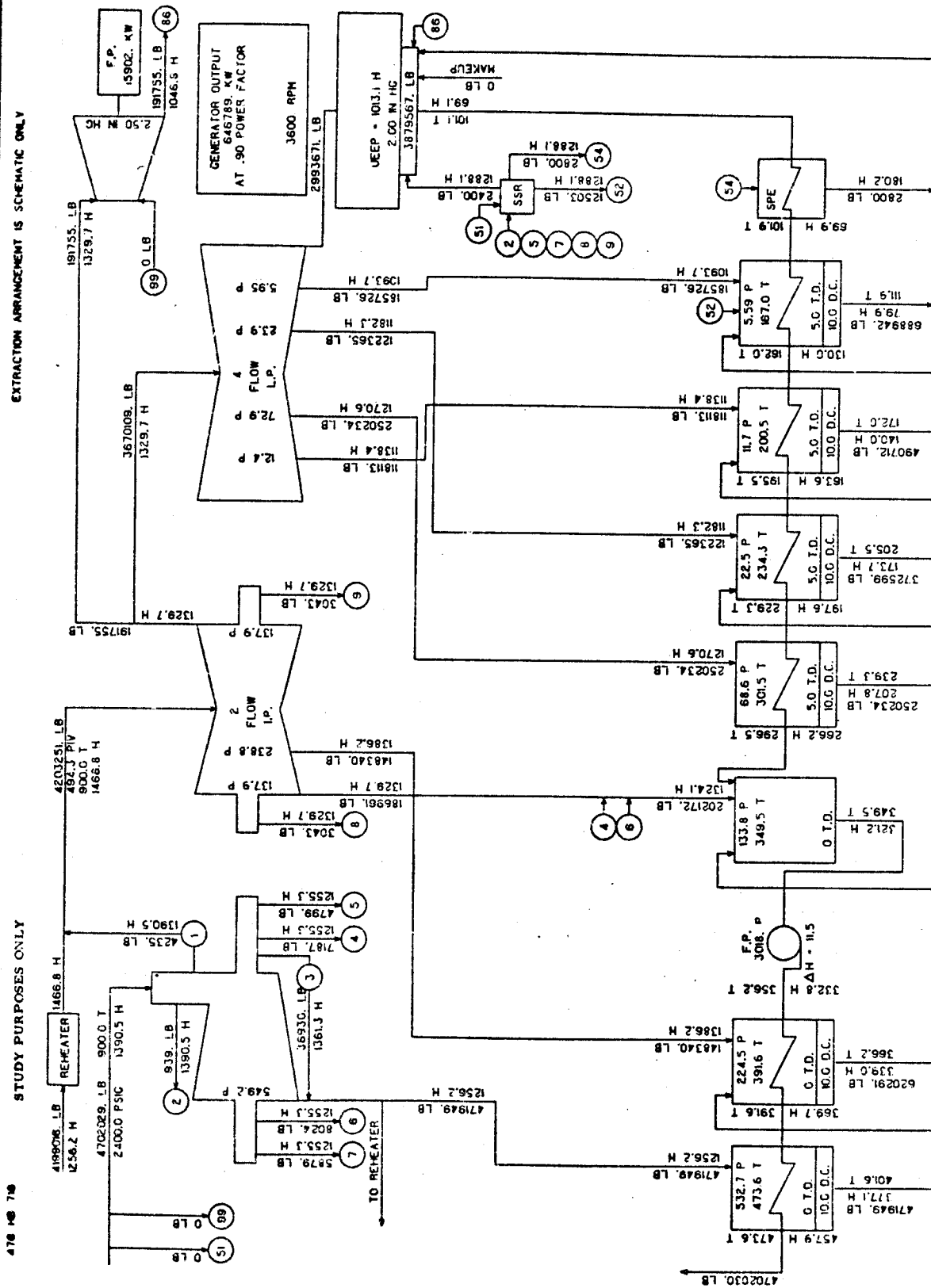


FIGURE XIII-C-3 T-H DIAGRAM FOR
WITAMIR STEAM GENERATOR



646789. KW 2.00 IN H₂ ABS.
TC4F 33.5 IN. LSB 3600 RPM
2400. PSIC 900. / 900. r
GEN- 782900. KVA

LEGEND - CALCULATIONS BASED
ON 1957 ASME STEAM TABLES
LB - FLOW-LB/HR
P - PRESSURE-PSIA
H - ENTHALPY-BTU/LB
T - TEMPERATURE-°F DEGREES

4702029.	1390.5	-	4579.1	BTU
VALVE BEST POINT.	4193016.	1466.8	-	1256.2
NET HEAT RATE				8148
			846789.	KW-HR

Fig. XIII-C-4

Table XIII-C-1

Principal Design Parameters of WITAMIR-I Steam Cycle

Total thermal power, MW(t)	3888
Power to steam cycle, MW(t)	3640
Total primary coolant flow rate (8 units), kg/hr	4.4×10^8
Primary coolant flow rate for each boiler, kg/hr	5.5×10^7
Li ₁₇ Pb ₈₃ temperature drop in boiler, °C	171
Li ₁₇ Pb ₈₃ inlet/outlet temp., °C	500/329
Heat load, each boiler, MW	455
Steam flow, each boiler, kg/hr	7.9×10^5
Total steam flow, kg/hr	6.3×10^6
Steam pressure, MPa	16.5
Steam temperature, °C	482
Feed water temp., °C	266
Steam generator tube heating surface per boiler, m ²	2000
Total steam generator surface area, m ²	1.6×10^4
Gross cycle efficiency, %	42
Gross thermal cycle output, MWe	1530
Gross direct cycle output, MWe	330
Recirculating power, MWe	330
Recirculating power fraction	.177
Net power output, MWe	1530
Net thermal efficiency, %	39.4

POWER FLOW DIAGRAM FOR WITAMIR I

XIII-12

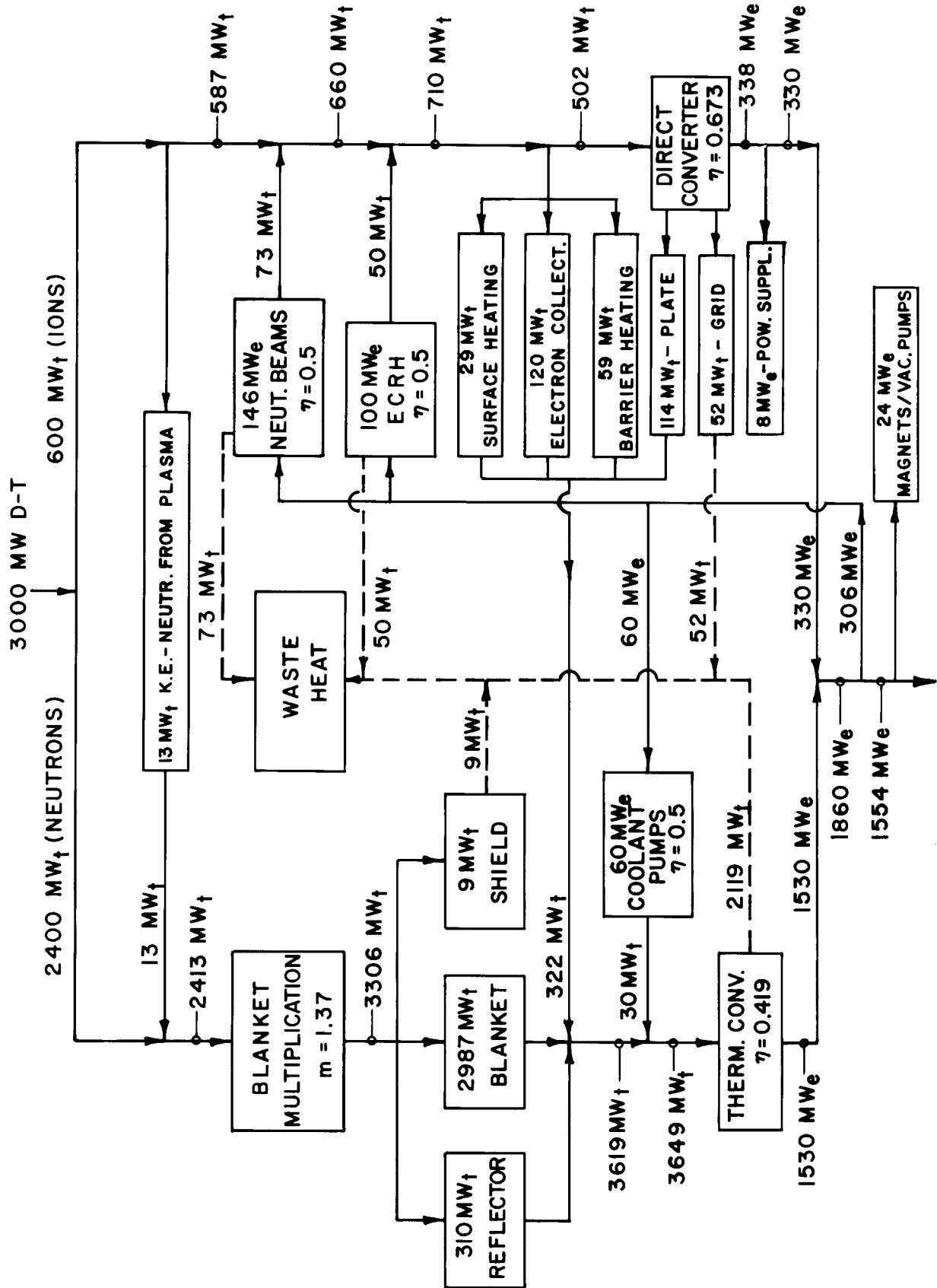


Fig. XIII-C-5

XIV. Materials Resource Analysis

XIV-A. Introduction

In a world of dwindling mineral resources, it is extremely important that any new technology take into account the burden it will place on existing production facilities and reserves of the required materials. This chapter examines the material requirements of the WITAMIR-I reactor and the implications of a 300 GWe fusion power economy based on such a reactor.

Early conceptual fusion power reactor designs relied on stainless steel for the first wall/blanket and magnet structural material. This placed a tremendous burden on the world chromium and nickel resources. Although the world chromium reserves are quite large, there are none in the U.S. at present day prices. The picture for nickel is even worse since the world nickel reserves are not that great. In WITAMIR-I the first wall/blanket is made from the martensitic material HT-9 which requires 30% less chromium and 95% less nickel. The structural material in the central cell coils is high strength aluminum and the stabilizer is high purity aluminum. This too reduces the dependence on stainless steel for structure and also improves the picture for copper, which is normally used as the stabilizer. Unfortunately, in the barrier and end plug coils, because of the high fields and unusual geometry, we were forced into using stainless steel for structure and copper for the stabilizer. In spite of this, the total requirement for chromium has been reduced by ~ 80% and for nickel by 94% over reactors utilizing stainless steel exclusively for the first wall/blanket/magnet structure.

XIV-B. Discussion and Analysis

Tables XIV-B-1 and XIV-B-2 give the material requirements for WITAMIR-I in the nuclear island and the balance of plant, respectively. Each table gives the elemental breakdown as well as the material or alloy used. The materials in the blanket category are divided into two columns, the initial and the 30-year replacement needs. The breeding material listed under blanket includes all the material in the headers, pipes and heat exchangers. Table XIV-B-3 summarizes the material requirements for WITAMIR-I. In the last column, the material requirements in tonnes/MWe are given. This information is used to arrive at the material requirements for a 300 GWe fusion economy based on the WITAMIR-I reactor. The values listed in Table XIV-B-4 are for the total requirements during a 30-year implementation

Table XIV-B-1
Elemental Breakdown of Material Requirements

WITAMIR-I Nuclear Island												
Blanket			Central			Transition & Yin Yang		Direct		Neutral		Tonnes
Initial	30 Year Replacement	Reflector	Shield	Cell	Barrier		Convertor	Beams &	Support	Total	Per	
				Coils	Coils	Coils	Incl.	ECRH				Structure
												(1530)
Al				3129.16			3.98	11.23	184.54	3328.91	2.175	
B		581.38						0.16		581.54	0.380	
C	2.20	13.2	161.12				0.37		0.516	185.95	0.121	
Cr	125.23	751.4	491.63		100.05	174.13	22.59	8.72	3.01	1676.76	1.096	
Cu				178.15	1116.57	1804.66	12.94	44.37	14.08	3170.77	2.072	
Fe	931.09	5586.6	3655.13	5.73	437.70	761.81	166.60	41.64	421.73	25,603.9	16.734	
He											XIV-2	
Li	79.67	111.										
La								0.34		190.67	0.124	
Mg				0.55					0.04	0.59	0.0002	
Mn	5.44	32.67	21.38	10.97	12.51	21.77	0.78	1.03	2.09	287.72	0.0004	
Mo	10.89	65.34	42.75		12.51	21.77	2.06	8.64		163.96	0.188	
Nb				38.13	15.98	19.64				73.75	0.107	
NI	5.44	32.67	21.38		62.53	108.83	1.98	4.78	2.79	240.40	0.048	
Pb	8306.95		3,368.00							11,674.95	0.157	
Sn					1.81					1.81	7.630	
Ta					5.51		13.79			19.30	0.001	
Ti				22.84	12.57	10.33		0.01	0.20	45.95	0.013	
V	3.27	19.62	12.83	4.12			0.94	0.05	0.30	41.14	0.030	
W	5.44	32.67	21.38				2.25	4.85		66.59	0.027	
Zn				2.74				0.01	0.20	2.95	0.044	
Zr				6.84			0.01	0.03	0.50	7.38	0.002	
TOTAL											30.88	

Table XIV-B-1 (contd.)

Materials Used in WITAMIR-I Nuclear Island

HT-9	1089.	6534.	4275.03		185.	10.	30,224.03	19.754
Li ₁₇ Pb ₈₃	8386.62						8386.62	5.481
B ₄ C		742.50					742.5	0.485
Pb		3368.					3368.0	2.201
316SS			625.6	1088.31	10.45	47.17	1771.53	1.158
Al-2219					4.32	12.18	2957.50	1.933
Cu				1105.44	12.94	43.58	2966.62	1.939
Al(HPA)			600.				600.00	0.392
NbTi			58.23	8.99			97.19	0.063
Nb ₃ Sn				15.67			15.67	0.010
NbTiTa				22.04			22.04	0.014
Ta10W					15.58		15.58	0.010
LaB ₆						0.5	0.50	0.0003
A-242		13,775.				430.	430.	0.281
Mo						7.6	7.6	0.005
W						4.83	4.83	0.003
Li								

111.

Elemental Breakdown of Material Requirements

WITAMIR-I Balance of Plant

[illegible]

Table XIV-B-3
Summary of Material Requirements for WITAMIR-I

Element	Nuclear Island (MT)	Balance of Plant (MT)	Total (MT)	Tonnes Per MWe
Al	3,328.91	8.00	3,339.91	2.180
B	581.54		581.54	0.380
C	185.95	95.60	281.55	0.184
Co		10.00	10.00	0.006
Cr	1,676.76	257.19	1,933.95	1.264
Cu	3,170.77	70.00	3,240.77	2.118
Fe	25,603.90	63,350.53	88,954.43	58.140
Li	190.67		190.67	0.125
La	0.34		0.34	0.0002
Mg	0.59		0.59	0.0004
Mn	287.72	232.96	520.68	0.340
Mo	163.96	73.60	237.56	0.155
Nb	73.75		73.75	0.048
Ni	240.40	264.35	504.75	0.330
Pb	11,674.95		11,674.95	7.630
Si		7.25	7.25	0.005
Sn	1.81		1.81	0.001
Ta	19.30		19.30	0.013
Ti	45.95	10.00	55.95	0.037
V	41.14	2.07	43.21	0.028
W	66.59	3.45	70.04	0.046
Zn	2.95		2.95	0.002
Zr	7.38		7.38	0.005
		TOTAL	111,753.33	73.037

Table XIV-B-4
Total, Annual and the Percentage of
Present U.S. Consumption, Material Needs for
a 300 GWe Fusion Economy Based on WITAMIR-I,
Assuming a 30-Year Implementation Period

	<u>Total</u> <u>Requirement</u> <u>(10³ Tonnes)</u>	<u>Annual</u> <u>Requirement</u> <u>(10³ Tonnes)</u>	<u>Percentage of</u> <u>1979 U.S. Annual</u> <u>Consumption (%)</u>	
Fe	17,442.00	581.400	0.70	
Pb	2,289.00	76.300	5.30	X
Al	654.00	21.800	0.40	X
Cu	635.40	21.200	0.94	X
Cr	379.20	12.640	2.40	X
B	114.00	3.800	3.20	
Mn	102.00	3.400	0.26	
Ni	99.00	3.300	1.60	X
C	55.20	1.840		
Mo	46.50	1.550	5.10	X
Li	37.50	1.250	32.00	X
Nb	14.40	0.480	16.00	X
W	13.80	0.460	5.00	X
Ti	11.10	0.370	2.10	
V	8.40	0.280	3.90	X
Ta	3.90	0.130		
Co	1.80	0.060	0.70	X
Si	1.50	0.050		
Zr	1.50	0.050	1.70	
Zn	0.60	0.020		
Sn	0.30	0.010	0.02	
Mg	0.12	0.004	0.004	
La	0.06	0.002		

period, the average annual requirements during this period and the percentage of the present day (1979) U.S. annual consumption. The U.S. annual consumption values were taken from Table XIV-B-5.⁽¹⁾ Listed in Table XIV-B-5 are the values for U.S. and world production, U.S. annual consumption and U.S. and world reserves at present day (1979) and 3 x present day prices of the materials which will be needed to implement a fusion economy. The countries in which these reserves are located are also listed.

The materials which present a minor concern are marked with an X in Table XIV-B-4. They will be briefly discussed in the following sections.

Lead

The requirement for lead is quite large, representing a 5.3% increment over current consumption. The U.S. currently imports ~ 32% of its lead. However, the demand for lead may be decreasing with the phasing out of leaded gasoline which requires 15% of the annual consumption. We therefore do not believe that the fusion demand will be problematic.

Aluminum

The U.S. produces more aluminum than any other country in the world, but is dependent on foreign sources for more than 90% of the raw materials.⁽¹⁾ However, at 3 x present prices, large known resources of aluminous materials within the U.S. would come into economic range. The 0.4% increments over present consumption would not appear to present any problems.

Copper

The U.S. imports ~ 25% of its copper.⁽¹⁾ This is due to foreign competition, limitations on the utilization of domestic copper deposits, fluctuating and periodically depressed prices and insufficient domestic smelting and refining capacity. Given the proper economic incentives, the copper demands can be met from domestic sources; however, the presently known copper reserves will most likely be depleted by 2040. Although the 0.94% additional demand for fusion is not large, the copper situation will continue to be somewhat tenuous.

Chromium

The United States, like Japan and the major industrial nations of Western Europe, depends entirely on imports for its supply of chromium. There are no reserves in the United States. World reserves are very large relative to foreseeable demand, but 99 percent of world reserves are located in Zimbabwe-

Rhodesia and the Republic of South Africa.⁽²⁾ Much of the remainder is in the U.S.S.R. and in Turkey.

At 3 x present prices, chromite, the ore of chromium, would become available from the Stillwater Complex, Montana.⁽¹⁾ Resources there are sufficient to supply the indicated additional U.S. requirements for nuclear fusion. Such prices are only likely, however, if sources abroad are cut off. In that case, demand for chromium for fusion purposes would have to compete with other demands for chromium. The metal has become an essential material for a number of important industrial applications, and competition would be severe.

The United State has low-grade resources of chromium from which chromite might be extracted, though probably at high cost. At present, however, there is no economic incentive for the industrial research and development that would be required to make chromium available from such resources.

Nickel

The U.S. imports ~ 65% of its annual consumption. Reserves will be virtually exhausted by the end of the century.

Large resources of nickel that would be economic at 3 x present prices are known in the Duluth Complex of northern Minnesota. One company has a substantial program of research and development in progress in a portion of the known deposits in the Complex. At present, however, there is no schedule for production from the deposits. Future development will depend not only on prices of nickel and copper but also on the resolution of environmental problems.

During the past 15 years an alternative to the conventional sources of nickel has emerged through recognition of the presence of large tonnages of nickel in deposits of "manganese nodules" on the deep sea floor. A belt south of the Hawaiian Islands is particularly rich in such deposits. The development of necessary technology is well advanced, but actual mining will not take place until legal and political problems are resolved.

Outside the United States there are large reserves of nickel both in sulfide deposits and in lateritic deposits in the tropical belt. Canada has large reserves and currently supplies nearly 60 percent of U.S. imports. It would thus appear that the additional 1.6% for fusion can be readily met.

Molybdenum

The United States accounts for about 60 percent of total world production of Mo and has nearly 45 percent of total world reserves of the metal.⁽¹⁾ U.S. reserves are large relative to the current rate of production, but at that rate about one-third of present reserves will be depleted by the end of the century. Furthermore, demand for the metal has been increasing at an annual rate of about 4 percent, and continued increase is forecast for the future.

A substantial part of U.S. molybdenum production is a by-product of copper mining and fluctuates with conditions in the copper industry. Prospects for development of new reserves are mixed. Substantial success has attended exploration in recent years, but development is complicated by environmental problems and by land withdrawals and other Federal actions that are hindering both exploration and development. Development of the second largest known Mo orebody in the world at Quartz Hill, Ketchikan District, Alaska may be prevented owing to its inclusion in a national monument.

The availability of Mo for fusion purposes is thus a function of four things: (1) rate of increase in demand for other purposes; (2) rate of discovery of new deposits; (3) rate of development of new production capacity; and (4) the health of the domestic copper industry. The demand for fusion purposes, spread over 30 years, is not large, but the molybdenum supply situation will need to be monitored periodically in anticipation of needs for fusion.

Lithium

The size of lithium reserves and resources is of prime importance in view of the prospective use of lithium as a source of tritium for the fusion process. Information on lithium, however, is incomplete and uneven in quality, and the availability of lithium for fuel is further clouded by possible changes in the scale of use of lithium for other purposes. Lithium resources and requirements by the year 2000 were reviewed at a symposium held in 1976. At a second symposium held in 1977, the results of a study by the lithium subpanel of CONAES were reported.⁽³⁾ Estimates given are based largely on this report plus a report by Erickson⁽⁴⁾ on brines in salars of Western Bolivia.

Known lithium resources in the world are contained mostly in two classes of deposits, pegmatite deposits and brine deposits. The largest known reserves of Li in pegmatite deposits are in the tin-spodumene belt (Kings

Mountain district) of North Carolina and in the Manono-Kitole district of Zaire, the former a principal source of the United States and world supply, the latter apparently about to be brought into production. The only productive brine deposit at present is that of Clayton Valley, Nevada, but development of the much larger brine deposits of the Atacama Desert in northern Chile is under way. All these are included in Table XIV-B-5 for world resources of Li. Reserves in Clayton Valley and North Carolina constitute U.S. reserves, since by-product Li will no longer be available from the brine of Searles Lake, California.

The figure for U.S. reserves at 3 x present prices includes resources known to exist in Great Salt Lake, additional Li present in Clayton Valley brines not economically recoverable at present prices, and probable additions, through further exploration, to tonnages of ore known in the Kings Mountain district. A 50 percent discount, possibly excessive, has been applied to the gross resource figures to allow for losses in extractive processes. The brines of the Salton Sea Basin, California, are estimated to contain⁽⁵⁾ 1,000,000 tonnes of Li, but in view of severe technological problems of extraction are not included. The brines of the Smackover Formation of Arkansas and Texas are also excluded. The figure for world reserves at 3 x present prices expresses the likelihood that additional reserves will be disclosed by deeper drilling in Zaire⁽⁶⁾ and that additional reserves in lithium-bearing brine deposits will be defined in western South America,⁽⁴⁾ the United States, and possibly other parts of the world unexplored for Li. A substantial part of these resources may ultimately be found to qualify as reserves at present prices. Known reserves of Li are so large relative to current demand that there has been little incentive to explore for lithium in recent years.

Despite large reserves and resources relative to current demand, and the favorable prospects for new discoveries, there has been concern about the future availability of Li for fusion. Lithium is under consideration for use in electric cars powered by lithium-anode batteries. Assuming that in the year 2000, 12 to 16 percent of all vehicles are powered by lithium-air-water batteries, Cooper and others⁽⁷⁾ estimate that between 234,000 and 425,000 tonnes of Li would be required. U.S. reserves at present prices would not support the necessary rate of production; Vine⁽⁸⁾ estimates the probable yield by the year 2000 at 230,000 tonnes. Conventional uses, batteries not

included, may demand as much as 300,000 tonnes between now and 2000. The need for tapping the deposits included under reserves at 3 x present prices is evident.

In considering the use of lithium for energy applications, it should be noted that lithium's contribution to energy supply will be far greater if it is used as a fusion fuel than if it is used in batteries for energy storage. In terms of energy yield, lithium would be economic as a fuel at nearly 10 times the present price of 3.5 cents per gram.

Niobium

Since niobium is an indispensable component of the superconducting magnets necessary in reactors, the size and distribution of niobium reserves and resources are of critical importance.

Niobium resembles chromium, in that the United States depends for its supply completely on imports of ores and the base alloy ferrocolumbium. The United States produces no niobium from domestic deposits. There are no domestic reserves. Reserves indicated at 3 x present prices are in the Powderhorn area, Colorado. They would be adequate for fusion requirements only in the absence of competing demands.

World reserves of niobium are large. Of the reserves indicated, 8,000,000 tonnes are in Brazil, nearly 600,000 tonnes in Canada, the remainder mostly in countries of Africa. Nearly all of the world demand outside the Communist sphere is supplied from Brazil (78 percent) and Canada (17 percent). Resources that would be available at 3 x present prices are even larger; the figure of 20×10^6 tonnes given in Table XIV-B-5 is conservative. Significant additional resources in Canada are included. Prices at the higher level would bring only modest U.S. resources into the category of reserves.

In summary, world reserves of niobium are more than adequate to supply world needs, including possible requirements, for many decades. The existence of large resources in Canada, available at 3 x present prices, gives the United States insurance against any possible disruption of the flow of niobium raw materials from sources outside North America.

Tungsten

The United States produces only about 40 percent of its normal requirements. In recent years the rest has been met from imports and releases from the government stockpile.

World consumption of tungsten for conventional uses is rising. Even at the 1978 rate of production over one-half U.S. present reserves, and nearly one-half of world reserves, would be used up by the year 2000. Other U.S. identified sources are estimated by Hobbs and Elliott to be about three times reserves at present prices, but the rate and prices at which these might become available are unknown.

A substantial share of world production comes from China, which supplies its own needs, part of the demand from the Soviet bloc, and part of Free World demand. The scale of Chinese production is uncertain. Production for 1975 is estimated by Thurber⁽⁹⁾ at 8.6 to 9 thousand tonnes. Hobbs and Elliott estimate that southeastern China contains 60 percent of world reserves (1.2 million tonnes). Second largest reserves are in Canada (215,000 tonnes). Much of the remaining reserves are in Australia, Turkey, Korea, Bolivia, and U.S.S.R.

China is thus a major factor in both present and future supply of tungsten. If industrialization of China is speeded up, internal needs for tungsten will rise, and much less might be available for export.

The 5% increment in the annual consumption due to fusion is not very excessive and meeting it should not be difficult.

Vanadium

Estimates of world reserves are incomplete. The U.S. Bureau of Mines estimates⁽¹⁰⁾ Free World reserves at 2,550,000 tonnes of which about 1,800,000 tonnes is in South Africa. The figure for South African reserves is a very conservative one; Van Rensburg and Pretorius estimate South African reserves at nearly 12,800,000 tonnes and this figure is based on firm data for the deposits in the Bushveld Complex. Much less firm is our estimate for reserves at 3 x present prices. The reason is that the economics of production of vanadium from low-grade sources of various types is speculative.

The additional 39% needed for fusion will necessitate a 10% increase in the amount of vanadium in U.S. imports annually. This does not appear to be excessive. Since vanadium is used as an alloying element in HT-9, it may be possible to replace it with something else which is more readily available.

Cobalt

The only use of cobalt in WITAMIR-I is in the turbine blades. The most commonly used material is UDIMET 700 which has 18.5% Co. However, the alloy HASTELLOY R-235 which has only 2.5% Co has been proposed as a suitable replacement.

There are no U.S. reserves of cobalt. Reserves of cobalt outside the U.S. are large relative to world annual consumption of cobalt, and additions to reserves are likely in the future. However, size of reserves is not as important as the dependence of rate of production on the scale of Ni and Cu production. About 40 percent of world production in 1978 was from Zaire and Zambia, but this may change with further development of cobalt-bearing lateritic nickel deposits in other countries of the world.

The requirement for fusion is relatively small if the low Co alloy can be used.

XIV-C. Conclusions

From the discussion presented we can conclude that a 300 GWe U.S. fusion economy based on the WITAMIR-I reactor would not place an undue burden on the needed material resources when spread over a period of 30 years. In some cases the need can be satisfied by tapping U.S. reserves at 3 x present day prices or increasing imports in cases where world reserves are substantial. It appears that the choice of materials in WITAMIR-I strikes a better balance with respect to depletion of U.S. reserves and dependence on foreign imports than have earlier fusion reactor designs.

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XV. System Economics

XV-A. Introduction

The system economics for WITAMIR conform to the guidelines provided in the DOE "Fusion Reactor Design Studies - Standard Accounts for Cost Estimates".

The two modes of analysis used are the current dollar mode which assumes that the purchasing value of the dollar changes with time and the constant dollar mode which assumes the dollar value remains unchanged, thus the inflation rates and component escalation rates are equal to zero. It is important to note that in the case of the constant dollar mode, it would be unfair to compare the value for the cost of power obtained for WITAMIR-I with the present cost of power from existing power plants. This is because the present cost of equipment is used without deescalation by eight years (the assumed construction period) and the interest during construction is based on present day cost. In the case of the current dollar mode, which indicates what the cost of power from WITAMIR-I would be eight years hence, it would be fair to compare it with other power systems which are costed out in the same way.

XV-B. Level of Technology

Many levels of technology are used in the design of a fusion reactor system and subsystems, ranging from current to advanced. Advanced technology is defined as one that has not been demonstrated on a commercial scale.

With a few exceptions, most of the technology used in the nuclear island is advanced. This is particularly true of the high energy negative ion neutral beams and the ECRH.

The level of technology in the balance of plant, however, is current, especially since a conventional steam cycle is used in the power conversion system.

XV-C. Design Allowance, Contingency and Spare Parts

The design allowance is a sum of money which is included in the estimate to account for items overlooked or underestimated. For this reason, the level of technology in a particular area dictates the amount of design allowance allocated for the different accounts. Thus, in WITAMIR-I a 15% design allowance was allocated for the reactor plant equipment while the remaining accounts got only 10%.

Contingency is a sum of money provided to account for items not specified in the engineering detail but which are known from experience to be needed. For this, we have used 10% throughout the report.

The spare parts allowance is the purchase cost and inventory cost prior to facility operation of the initial inventory of spare parts required onsite. Reactor parts rotated into service as part of a scheduled maintenance are not considered as spare parts.

In WITAMIR-I the following spare parts allowance was used:

Reactor Plant Equipment	2%
Turbine Plant and Miscellaneous Equipment	1%
Electric Plant, Structures and Site Facilities	0.5%

XV-D. Nuclear Island Unit Costs

The unit costs for the accounts which dominate the reactor plant equipment will be given with some explanatory notes.

XV-D-1. Blanket and First Wall

WITAMIR-I has perhaps the simplest blanket/first wall design of any conceptual fusion reactor. The material is the ferritic steel HT-9 and the blanket consists of banks of bent seamless tubes, manifolded at the top and bottom. A unit cost of \$25/kg was used.

The breeding material $\text{Li}_{17}\text{Pb}_{83}$, which is 99% lead, was costed at \$3.5/kg.

XV-D-2. Shield

The reflector, manufactured of thick slabs of HT-9, was priced at \$10/kg. The other unit costs were:

HT-9	\$10/kg
Lead	\$ 3/kg
B_4C	\$ 6/kg

XV-D-3. Magnets

Experience has shown that the cost of superconducting coils usually falls in the range of \$40-60/kg depending on their complexity. We have used the following costs for the WITAMIR-I coils:

Central-Cell: Simple solenoids using NbTi/Al stabilizer/Al structure	\$40/kg
C-Shaped Coils: NbTi with Cu stabilizer and SS structure	\$50/kg
Barrier Coil: NbTi/NbTiTa/Nb ₃ Sn with Cu stabilizer and CuNb structure	\$60/kg

XV-D-4. Supplemental Heating

A unit cost of \$2/Watt was used for both neutral beams and ECRH.

XV-D-5. Vacuum Systems

Compound cryopumps were priced at $\$20 \times 10^3/\text{m}^2$. Each coil vacuum system was priced at $\$200 \times 10^3$.

XV-D-6. Main Heat Transfer System

The $\text{Li}_{17}\text{Pb}_{83}$ circulation rate is 4.4×10^8 kg/hr. The cost of a liquid metal pump which circulates 10^7 kg/hr is $\$3.3 \times 10^6$ and the cost goes as follows:

$$C \approx 3.3 \times 10^6 \left(\frac{R}{10^7} \right)^{0.75}$$

where R is the new circulation rate. Thus a pump of 4×10^7 kg/hr circulation rate will cost $\sim \$9.3 \times 10^6$.

In WITAMIR-I, each three modules are served by one pump, thus eleven are needed, with a spare making twelve pumps. The following unit costs for HT-9 pipes were used:

$d \leq 30$ cm	$\$20 \times 10^3/\text{m}$
$d = 30 - 60$ cm	$\$30 \times 10^3/\text{m}$
$d = 100$ cm	$\$60 \times 10^3/\text{m}$

The steam generator was priced at $\$22.50/\text{kWth}$.

The coolant clean-up system was priced at $\$3.50/\text{kg}$ of breeding material inventory.

XV-D-7. Auxiliary Cooling Systems

The amount of cryogenerator power needed is 24 MW at 300 K. The algorithm used for costing cryogenerators and the complete distribution system is:

$$C = 31,460 (\text{KW}_{300})^{0.7} .$$

Thus the cost of refrigeration, $\$36.635 \times 10^6$, is divided evenly between magnet cooling and cryopumps in the direct convertor and the neutral beams.

XV-D-8. Radwaste Treatment and Disposal

The air detritiation system in the primary containment building is priced at $\$250/\text{m}^3$. The reactor building volume is $100,000 \text{ m}^3$.

XV-E. Balance of Plant Unit Costs

The costs of the Turbine Plant, Electric Plant and Miscellaneous Plant equipment accounts were obtained by using the "Fusion Reactor Design Studies: Standard Cost Estimating Rules," PNL-2987. The same document was used to price out the buildings as shown in Table XV-E-1.

Table XV-E-1

Building Unit Costs

<u>Building</u>	<u>Volume (m³)</u>	<u>Unit Cost (\$/m³)</u>	<u>Cost (\$ x 10⁶)</u>
Reactor Containment	100,000	400	40.00
Service Building	100,000	270	27.00
Hot Cells	20,000	400	8.00
Radwaste	10,000	400	4.00
Turbine	182,500	83	15.15
Maintenance	100,000	100	10.00
Control	2,000	200	0.40
Administration	4,000	200	0.80
Miscellaneous	10,000	200	<u>2.00</u>
Total			107.35

XV-F. Direct Cost Accounts

	<u>\$ x 10⁶</u>
22 Reactor Plant Equipment	
22.01 Reactor Equipment	
22.01.01 Blanket & First Wall	
22.01.01.01 Breeding/Cooling Material	29.353
22.01.01.02 Blanket Structure	27.225
Total (22.01.01)	56.578
22.01.02 Shield	
22.01.02.01 Reflector	42.750
22.01.02.02 Shield	109.685
Total (22.01.02)	152.435
22.01.03 Magnets	
22.01.03.01 Central Cell Coils	136.000
22.01.03.02 Barrier Coils	66.96
22.01.03.03 Transition Coils	58.160
22.01.03.04 Yin-Yang Coils	33.000
Total (22.01.03)	294.12
22.01.04 Supplemental Heating	
22.01.04.01 Barrier ECRH	66.400
22.01.04.02 Barrier Neutral Beams	110.800
22.01.04.03 Plug ECRH	32.800
22.01.04.04 Plug Neutral Beams	36.800
Total (22.01.04)	246.8
22.01.05 Primary Support Structure	
22.01.05.01 Reactor Support	3.675
22.01.05.02 Other	0.500
Total (22.01.05)	4.175
22.01.06 Reactor Vacuum Systems	
22.01.06.01 Direct Convertor	5.544
22.01.06.02 Neutral Beams	10.680
22.01.06.03 Reactor Building	2.000
22.01.06.04 Magnet Vacuum System	7.600
22.01.06.05 Beam Dump Diff. Pumping	1.000
Total (22.01.06)	26.824

22.01.07	Power Supplies		
22.01.07.01	DC/AC/DC Converters	6.600	
22.01.07.02	Magnet Supplies	2.200	
	Total (22.01.07)		8.800
22.02	Main Heat Transfer System		
22.02.01	Primary Coolant		
22.02.01.01	Pumps & Motor Drives	112.000	
22.02.01.02	Piping	55.200	
22.02.01.03	Dump Tanks	1.000	
22.02.01.04	Steam Generators	72.360	
22.02.01.05	Coolant Cleanup System	28.000	
	Total (22.02.01)		268.56
22.02.02	Tritium Extraction		
22.02.02.01	T ₂ from Li ₁₇ Pb ₈₃	1.000	
	Total (22.02.02)		1.000
22.03	Auxiliary Cooling Systems		
22.03.01	Magnet Cooling	18.317	
22.03.01.01	Refrigeration		
22.03.01.02	Distribution System		
22.03.01.03	Storage Tanks		
22.03.02	Shield Cooling	1.500	
22.03.03	Vacuum Pump Cooling	18.317	
22.03.03.01	Refrigeration		
22.03.03.02	Distribution & Storage		
	Total (22.03)		38.134
22.04	Radwaste Treatment & Disposal	10.000	10.000
22.05	Fuel Handling & Storage		
22.05.01	Fuel Purification	3.000	
22.05.02	Pellet Fabrication	0.250	
22.05.03	Pellet Injection	0.500	
22.05.04	Air Detritiation	25.000	
	Total (22.05)	28.750	28.750
22.06	Other Reactor Plant Equipment		
22.06.01	Remote Maintenance Equipment	20.000	20.000

22.07	Direct Convertor		
22.07.01	Vacuum Tank	2.000	
22.07.02	DC Modules	1.660	
22.07.03	Thermal Shield	0.600	
22.07.04	Cryopumps (incl. in 22.03.03)		
22.07.05	Cooling System	3.533	
22.07.06	Power Condit. (incl. in 22.01.07.01)		
22.07.07.	Flux Shaping Coils	3.370	
	Total (22.07)	11.163	11.163
22.08	Instrumentation & Control	18.15	18.15
	Total - Reactor Plant Equipment		1185.49
23	Turbine Plant Equipment		
23.01	Turbine-Generators		
23.01.01	Turbine Generators and Accessories	74.81	
	Turbines		
	Generator		
	Non-Standard Accessories		
	Steam By-Pass		
	Live Steam Reheaters		
	Piping, Value and Heaters for By-Pass System		
	Thermal Insulation		
23.01.02	Foundations	2.30	
23.01.03	Standby Exciters		
23.01.04	Lubricating System		
23.01.05	Gas System		
23.01.06	Reheaters		
23.02	Main Steam System	6.48	
23.03	Heat Rejection Systems	23.52	
23.03.01	Waste Intake Common Facilities		
	Traveling Screens		
	Screen Wash Pumps		
	Chlorination		
23.03.02	Circulating Water Systems		
	Circulating Water-Pumps and Drives		
	Pumps to Condensers and Cooling Tower		

23.03.03	Cooling Towers	
	Foundations and Basins	
	Primary Cooling Towers	
	Waste Heat Cooling Towers	
	Waste Heat System Heat Exchanger	
23.03.04	Other Systems Rejecting Heat to the Atmosphere	
23.04	Condensing Systems	17.16
23.04.01	Condensers	
	Condensers Complete Including Shells, Tube Plates, Water Boxes, Hot Wells, etc.	
	Expansion Joints	
	Foundations	
23.04.02	Condensate System	
	Condensate Pumping Units	
23.04.03	Gas Removal System	
	Vacuum Pumps	
	Air Ejector Equipment	
	Priming Ejectors	
23.04.04	Turbine By-Pass Systems (Excluding Piping)	
23.05	Feed Heating System	11.37
23.05.01	Regenerators and Recuperators	
23.05.02	Pumps	
	Turbine Driven Feed Pumps, Bedplates, etc.	
	Motor Driven Feed Pumps, Bedplates, etc.	
	Turbine Drives	
	Exhaust Ducts	
23.05.03	Tanks	
	Condensate, Chemical, Oil, etc.	
23.06	Other Turbine Plant Equipment	11.97
23.06.01	Turbine Auxiliaries (Excluding Piping)	
23.06.02	Auxiliary Cooling Systems (Excluding Piping)	
23.06.03	Make-Up Treatment (Excluding Piping)	

23.06.04	Chemical Treatment and Condensate Purification		
	Filtration, Aeration, Retention Tanks		
	Demineralization - Regeneration		
	Polishing Units		
	Instrumentation and Control		
	Chemical Feed		
23.06.05	Central Lubrication Service System (Excluding Piping)		
23.07	Instrumentation and Control Equipment	18.05	
	Total - Turbine Plant Equipment		165.66
24	Electrical Plant Equipment		
24.01	Switchgear	16.05	
24.01.01	Generator Circuits		
24.01.02	Station Service		
24.02	Station Service Equipment	29.09	
24.02.01	Station Service and Startup Transformers		
24.02.02	Low Voltage Unit Substation and Lighting Transformers		
24.02.03	Battery System		
24.02.04	Diesel Engine Generators		
24.02.05	Gas Turbine Generator		
24.02.06	Motor Generator Sets		
24.03	Switchboards (Including Heat Tracing)	1.91	
24.03.01	Main Control Board for Electrical System		
24.03.02	Auxiliary Power and Signal Boards		
24.04	Protective Equipment	4.74	
24.05	Electrical Structures and Wiring Containers	25.12	
24.05.01	Concrete Cable Tunnels		
24.05.02	Cable Traps and Supports		
24.05.03	Conduit		
24.05.04	Other Structures		
24.06	Power and Control Wiring	43.09	
24.06.01	Generator Circuits Wiring		
24.06.02	Station Service Power Wiring		
24.06.03	Control Wiring		

24.06.04	Instrument Wiring		
24.06.05	Containment Penetration		
	Total - Electric Plant Equipment		120.00
25	Miscellaneous Plant Equipment		
25.01	Transportation and Lifting Equipment	2.73	
25.01.01	Cranes, Hoists, Monorails and Conveyors		
25.01.02	Railway		
25.01.03	Roadway Equipment		
25.01.04	Watercraft		
25.01.05	Vehicle Maintenance Equipment		
25.02	Air and Water Service Systems	9.14	
25.03	Communications Equipment	1.92	
25.04	Furnishing and Fixtures	1.22	
	Total - Miscellaneous Plant Equipment		15.00
26	Special Materials		
26.01	Reactor Coolant		
26.02	Intermediate Coolant		
26.03	Turbine Cycle Fluids		
26.04	Other Materials (Liquid Helium)	2.8	
	Total - Special Materials		2.80

XV-G. Capital CostsXV-G-1. Direct Costs Summary

<u>Acct. No.</u>	<u>Description</u>	<u>Acct. Total (\$ x 10⁶)</u>	<u>Cost (\$ x 10⁶)</u>
20	Land and land rights	(3.00)	3.00
21	Structures & site facilities		107.35
	Design allowance		10.74
	Contingency (10%)		10.74
	Spare parts (0.5%)	(129.36)	0.53
22	Reactor plant equipment		1185.49
	Design allowance (20%)		237.10
	Contingency (10%)		118.55
	Spare parts (2%)	(1564.85)	23.71
23	Turbine plant equipment		165.66
	Design allowance (10%)		16.57
	Contingency (10%)		16.57
	Spare parts (1%)	(200.46)	1.66
24	Electric plant equipment		120.00
	Design allowance (10%)		12.00
	Contingency (10%)		12.00
	Spare parts (0.5%)	(144.6)	0.60
25	Miscellaneous plant equipment		15.00
	Design allowance (10%)		1.50
	Contingency (10%)		1.50
	Spare parts (1%)	(18.15)	0.15
26	Special materials	(2.80)	2.80
TOTAL DIRECT COSTS			2063.22

XV-G-2. Indirect Costs

91	Construction facilities Equipment & Service (15% of T.D.C.)	309.48
92	Engineering and construction Management service (15% of T.D.C.)	309.48
93	Owner's cost (5% of T.D.C.)	103.16
TOTAL INDIRECT COSTS		722.12
TOTAL DIRECT AND INDIRECT COSTS		2785.34

XV-G-3. Time Related Costs

As mentioned at the start of the chapter the busbar cost and the total capital cost will be based on two economic analysis modes as directed in the DOE "Fusion Reactor Design Studies - Standard Accounts for Cost Estimates".⁽¹⁾ They are the constant dollar mode and the current dollar mode. In the constant dollar mode, interest on capital is taken at 5% and escalation is assumed to be zero. This kind of estimate then reflects the cost of energy comparable with today's cost and in this sense, a zero escalation is justified since the costing is done in present day dollar values. Similarly, the interest on the capital is taken as what it may have been eight years ago when the plant construction was initiated.

In the current dollar economic mode, a 10% interest on capital is taken and escalation is assumed to be at a 5% rate. These values appear somewhat low judging by the present inflationary rates, but since all the users of these standard costing procedures will be guided by them, the estimates will be comparable.

We have assumed an eight-year construction period for WITAMIR-I. This may seem optimistic, but it is difficult to judge since nobody has built a fusion reactor under the assumed conditions of an ongoing developed industry.

The cash flow profile used to determine the F_{IDC} (interest during construction factor) and F_{EDC} (escalation during construction factor) is the classic S-curve biased about 20% to the right. This kind of profile seems to hold for major fissile and nuclear power plants constructed thus far. The factors used below have been taken from Table 2, page 21 of PNL-2648 (Fusion Reactor Design Studies - Standard Accounts for Cost Estimates).⁽¹⁾

<u>Acct. No.</u>	<u>Description</u>	Constant Dollar <u>\$ x 10⁶</u>	Current Dollar <u>\$ x 10⁶</u>
94	Interest during const. F_{IDC} (constant dollar) = 0.170 F_{IDC} (current dollar) = 0.466	473.51	1297.97
93	Escalation during const. F_{EDC} (constant dollar) = 0 F_{EDC} (current dollar) = 0.261	0.000	726.97
	TOTAL TIME RELATED COSTS	473.51	2024.94

<u>Capital Costs Summary</u>		
	Constant Dollar	Current Dollar
	<u>\$ x 10⁶</u>	<u>\$ x 10⁶</u>
Direct Costs	2063.22	2063.22
Indirect Costs	722.12	722.12
Time Related Costs	<u>473.51</u>	<u>2024.94</u>
TOTAL CAPITAL COSTS	3258.85	4810.28
CAPITAL COST OF POWER FROM		
WITAMIR-I (1530 MWe)	\$2130/kWe	\$3144/kWe

XV-H. Operation and Maintenance

The cost of operation and maintenance is taken as 2% per year of the total direct and indirect costs.

Scheduled component replacement cost of 4.54×10^6 was arrived at by assuming that all the first wall/blanket material will be replaced every six years.

In figuring the cost of fuel we assumed that a surplus of tritium will be available for startup purposes from an existing fusion industry. Thus, the fuel cost is only for replenishing the burned lithium and for the purchase of deuterium which is costed at \$1200/kg.

		\$ x 10 ⁶
40-47	Operation and Maintenance	55.71
50-51	Scheduled Component Replacement	4.54
02-03	Annual Fuel Cost	1.00

XV-I. Plant Availability

It is very difficult to select a plant availability for a device that has never been built and tested.

Scheduled downtime for routine blanket replacement can be held to a minimum in WITAMIR-I because of the adopted maintenance procedure. Eleven blanket modules have to be replaced every two years and we feel that can be done in six weeks. Unscheduled shutdowns are estimated at 1 day per week or 52 days per year. Thus a total of 73 days per year will be charged as the downtime, setting the plant availability at 80%.

The plant capacity (using 1530 MWe as the net power output) is therefore:

$$\begin{aligned}\text{Plant Capacity} &= 1530 \times 10^3 \text{ kWe} \times 8760 \text{ hrs/yr} \times 0.80 \\ &= 1.072 \times 10^{10} \text{ kWhr/yr.}\end{aligned}$$

XV-J. Busbar Power Cost

XV-J-1. Constant Dollar Mode

Interest on capital is taken as 10% and no escalation is taken for O & M, component replacement and fuel cost.

$$\begin{aligned}\text{Busbar Cost} &= \frac{[(\text{Total Capital Cost})(\text{Interest Rate}) + (\text{O \& M}) \\ &\quad + \text{Compon. Replacement}) + (\text{Fuel Cost})] 1000 \text{ mills}}{\text{Plant Capacity (kWhr)}} \\ &= \frac{[(3259 \times 10^6)(0.1) + (55.7 \times 10^6) \\ &\quad + (4.54 \times 10^6) + (1.0 \times 10^6)] 1000}{1.072 \times 10^{10}} \\ &= 36.1 \text{ mills/kWhr} .\end{aligned}$$

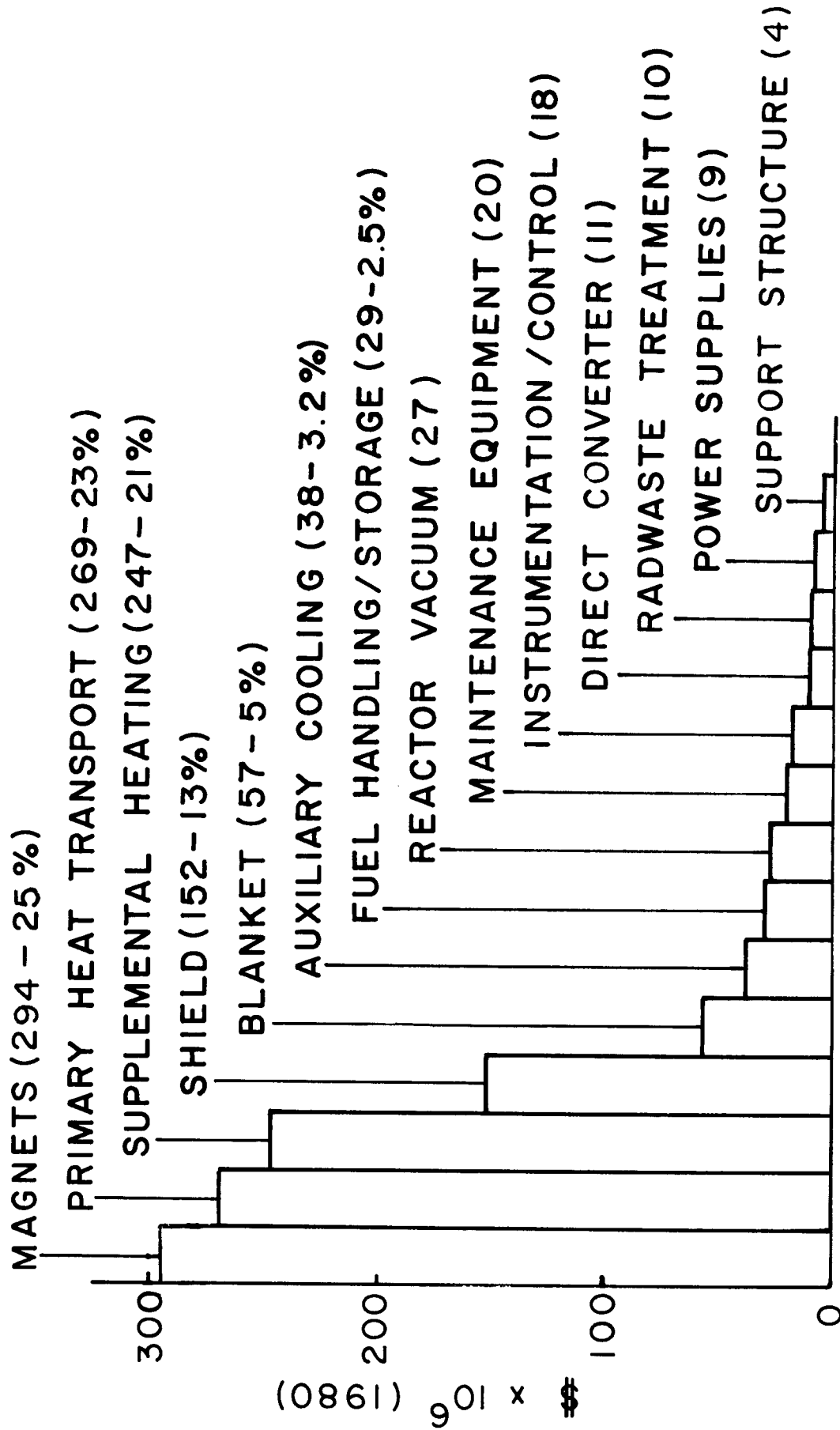
XV-J-2. Current Dollar Mode

In this mode interest on capital is taken at 15% to reflect possible higher rates in the future. Further, an escalation of 5% is assumed for O & M, component replacement and fuel costs.

$$\begin{aligned}\text{Busbar Cost} &= \frac{[(4810 \times 10^6)(0.15) \\ &\quad + (55.7 \times 10^6 + 4.54 \times 10^6 + 1.0 \times 10^6)(1.05)^8] 1000}{1.072 \times 10^{10}} \\ &= 75.7 \text{ mills/kWhr}\end{aligned}$$

XV-K. Results and Discussion

Figure XV-K-1 shows a breakdown of the reactor plant equipment (RPE) costs before the inclusion of contingency, design allowance or spare parts. As in most fusion reactor systems the costs of the magnets dominate, accounting for 25% of the total RPE. It is interesting to note that about 50% of the magnet costs are in the central cell.



BREAKDOWN OF REACTOR PLANT EQUIPMENT COSTS

Primary heat transport is the next largest cost, accounting for 23% of RPE. The bulk of the cost is in the liquid metal pumps and motor drives, the piping, the steam generators and the coolant clean up system. The cost of the coolant itself is included in the blanket account. Obviously the widespread layout of the tandem mirror central cell increases the cost of the piping needed for it. In contrast, the primary heat transport system in NUWMAK⁽²⁾ was only 11% of the RPE.

Supplemental heating, in the form of neutral beams and ECRH, is 21% of RPE. Unlike the tokamak, where the supplemental heating is needed only to ignite the plasma, the tandem mirror depends on neutral beams and ECRH to pump the barriers and maintain the electrostatic potential in the plugs. In NUWMAK⁽²⁾, supplemental heating accounted for only 9% of the RPE.

Finally, the shield and the blanket add up to 18% of the RPE. This is not surprising, since about the same fraction is usually encountered in tokamaks, which are more compact with respect to the blanket and shield. The simple geometry of the blanket in WITAMIR-I has offset the potentially higher cost which would have resulted because of the larger surface area covered by the blanket in the central cell.

It is interesting to note that 86% of the RPE is tied up in the first five items on the bar chart in Fig. XV-K-1. In contrast, the same five items accounted for only 56% of the RPE in NUWMAK.

Another interesting observation is the fact that the cost of the central cell in WITAMIR-I is 58% of the total RPE. In principal, this means that the power output can be doubled with an additional outlay of ~ 60% for the RPE. Obviously, the balance of plant has to be increased accordingly. Balance of plant costs increase according to exponents ranging from 0.3 to 0.7, depending on the system. Thus, there appears to be a definite potential for decreasing the cost of power by going to longer central cell tandem mirrors.

Finally, it is beneficial to compare the cost of power from WITAMIR-I with that of a tokamak. In Table XV-K-1, the power plant costs in \$/kWe and the cost of power in mills/kWhr are compared for WITAMIR-I and NUWMAK.

NUWMAK, which is the latest in a series of tokamak conceptual designs performed at the University of Wisconsin, is a compact, 660 MWe fusion power reactor. The economic analysis for it followed the same ground rules as for WITAMIR-I and can therefore be used as a fair comparison. NUWMAK values have been escalated at 5% per year, from 1978-1980 in the constant dollar mode and

Table XV-K-1

Comparison of Plant and Busbar Costs Between
WITAMIR-I and NUWMAK

	Constant Dollars (1980)	Current Dollars (1988)
<u>Plant Costs</u>		
WITAMIR-I	\$2130/kWe	\$3144/kWe
NUWMAK	\$2227/kWe	\$3288/kWe
<u>Busbar Costs</u>		
WITAMIR-I	36.1 mills/kWhr	75.7 mills/kWhr
NUWMAK	37.5 mills/kWhr	79 mills/kWhr

from 1986-1988 in the current dollar mode. Because NUWMAK only had an output of 660 MWe, it can be said that its cost figures are perhaps on the high side for a tokamak.

Nevertheless, the values indicate that power costs from tandem mirrors are comparable to tokamaks when costed out according to the same ground rules. Furthermore, larger power plants with outputs of 2000-3000 MWe have a definite advantage for the tandem mirror over a tokamak. Increasing the power output in a tandem mirror with thermal barriers entails making the central cell longer. Aside from increasing the pipe lines, it adds nothing to the complexity of the system. This is not true of the tokamak, in which a larger power output implies larger magnets and bulkier blanket/shield modules, making it more unwieldy and more difficult to maintain.

References for Chapter XV

1. Steve C. Schulte, T. L. Willke, J. R. Young, "Fusion Reactor Design Studies - Standard Accounts for Cost Estimates," PNL-2648, Pacific Northwest Laboratory, Richland, Washington, May 1978.
2. B. Badger et al., "NUWMAK - A Tokamak Reactor Design Study," UWFD-330, Fusion Engineering Program, Nuclear Engineering Department, University of Wisconsin, Madison, WI, March 1979.

Appendix AGeneral Reactor Parameters

	<u>Final</u> <u>(9/15/80)</u>
Thermal Power - MW_{th}	3888
DT Fusion Power - MW_{th}	3000
Plasma Q-Value	28.0
Gross Elec. Power - MW_e	1860
Net Elec. Power - MW_e	1530
Recirculating Fraction - %	18
Net Elec. Eff. - %	39.4
<u>Plug</u>	
Density, cm^{-3}	2.73×10^{13}
Ave. Ion Energy, keV	905
Beta	0.64
<u>Central Cell</u>	
Density, n , cm^{-3}	1.51×10^{14}
Ave. Ion Temp., T_i , keV	32.5
Beta	0.4
Overall Reactor Length - m (w/o DC)	207
Central Cell I.D. - m	1.94
Reactor Structural Material	HT-9
Reactor Coolant	$Li_{17}Pb_{83}$
Breeder Material	$Li_{17}Pb_{83}$
Breeding Ratio	1.07
n Wall Loading, MW/m^2	2.4
Max. End Plug Mag. Field - T	6
S/C - End Plug Material	NbTi
Central Cell Mag. Field - T	3.6
S/C - Central Cell Magnets, Material	NbTi
Barrier Pumping Method	NB
End Plug Beam Energy - keV	500
Engineering Power Density, MW/m^3	

TMR Plasma Parameters (continued)

	<u>Final</u> <u>(9/15/80)</u>
<u>Dimensions</u>	
Central Cell Radius, m	0.717
Central Cell Length, m	165.
Plug Radius, m	0.773
Plug Length, m	5.5
Transition Length, m	5.5
Volume Ratio, $V_{\text{cell}}/2 V_{\text{plug}}$	68.9
Barrier Length, m	10
<u>Magnetic Fields</u>	
Central Cell Field, T	3.6
Plug Max. (On-Axis) Field, T	6.
Plug Vacuum Mirror Ratio	1.5
Barrier Vacuum Mirror Ratio	10
Central Cell Vacuum Mirror Ratio	3.9
Plug Ellipticity	29
Barrier Maximum Field, T	14
<u>Power</u>	
Fusion Power, MW_t ($Q_F = 17.6$)	3000
Alpha Power, MW_t	600
Neutron Wall Loading - Central Cell, MW/m^2	2.4
Surface Heat Load - Central Cell, W/cm^2	2.0
Surface Heat Load - Barrier, W/cm^2	50
Surface Heat Load - Plug, W/cm^2	3.0
Plasma Power Density - Central Cell, W/cm^3	11.3
Power to Direct Convertor, MW_t	502

TMR Plasma Parameters (continued)

	<u>Final</u> <u>(9/15/80)</u>
<u>Plug Heating</u>	
NB Trapping Fraction	0.128
Neutral Beam Power, MW	2.35
Neutral Beam Energy, keV	500
ECRH Power, MW	16.5
ECRH Freq., GHz	112
<u>Particle Balance</u>	
Axial Loss Rate, s^{-1}	
D	3.89×10^{21}
T	3.89×10^{21}
α	1.07×10^{21}
p	2.94×10^{19}
Beam Refuelling Rate - Plug, s^{-1} each plug	1.47×10^{19}
Beam Refuelling Rate - Central Cell, s^{-1}	2.80×10^{21}
Pellet Refuelling Rate - Central Cell, s^{-1}	7.12×10^{21}
Fractional Burnup, %	21.6
Barrier Trapping (current) Amp. equiv.	1092
<u>Physics Parameters</u>	
<u>Central Cell</u>	
Ion Density, cm^{-3}	1.51×10^{14}
Ion Temperature, keV	32.5
Electron Temperature, keV	32.8
Beta	0.4
$n\tau_{ic}$, $s\ cm^{-3}$	7.82×10^{14}
$n\tau_{ec}$, $s\ cm^{-3}$	7.16×10^{14}

TMR Plasma Parameters (continued)

	<u>Final</u> <u>(9/15/80)</u>
<u>Plug</u>	
Ion Density, cm^{-3}	2.73×10^{13}
Plug Species	p
Mean Ion Energy, keV	905
Electron Temperature, keV	123
Beta	0.64
$n\tau_{ip}$, s cm^{-3}	9.83×10^{13}
<u>Electrostatic Potentials</u>	
Central Cell - Ground, kV	224
Plug - Central Cell, kV	102
Central Cell - Barrier, kV	141
<u>Barrier Cell</u>	
Beta	0.235
Pumping Parameter	2
Power Drain or Input, NB, MW	55.2
Ion Density, cm^{-3}	6.92×10^{12}
ECRH Power to Plasma, MW	33.3
ECRH Frequency, GHz	40

Blanket and Shield Parameters

	Final (9/15/80)
Blanket Structure & Material	HT-9
Blanket Coolant	Li ₁₇ Pb ₈₃
Breeding Material	Li ₁₇ Pb ₈₃
<u>Blanket Composition, Vol. %</u>	
Structure %	9 ~ 25
Coolant/Breeder %	81 ~ 75
Void	10 ~ 0%
<u>Blanket Dimensions</u>	
Length Central Cell - m	165
Inside Diameter Central Cell - m	1.94
Blanket Thickness - m	.73
Reflection Thickness - m	.28
Shield Thickness - m	0.6
Length Each Transition Zone - m	15.5
End Cell Length, m	5.5
Total DT Neutron Power-MW	2400
Blanket Multiplication	1.37
Power in Central Cell First Wall - MW	20
Power in Central Cell Blanket - MW	3317
Power in Central Cell Shield - MW	9
Surface Power in Transition Zone	60
Surface Power in End Cell	< 1
Neutron Wall Loading MW/m ² (14 MeV)	
Central Cell	2.4
Transition Zone	
End Zone	
First Wall Heating Load W/cm ²	
Central Cell	2
Transition Zone	50
End Zone	3

Blanket and Shield Parameters (continued)

	Final (9/15/80)
Maximum Energy Deposition W/cm ³	
Central Cell	15
Transition Zone	
End Zone	
First Wall Thickness mm	2
Coolant Temperature °C	
Inlet	329
Outlet	500
Maximum Coolant Velocity cm/s	12.5
Coolant Pressure Drop MN/m ²	0.69
Maximum Blanket Pressure MN/m ²	1.15
Maximum Structure Temperature °C	530
Maximum Structure Stress MN/m ²	50
Total Coolant Flow kg/h	4.4×10^8
Total Steam Flow kg/h	6.3×10^6
Steam Pressure MN/m ²	16.6
Steam Temperature °C	468
Feed Water Temperature °C	219
Steam Generator Surface m ²	1.6×10^4
Reheat Temp. °C	468
Gross Thermal Efficiency %	42
Gross Plant Output (Thermal Cycle) - MW _e	1530
Direct Convertor Output - MW _e	330
Total Plant Gross Output - MW _e	1860
Recirculating Power - MW _e	330
Magnet	12
Injection Power	246 (at $\eta=0.5$)
Vacuum	12
Coolant Circulation	60 (at $\eta=0.5$)
Net Plant Output - MW _e	1530
Net Plant Efficiency - %	39.4

Tandem Mirror Neutronics Parameters(Based on 1 MW/m² Wall Load)

	Final (9/16/80)
<u>Blanket & Shield</u>	
Breeding Ratio	
⁶ Li	1.043
⁷ Li	.025
Total	1.068
Energy Deposited per 14.1 MeV Neutron	
Neutron	7.166 MeV
Gamma	12.216 MeV
Total	19.38 MeV
Energy Multiplication	1.38
<u>First Wall</u>	
Radius	96.7 cm
Volume/Unit Length	182.6 cm ³ /cm
Thickness	.3 cm
Power Density	
Neutron	4.41 W/cm ³
Gamma	3.52 W/cm ³
Total	7.93 W/cm ³
DPA per year/MW/m ²	16.88
H production ppm/yr	482
He production ppm/yr	117
Energy Deposition per 14.1 MeV Neutron	
Neutron	.187 MeV
Gamma	.149 MeV
Total	.336 MeV
<u>Breeding Blanket</u>	
Radius	97 cm
Volume/Unit Length	60,700. cm ³ /cm
Thickness	72.5 cm

Tandem Mirror Neutronics Parameters (continued)

	Final (9/16/80)
Power Density	
Neutron	.490 W/cm ³
Gamma	.722 W/cm ³
Total	1.212 W/cm ³
Energy Deposition per 14.1 MeV Neutron	
Neutron	6.90 MeV
Gamma	10.17 MeV
Total	17.07 MeV
<u>Reflector</u>	
Radius	169.5
Volume/Unit Length	32,283 cm ³ /cm
Thickness	28 cm
Power Density	
Neutron	.0078 W/cm ³
Gamma	.249 W/cm ³
Total	.257 W/cm ³
Energy Deposition per 14.1 MeV Neutron	
Neutron	.058 MeV
Gamma	1.86 MeV
Total	1.92 MeV
<u>Shield</u>	
Radius	197.5 cm
Volume/Unit Length	85,765 cm ³ /cm
Thickness	60 cm
Power Density	
Neutron	.0012 W/cm ³
Gamma	.0017 W/cm ³
Total	.0029 W/cm ³
Energy Deposition per 14.1 MeV Neutron	
Neutron	.023 MeV
Gamma	.034 MeV
Total	.057 MeV
Percent of Total Energy	.29%

Tandem Mirror Neutronics Parameters (continued)

	Final (9/16/80)
<u>Magnets</u>	
Thermal Insulation (Mylar)	
Radiation Dose	
Midplane	8.6×10^5 rad/yr
Header	1.6×10^6 rad/yr
Electrical Insulation (Epoxy)	
Radiation Dose	
Midplane	6.4×10^5 rad/yr
Header	1.5×10^6 rad/yr
DPA in Al per yr/MW/m ²	2.73×10^{-7}
Power Density	
Neutron	1.12×10^{-8} W/cm ³
Gamma	6.30×10^{-8} W/cm ³
Total	7.42×10^{-8} W/cm ³
Radius	330. cm
Volume/Unit Length	238,760 cm ³ /cm
Thickness	100 cm

Tritium Considerations

		Final (9/15/80)
Fractional Burnup of T in Central Cell		21.6%
<u>Fueling</u>		
Central Cell		pellets; N.B.I.
Fueling rate*		
D (atoms/s)	Beam	2.16×10^{21}
	Pellets	2.8×10^{21}
T (atoms/s)	Pellets	4.96×10^{21}
<u>Plugs</u>		
Isotope Injected		H
Injection rate* (atoms/s)		2.94×10^{19}
Neutral Beam Injectors		
Fraction of gas handled in beam lines but not injected		0.86
<u>Exhaust</u>		
Particle Flow Rate into Direct Convertor		
T (atoms/s)		3.89×10^{21}
D (atoms/s)		3.89×10^{21}
H (atoms/s)		2.94×10^{19}
He (atoms/s)		0.13×10^{21}
Direct Convertor		
T inventory due to implantation		
Coolant type		
Coolant flow rate (kg/hr)		
T loss to coolant (Ci/d)		
Grid material		
Grid erosion (kg/d)		

*Including any atoms not trapped in plasma.

Tritium Considerations (continued)

	<u>Final</u> <u>(9/15/80)</u>
Thermal Dump	
T inventory due to implantation	
Coolant type	
Coolant flow rate (kg/hr)	
T loss to coolant (Ci/d)	
Grid material	
Grid erosion (kg/d)	
<u>Blanket</u>	
Material	Li ₁₇ Pb ₈₃
Temperatures (°C)	
High	500
Low	329
Average	415
Flow Rate (kg/hr)	4.4×10^8
Total Mass of Breeding Material (kg)	8.4×10^6
Tritium	
Pressures (torr)	
At high temperature	1×10^{-4}
At low temperature	1×10^{-4}
At average temperature	1×10^{-4}
Concentration (wppm)	0.012
Extraction scheme	Molten Salt
<u>Steam Generator Tubes</u>	
Composition	Double Walled HT-9
Thickness (mm)	1 + 1
Surface Area (m ²)	1.6×10^4
Temperatures (°C)	
Coolant in	500
Coolant out	329
H ₂ O in	266
H ₂ O out	482

Tritium Considerations (continued)

	Final (9/15/80)
Pressure H ₂ O (psi)	2400 (16.6 MPa)
T permeation to H ₂ O (Ci/d)	10
<u>Tritium Inventory (g)</u>	
Fueling & Exhaust	
Neutral beam cryopumps	1.0
Reactor chamber	0.1
Direct convertor	?
Direct convertor coolant & coolant processing	?
Direct convertor cryopumps (time pumps on-line)	28.0 (4 hrs)
Cryogenic distillation columns	125
Fuel Cleanup	20
Pellet Injector	20
Blanket	
Breeding material	100
Breeder processing	7
Structural material & piping	
Steam generator piping	
Total Active Tritium Inventory, g	452
Storage for 1 day, g	2136
<u>Containment</u>	
Total Building Volumes (m ³)	
Volume of Reactor Hall (m ³)	
Pressure in Reactor Hall (torr)	

Radioactivity

	<u>Final</u> <u>(9/15/80)</u>
Operating Time (years)	
<u>Activity at Shutdown (Ci/Watt)</u>	
Central Cell	
Blanket	
Structure	0.7
Coolant	
Reflector	0.19
Shield	5.7×10^{-4}
Magnets	6.4×10^{-6}
<u>Afterheat at Shutdown (Watts)</u>	
Central Cell	
Blanket	
Structure	1.22×10^3
Reflector	3.6×10^2
Shield	6.6×10^{-1}
Magnets	2.2×10^{-2}
<u>BHP at Shutdown (Air) $\text{km}^3(\text{air})/\text{kW}_{\text{th}}$</u>	
Central Cell	
Blanket	
Structure	49
Reflector	8.3
Shield	1.7×10^{-2}
Magnets	2.2×10^{-4}
<u>Dose Outside Shield mrem/hr at Shutdown</u>	
Central Cell	3.93
Magnet	1.1