New Analysis of First Wall Lifetime Considerations for Fusion Reactors

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March 1977

UWFDM-212

FUSION TECHNOLOGY INSTITUTE

UNIVERSITY OF WISCONSIN

MADISON WISCONSIN

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These two seminars relate recent results on two fundamental aspects of fusion reactors, materials behavior and induced radioactivity in a fusion neutron spectrum. The results we have obtained on first wall lifetime suggest that very long lifetimes may be possible in stainless steels if the structure is operated at temperatures of 300°C or less. The limiting factors for structure lifetime are excessive swelling and loss of fracture toughness. We find one should design to stress limits rather than ductility limits and one should minimize swelling to minimize differential stresses. Swelling varies strongly with temperature and one can reduce this problem significantly by operating the structure at temperatures below 400-600°C.

To retain reasonable power cycle efficiency, these results suggest we should seek blanket designs which decouple the structure temperature from the heat transport medium. The flowing lithium oxide blanket concept developed earlier at Wisconsin admits this possibility and we find one trades only 1-3% in power cycle efficiency in order to operate the first wall at 250-300°C. Further, this operating temperature permits straightforward boiling water cooling of the first wall without excessive pressure and isothermal operation which helps minimize stresses and differential swelling.

In sum, much longer first wall lifetimes than heretofore considered may be possible with 316 stainless steel using practical fusion blanket designs that produce thermal efficiencies in the range from 34 to 38%. That fusion systems can have reliable materials performance will also help to achieve good plant availability factors. In addition, less solid radioactive waste is generated and the second seminar describes the possibilities for further minimizing this problem.

Long term radioactivity is not inherent to the deuterium-tritium fusion cycle. Rather, radioactivity induced by neutrons will depend on the materials used to construct the reactor. As such, the levels of induced radioactivity can be controlled by appropriate selection of elements in a structural alloy and, in principle, by selection of specific isotopes of a given element in an alloy. We have found that by using a combination of alloy selection and/or isotopic tailoring, the long term radioactivity levels in steels can be reduced significantly. In one case, the radioactivity level at 60 years after reactor shutdown is six orders of magnitude below the comparable level in 316 stainless steel. This 60 year level is already three orders of magnitude below the 316 S.S. activity level at one million years. Practical steel alloys have been considered along with potentially practical isotope tailoring levels. Examples are also given for molybdenum alloys.

Methods for isotopic tailoring based on laser isotope separation have been examined and economic estimates have been made based on the physics of the separation process and current experience with laser costs. The technique appears to be economically attractive, especially when isotope selection is required on minority elements in an alloy (for example, Ni or Mo in steel).

Overall, one can conclude that induced radioactivity is controllable in fusion by alloy selection and isotopic tailoring. Thus, the potential exists for restricting the long term solid radwaste disposal problem to a few human generations. When coupled with long first wall lifetimes (which reduces the amount of material subject to irradiation per MW generated), these results indicate very positive engineering advantages for controlled fusion reactors.

Printed copies of the slides used in the seminars are attached together with a brief writeup on the first wall lifetime problem and two summary papers accepted for presentation and publication at the June, 1977 American Nuclear Society Meeting in New York.

SEMINAR TOPICS

1. LIFETIME OF FIRST WALLS IN FUSION REACTORS
   a. LIFETIME CRITERIA (W. WOLFER)
   b. POTENTIAL FOR LONG LIFETIMES WITH STAINLESS STEELS (WOLFER)
   c. INFLUENCE OF LIFETIME CRITERIA ON BLANKET DESIGN (CONN)
   d. IMPACT ON PLANT AVAILIBILITY, ECONOMICS, AND RADWASTE DISPOSAL (CONN)

2. MINIMUM RADIOACTIVITY IN STAINLESS STEELS AND OTHER ALLOYS
   a. ISOTOPIC TAILORING (CONN)
   b. ALLOY CHOICES (CONN)
   c. POTENTIAL ROLE OF LASER ISOTOPE SEPARATION (JOHNSON)

3. SUMMARY – POTENTIAL IMPACT OF THESE FINDINGS FOR FUSION (CONN)

Design Considerations for the First Wall and Blanket Structure of a Fusion Reactor

1. Introductory Remarks

It has been widely recognized that the foremost factors which limit the life of the first wall structure are

(a) excessive swelling

(b) loss of ductility

(c) and surface erosion.

The objective of this presentation is to outline a design concept in which the impact of these limiting factors is greatly reduced. Furthermore, an attempt is made to quantify these factors in terms sufficiently general as to be applicable to various fusion concepts, and to identify critical areas of material behavior where more understanding and experiments are needed.

This presentation deals only with the limiting factors (a) and (b). It will be assumed that, if necessary, the first structural wall is protected by a sacrificial wall or material whose major functions are to prevent blistering and ion sputtering in the first wall, and, in the case of laser fusion reactors, convert the fluctuating radiation heat into a steady heat input to the first wall.(1)

The lifetime consideration for the first wall and blanket structure then become centered around swelling, embrittlement, and irradiation creep. All three phenomena depend on the temperature. Therefore, these considerations are tightly linked to the thermal plant efficiency for most of the previously proposed blanket designs. However, a recent blanket concept developed by D. K. Sze et al.[2] and utilized by Abdel-Khalik and Conn[3,1] has made it possible to uncouple the selection of the first wall temperature from the heat transport system. In this concept, the blanket material and heat transport system consists of a bed of Li_2O particles flowing through the blanket under the gravitational force. Most of the neutron energy is deposited in the blanket and the heat is carried away with the blanket material. The first wall is cooled separately by boiling water, and it can be kept at a uniform and sufficiently low temperature to reduce swelling.

The design considerations for the blanket structure and first wall will be concentrated on the following questions:

a. What is the optimal first-wall temperature?

b. How much swelling can be tolerated?

c. How can fracture be avoided or delayed?

All these questions are strongly interrelated because of the nonuniform swelling produced by temperature and flux gradients. The differential swelling produces stress gradients which in turn are relaxed by irradiation creep, and the stresses in the structure together with the fracture toughness determine the probability of failure. This, in short, is the outline and common thread for the following considerations.

2. Swelling

When extrapolating our experience on swelling to a fusion environment, two major differences are recognized between fission and fusion neutron damage. First, the collision cascade size is substantially larger for fusion neutrons. Nevertheless, this difference is not expected to introduce qualitative differences in the swelling behavior of a material, although it may affect the relative damage efficiency of a 14 MeV neutron versus a 1 MeV neutron. The second difference, however, is of utmost concern, namely the production of gas atoms via (n,p) and (n,α) reactions. A third aspect is related to the temperature range for which we have a large amount of swelling data. Since EBR-II has an inlet temperature of about 370°C, there exists very little information on swelling at lower temperatures.

Therefore, in the following assessment of swelling in a fusion reactor, simple theoretical models will be employed in conjunction with experimental data on swelling in type 316 stainless steel, irradiated in EBR-II and HFIR. The high helium production rate in HFIR revealed that there are two major effects of insoluble gases on swelling.(4) At temperatures above 0.5 T_m (T_m is the melting point), swelling is due to bubble growth. At lower temperatures, swelling is bias driven but due to helium it commences earlier.

The former effect can be described in a simple model. If N is the void number density, γ the surface energy, k the Boltzmann constant, and T the absolute temperature, then the swelling is given by

$$S = \left(\frac{3}{4\pi N}\right)^{1/2} \left(\frac{C_{He}kT}{2\gamma}\right)^{3/2} \tag{1}$$

for large helium concentrations C_{He} and for $T \geq 0.5\ T_m$. The latter condition means that in order to obtain equilibrium bubbles, there must be self-diffusion. The void number density itself is a function of temperature, and the experimental data(5) on type 316 SS irradiated in EBR-II can be approximated by a formula

$$N = N_0 \exp(Q/kT)$$

where $N_0 = 4.16 \times 10^7$ cm^{-3} and $Q = 1.05$ eV. Using a surface energy of $\gamma = 1000$ ergs/cm^2, the bubble swelling is obtained as shown in Fig. 1. The uppermost curve corresponds to 1% He or about 30 years of exposure at 7.64 MW/m^2.(1) The data points represent the experimental result of the HFIR irradiations for $T \geq 550^{o}$C with the concentrations ranging from 2000 to 4300 appm.(4) The agreement with the results of Eq. (1) is satisfactory.

Below a temperature of 550^{o}C swelling is bias driven as will be shown shortly, and it is in excess of what would be predicted according to Eq. (1). Nevertheless, helium does have an effect on swelling by enhancing the void nucleation. Although there is as yet no consistent theory to determine the number of voids nucleated after a certain dose, it is possible to show the propensity for void nucleation with the steady-state void nucleation theories. An example of this is shown in Fig. 2, which is based on calculations by Wiedersich and Hall.(6) It can be seen that He increases the void nucleation rate at all temperatures, though the effect is less pronounced at low temperatures. These results show that the He production will reduce the incubation dose and thereby affect swelling even below 550^{o}C. Furthermore, the final void number density may also depend on the He concentration, but the bias-driven growth rate should not be affected. Hence, it is assumed that the steady-state swelling rate at temperatures below 550^{o}C can be computed with a void growth model and sink densities as observed in EBR-II irradiated SA 316 stainless steel.(5) These computations have been carried out recently and reported in Ref. 1. The bias parameter was evaluated by matching the computed swelling rate, R, to the observed one at T = $500^{o}C$. Two estimates were then made, for a nominal case where the void sink strength, $4\pi Nr$ (r is the average void radius), is equal to 10^{10} cm^{-2} and smaller than the dislocation sink strength, and for a maximum case where void and dislocation sink strengths are equal. These estimates are shown in Fig. 3 for a displacement rate of 51 dpa/yr. Since the corresponding He production rate is about 800 appm/yr, we see that the bias-driven swelling rate is much greater than the gas-driven swelling rate for all temperatures below $550^{o}C$. Adopting previous assumptions that an upper limit of 10% swelling is tolerable for the first wall, it appears feasible that an operating temperature of $300^{o}C$ and a wall loading of 7.64 MW/m^2 could result in a lifetime equal to the plant life.

Whether or not the loss of ductility may become the limiting factor will be discussed next.

3. Stresses in the Blanket Structure

Even if temperature gradients can be limited to acceptable levels, there are inevitably flux or displacement gradients across the blanket structure. Hence, there are swelling gradients which produce stresses. Irradiation creep in turn relaxes these stresses, and the final stress distribution reached depends then on differential swelling and on the irradiation creep rate. The latter is also temperature dependent, as can be seen from Fig. 4. It shows the irradiation creep compliance $\psi = \dot{\varepsilon}/\sigma$ for 20% CW 316 stainless steel(7) and for doses greater than 20 dpa.

The exact stress distribution through the blanket structure can only be evaluated for a specific design. Nevertheless, it is possible to assess the magnitude of the stresses without knowing the details of the structure. For example, we can consider a beam with a lateral flux gradient across it and compute the stress distribution under generalized plane strain. Since the elastic and the creep strain rate must be equal to the differential swelling rate, one obtains

$$\frac{1}{E}\frac{d\sigma}{dt} + \psi\sigma = \frac{1}{3}(\overline{R} - R) \tag{2}$$

where $\overline{R}$ is the average and R the local swelling rate. If we assume for simplicity sake that the creep compliance ψ and the swelling rate R are time-independent, then the integration of Eq. (2) gives

$$\sigma(r,t) = \sigma_0 \exp(-E\psi t) + \frac{1}{3}\frac{\overline{R}-R}{\psi}[1 - \exp(-E\psi t)] \tag{3}$$

where σ_0 is the initial thermal stress. This stress decays with a time constant of $(1/E\psi)$ which, for the initial irradiation creep rate, is of the order of 1.6 months at a dose rate of 51 dpa/year.

The second term gives the stress generated by the differential swelling rate $(\overline{R} - R)$. This stress saturates at a maximum value given by

$$\sigma(r,\infty) = \frac{1}{3}(\bar{R} - R)/\psi \; . \tag{4}$$

The saturation stress is greatest for a beam constrained rigidly in the direction perpendicular to the swelling gradient direction, i.e. for $\overline{R} = 0$.

The absolute magnitude of this saturation stress is plotted in Fig. 5 as a function of temperature. It is found that even for the maximum swelling rate, the stresses due to differential swelling are below 20 ksi for temperatures below 350^{o}C.

4. Fracture Toughness

Recent irradiation experiments in HFIR on type 316 stainless steels have shown[4] that the radiation and helium embrittlement leads to zero ductility at temperatures above 550^{o}C due to integranular failure. Below this temperature, the material appears to retain a small value of ductility as measured by the macroscopic fracture strain in uniaxial extension. The fracture mechanism is then by plastic instability on a localized scale. Since the irradiated material even in the annealed condition has a high yield strength below 400^{o}C, the major concern about embrittlement must be focused on fracture toughness rather than uniaxial fracture strain. It is well known that the latter is not a good measure of the fracture toughness.

Therefore, an attempt is being made here to estimate the fracture toughness with a model developed by Malkin and Tetelman.[8] This model relates the Mode I fracture toughness to the yield stress σ_y, the decohesion stress σ_f, and the minimum root radius ρ_0. The latter is of the order of the grain size and taken to be 0.002 inches. Below this minimum value the fracture toughness is found to be independent of the root radius for unirradiated materials.

It was argued previously,[1] that ρ_0 would be of the width of the plastic channels observed in highly irradiated steels after plastic deformation. However, a study of the extent of plastic deformation in the vicinity of fractive surfaces had shown[9] that many plastic channels emanated from the fracture surface up to distances of 3/16 inch. Hence, the localization of plastic flow at a crack tip appears to be at least as large as the grain size. Therefore, it was assumed that ρ_0 should remain the same for irradiated materials.

The fracture toughness K_{Ic} is given by[8]

$$K_{Ic} = 2.89\ \sigma_y \sqrt{\exp\left[\frac{\sigma_f}{\sigma_y} - 1\right] - 1}\ \sqrt{\rho_0}\ (\mathrm{psi}\ \sqrt{\mathrm{inch}})\ ,$$

where σ_f was estimated earlier[1] to be about 235 ksi. The yield stress in HFIR irradiated 316 stainless steel shows a strong temperature dependence,[4] and the following values listed in Table 1 were selected to reflect this dependence. The calculated value of the fractive toughness is listed in the last column. These values are greater by slightly more than a factor of 20 than previous estimates[1] for which ρ_0 was assumed to be 1000 Å, the width of a plastic channel. This great uncertainty as well as the perhaps questionable application of Tetelman's model strongly emphasizes the need for measurements on fracture toughness on irradiated materials. It also demonstrates that a better understanding of the fracture mechanism will be of great benefit to the development of fusion technology. This will remain true for whatever material is being considered for the first wall and the blanket structure, since similar radiation hardening is expected for any structural material.

Table 1 Fracture Toughness
Estimate for Irradiated Stainless Steel

$T(^{o}C)$	σy (ksi)	$K_{Ic}/\sqrt{\rho_0}$ (ksi)	K_{Ic} (ksi $\sqrt{\text{inch}}$)
250	120	440	19.7
300	115	451	20.2
350	110	462	20.7
400	90	521	23.3
450	62	700	31.3
500	40	1320	58.9

The fracture toughness and the stresses in the blanket structure determine the critical crack size. If we assume a through-crack of length 2a in a plate member of the structure, then the critical crack size is given by

$$a = \frac{1}{\pi}\left(\frac{K_{Ic}}{\sigma}\right)^2 .$$

Using the values for the saturation stress, the critical crack size can be calculated, and the results are shown in Fig. 6.

Although the critical crack size increases again at 500^{o}C, one approaches the temperature where integranular failure occurs. Since the detection of cracks of 0.5 inches should not present any difficulties, these results indicate an operating temperature around 300^{o}C for the first wall.

5. Conclusions

Considerations on swelling, irradiation creep, and embrittlement in type 316 stainless steels indicate that this material, if used for the first wall and the blanket structure in a fusion reactor, may have a lifetime equal to the plant life. However, it is necessary to protect the first wall by a sacrificial wall, and to operate the structural parts in a temperature range of 250 to 300^{o}C.

Acknowledgement

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3. S. Abdel-Khalik, R. W. Conn, "First Wall and Blanket Design for an ICTR," Trans. Amer. Nucl. Soc. (June 1977), to be published.

4. E. E. Bloom, F. W. Wiffen, P. J. Maziasz, and J. O. Stiegler, Nuclear Techn. 31, 115 (1976).

5. H. R. Brager, J. L. Straalsund, J. Nucl. Matls. 46, 136 (1973).

6. H. Wiedersich, B. O. Hall, to be published in J. Nucl. Matls.

7. E. R. Gilbert, "Dependence of Irradiation Creep on Temperature and Atom Displacements in 20% Cold Worked Type 316 Stainless Steel," HEDL-SA-824, April 1976.

8. J. Malkin, A. S. Tetelman, Engineering Fracture Mechanics 3, 151 (1971).

9. J. E. Flinn et al., "Evaluation of Ex-Reactor Loading Event on High-Fluence EBR-II Control-Rod Thimble 5E3," ANL/EBR-068, February 1973.

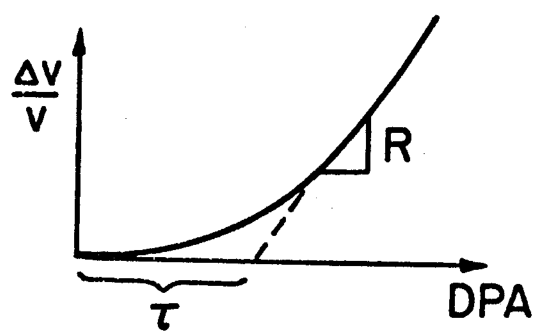
THE LIMITING FACTORS FOR THE LIFETIME OF THE FIRST WALL AND BLANKET

1. EXCESSIVE SWELLING
2. LOSS OF FRACTURE TOUGHNESS
3. SURFACE EROSION

CONCEPTS FOR OBTAINING LONG FIRST WALL LIFETIMES

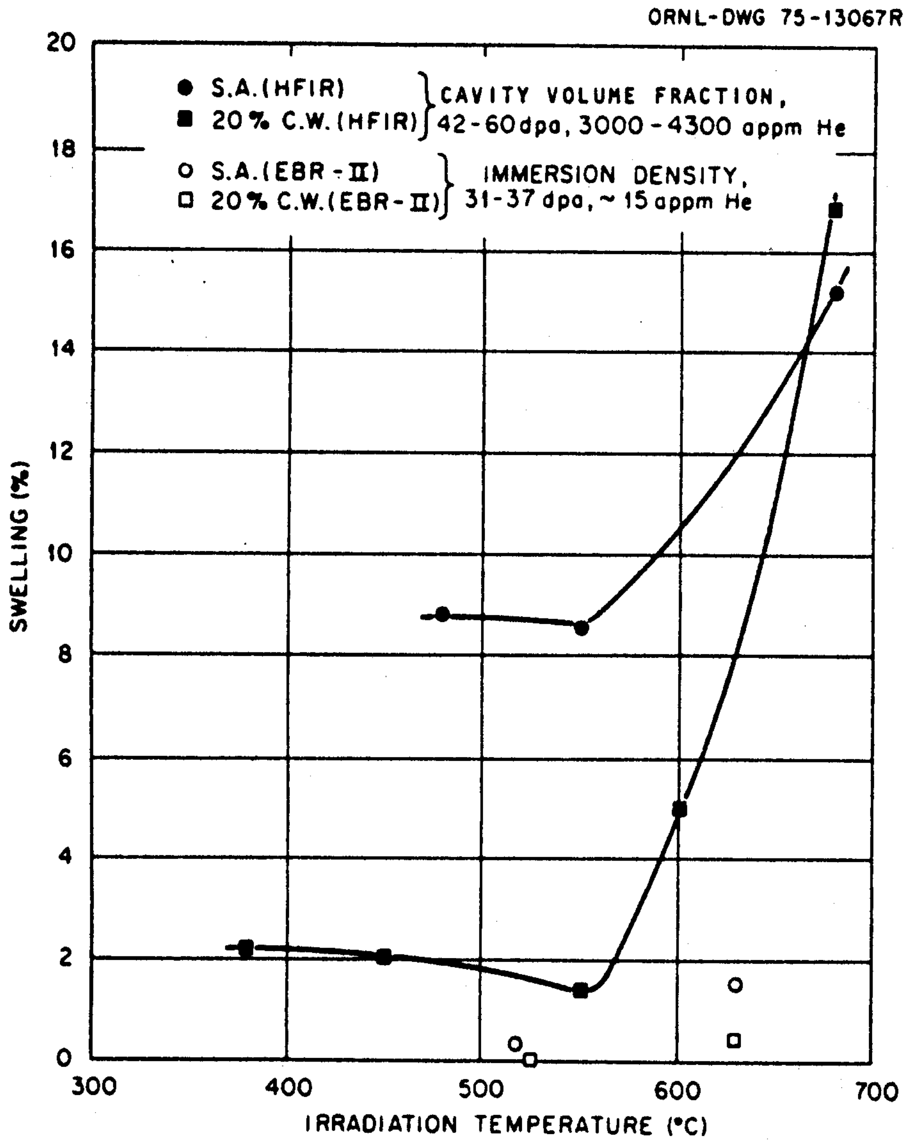
1. CRITERIA FOR SWELLING

- MAXIMIZE INCUBATION DOSE, τ
- MINIMIZE STEADY-STATE SWELLING RATE, R



2. CRITERIA FOR FRACTURE TOUGHNESS

- LOW PRESSURE COOLANT SYSTEMS
- MINIMIZE THERMAL STRESSES
- MINIMIZE DIFFERENTIAL SWELLING
- AVOID RIGID CONSTRAINTS
- AVOID STRESS CONCENTRATIONS
- IMPROVE FRACTURE TOUGHNESS



ORNL-DWG 75-13067R
S.A. (HFIR)
20% C.W. (HFIR)
CAVITY VOLUME FRACTION, 42-60dpa, 3000-4300 appm He
S.A. (EBR-II)
20% C.W. (EBR-II)
IMMERSION DENSITY, 31-37 dpa, ~15 appm He
SWELLING (%)
0
2
4
6
8
10
12
14
16
18
20
300
400
500
600
700
IRRADIATION TEMPERATURE (°C)

SWELLING AT HIGH TEMPERATURES
$T > 0.5\,T_M$

- INCUBATION DOSE IS REDUCED BY HELIUM

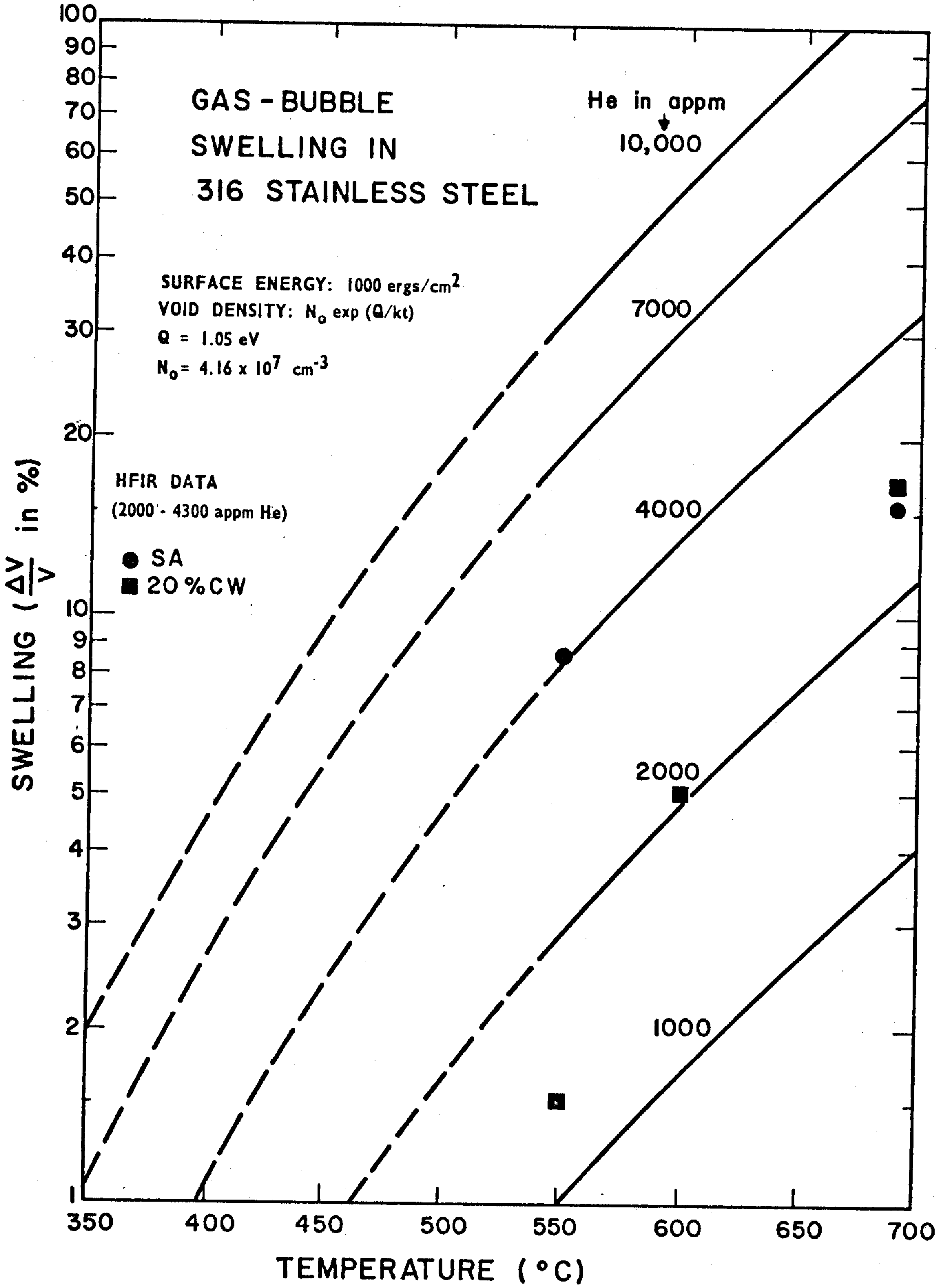
- SWELLING IS GAS-DRIVEN

$$S = \left(\frac{3}{4\pi N}\right)^{1/2} \left(\frac{C_{He}\,kT}{2}\right)^{3/2}$$

VOID NUMBER DENSITY

$$N = N_o \exp(Q/kT)$$

Figure 1


GAS - BUBBLE
SWELLING IN
316 STAINLESS STEEL
He in appm
10,000
7000
4000
2000
1000
SURFACE ENERGY: 1000 ergs/cm^2
VOID DENSITY: N_o exp (Q/kt)
Q = 1.05 eV
N_o = 4.16 x 10^7 cm^{-3}
HFIR DATA
(2000 - 4300 appm He)
SA
20 % CW
SWELLING ($\frac{\Delta V}{V}$ in %)
TEMPERATURE (°C)
100
90
80
70
60
50
40
30
20
10
9
8
7
6
5
4
3
2
1
350
400
450
500
550
600
650
700

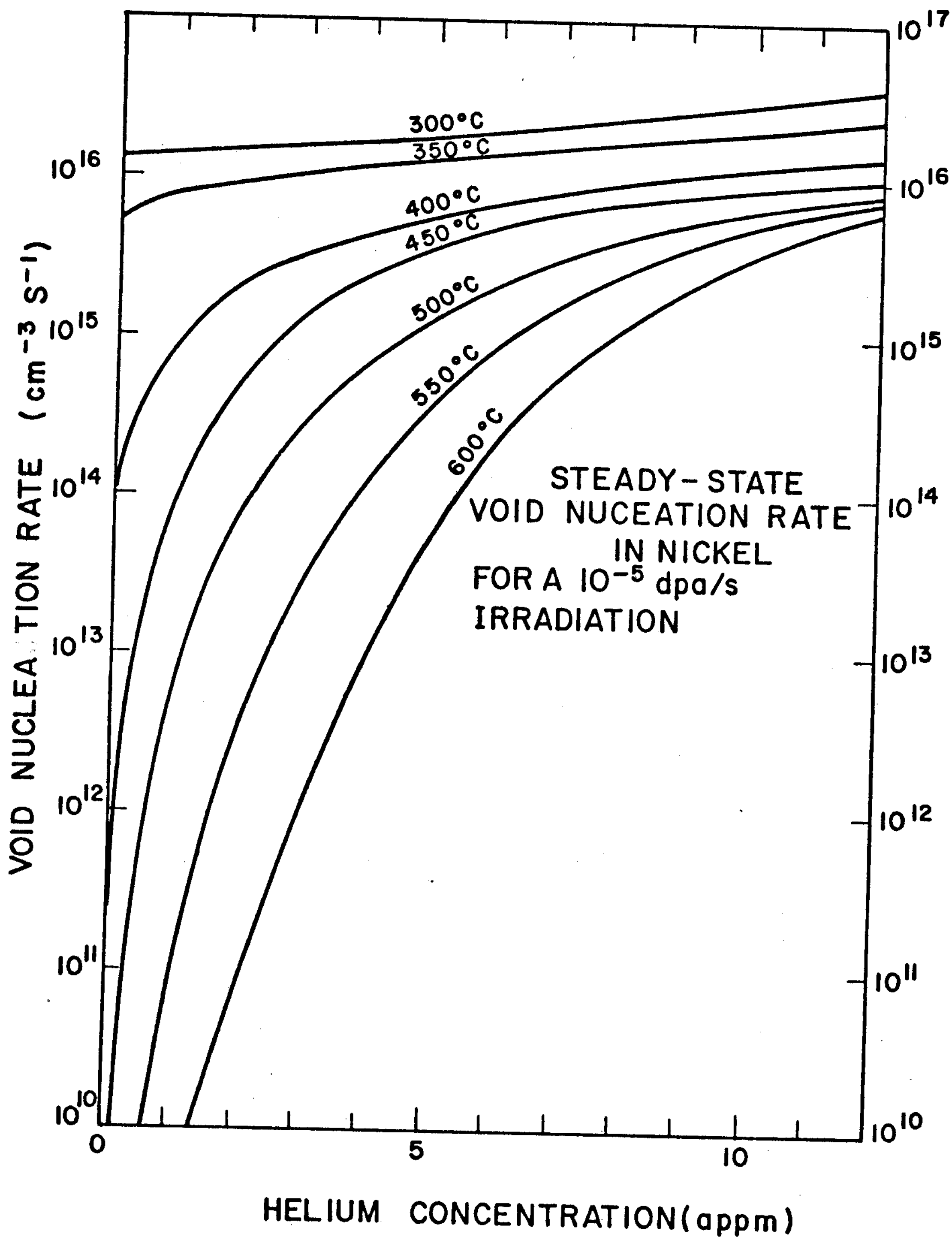
SWELLING AT LOW TEMPERATURES
$T < 0.5 T_M$

- VOID NUCLEATION AND INCUBATION DOSE ARE DETERMINED BY THE BIAS

- IF A BIAS IS PRESENT, VOID NUCLEATION IS IMMEDIATE

- SWELLING RATE DECREASES WITH TEMPERATURE

Figure 2



300°C
350°C
400°C
450°C
500°C
550°C
600°C
STEADY-STATE
VOID NUCEATION RATE
IN NICKEL
FOR A 10^{-5} dpa/s
IRRADIATION
VOID NUCLEATION RATE (cm^{-3} S^{-1})
10^{10}
10^{11}
10^{12}
10^{13}
10^{14}
10^{15}
10^{16}
10^{17}
0
5
10
HELIUM CONCENTRATION(appm)

SWELLING MODEL

SINK DENSITY: $L = 4\pi N_0 r + \rho$

SWELLING RATE

$$\frac{DS}{DT} = \Omega\left(\frac{1}{4\pi N_0 r} + \frac{1}{\rho}\right)^{-1} \{\delta Z \underline{\psi} - D_V(C_V^0 - C_V^{EQ})\}$$

VACANCY CONCENTRATION AT VOID

$$C_V^0 = C_V^{EQ} \; \mathrm{EXP}\{(\frac{2\gamma}{R} - P_G) \frac{\Omega}{kT}\}$$

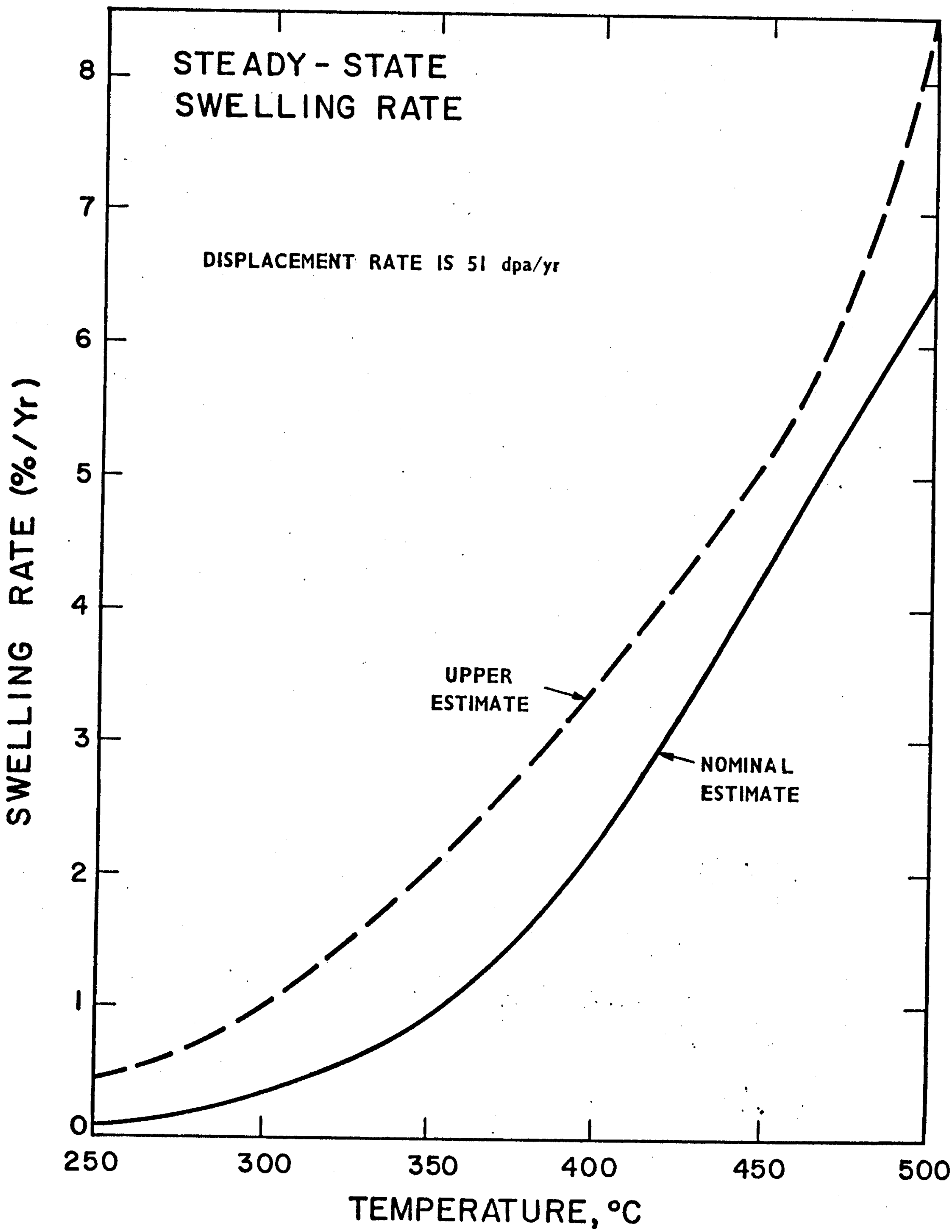
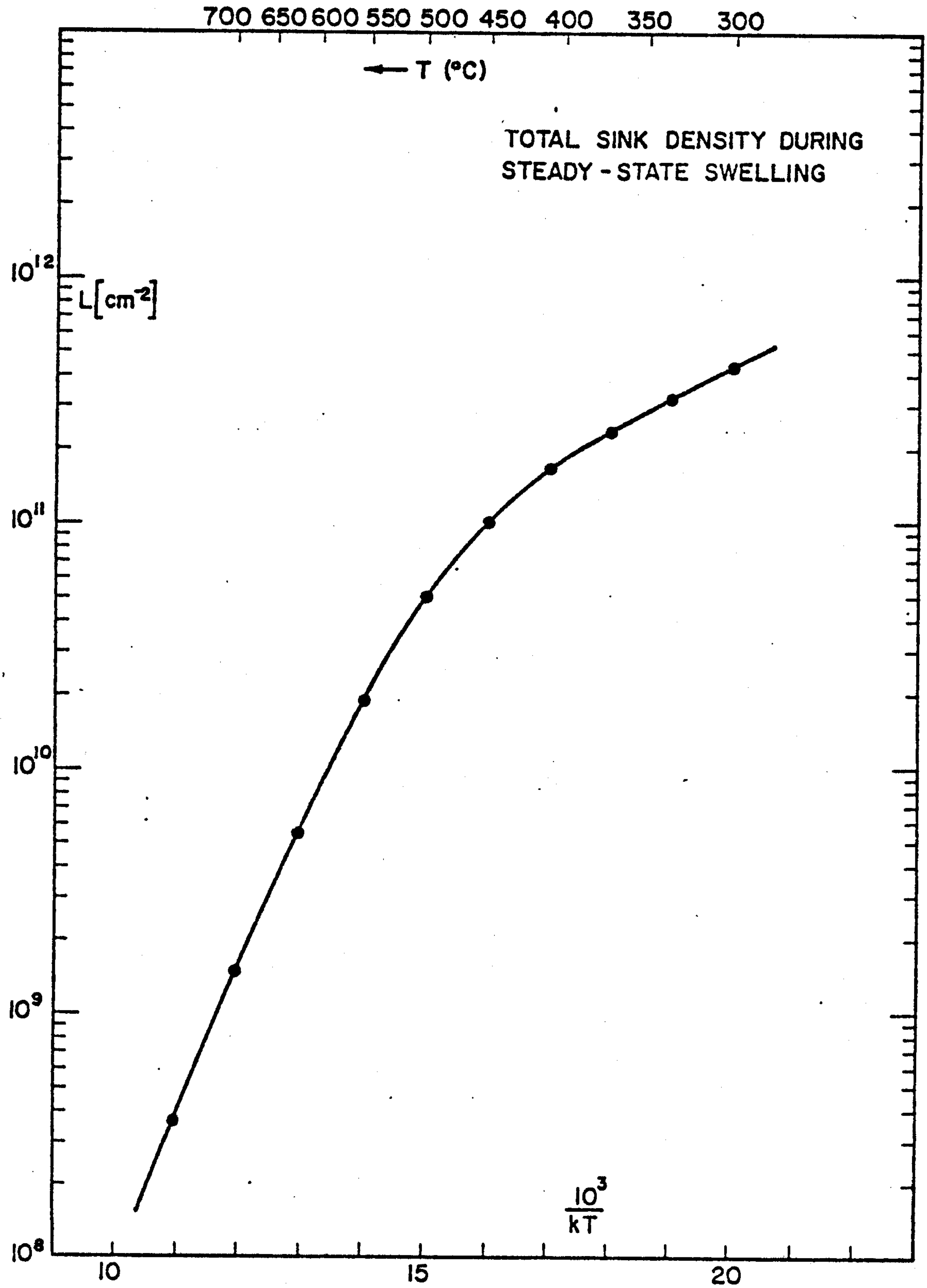
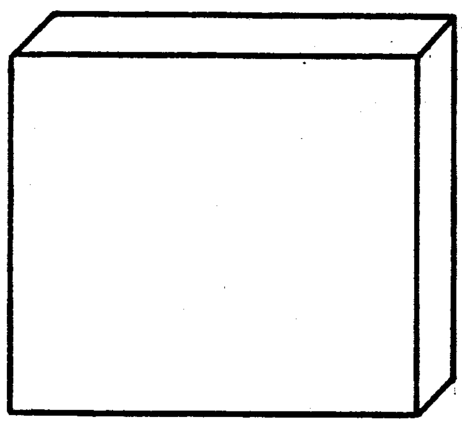
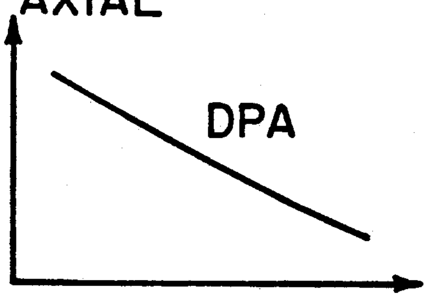

STEADY - STATE
SWELLING RATE
DISPLACEMENT RATE IS 51 dpa/yr
UPPER
ESTIMATE
NOMINAL
ESTIMATE
SWELLING RATE (%/Yr)
0
1
2
3
4
5
6
7
8
250
300
350
400
450
500
TEMPERATURE, °C

Figure 3


700 650 600 550 500 450 400 350 300
← T (°C)
TOTAL SINK DENSITY DURING
STEADY - STATE SWELLING
10^{12}
L [cm^{-2}]
10^{11}
10^{10}
10^{9}
10^{8}
$\frac{10^3}{kT}$
10
15
20

STRESSES DUE TO DIFFERENTIAL SWELLING





EQUATION FOR AXIAL STRESS

$$\frac{1}{E}\frac{d\sigma}{dt} + \psi\sigma = \frac{1}{3}(\bar{R} - R)$$

SATURATION STRESS

$$\sigma(r) = \frac{1}{3}\,\frac{\bar{R} - R}{\psi}$$

$\bar{R}, R$ AVERAGE AND LOCAL SWELLING RATE

$\psi = \frac{\dot{\epsilon}}{\sigma}$ IRRADIATION CREEP COMPLIANCE

Figure 4

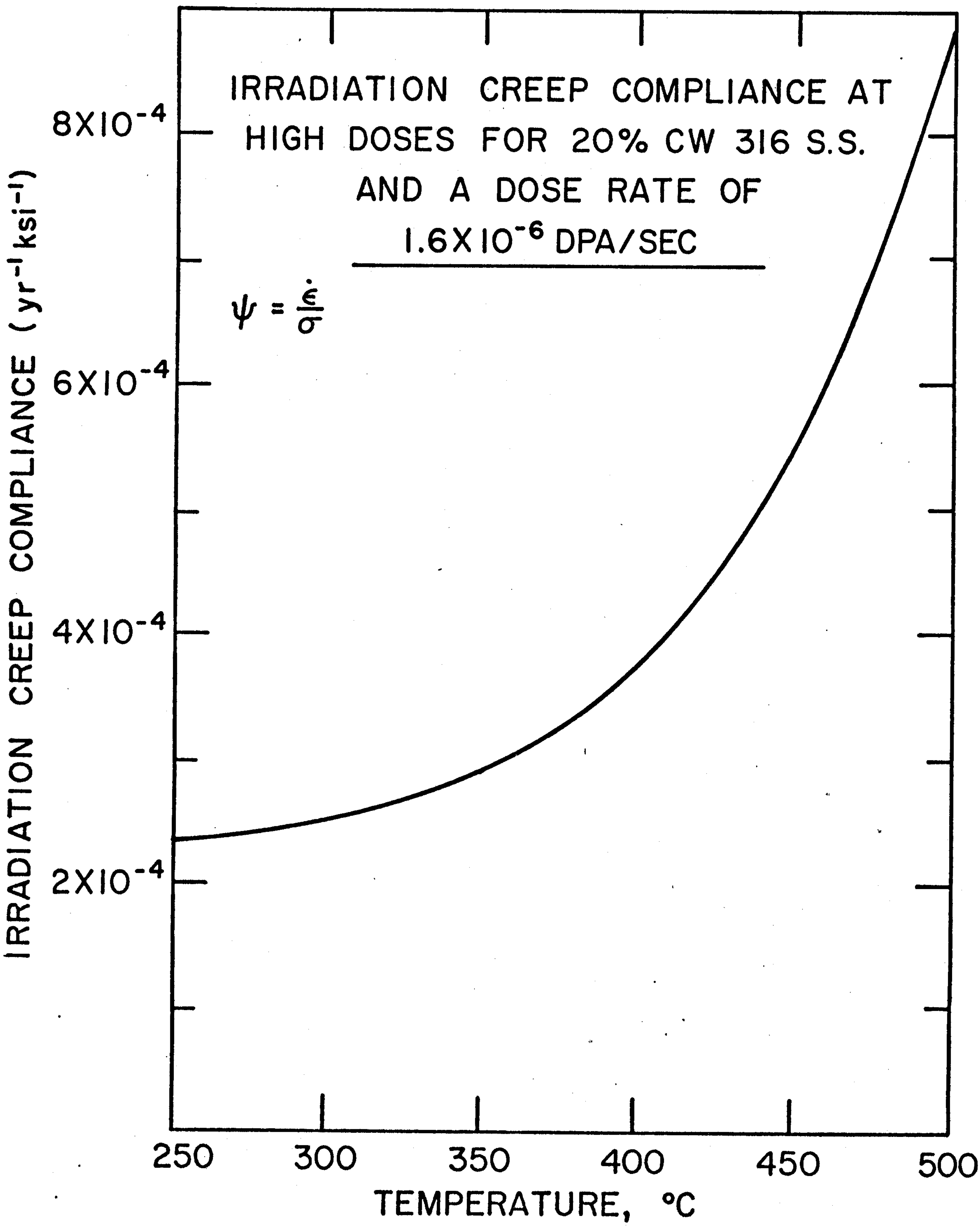
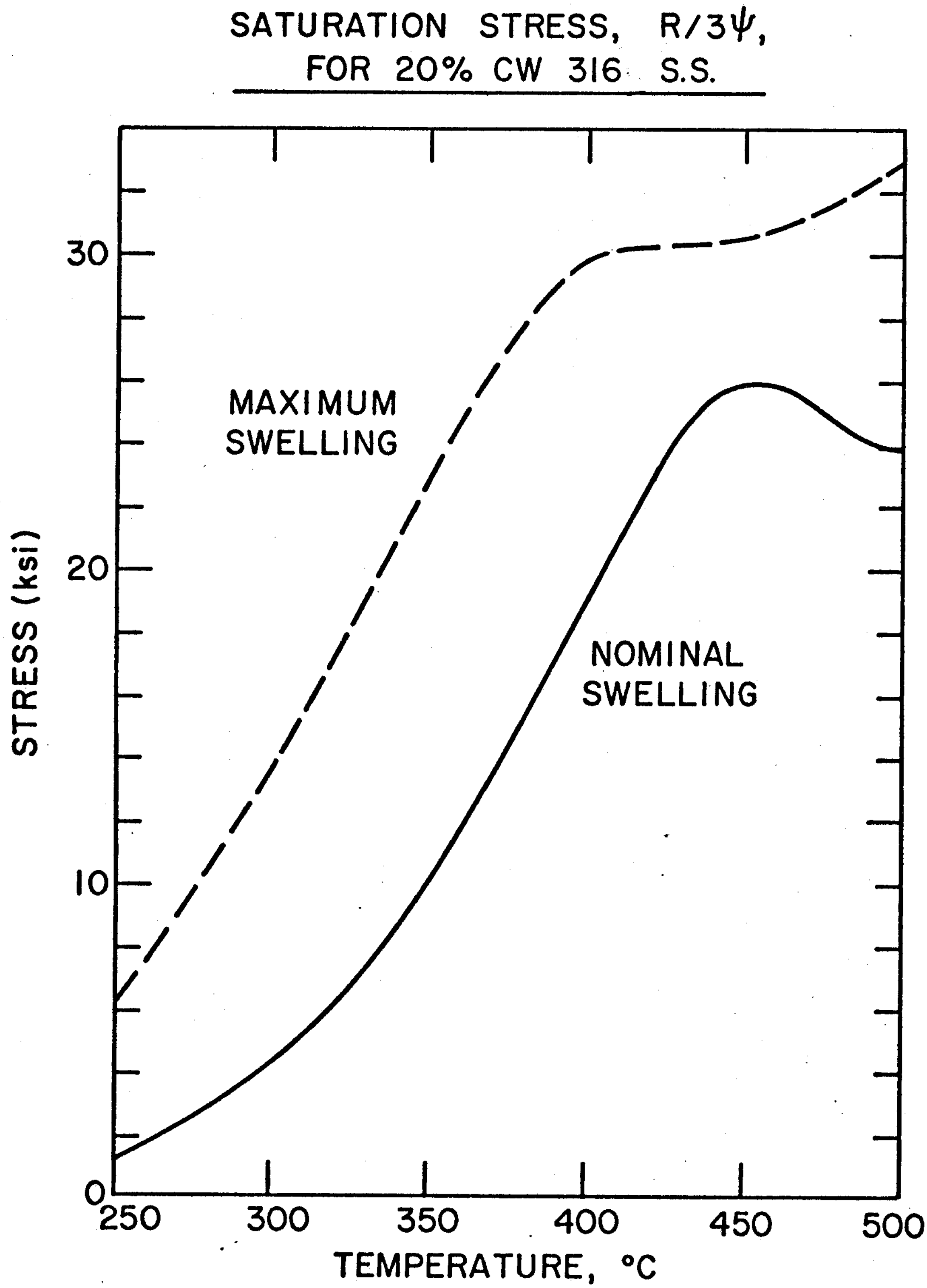
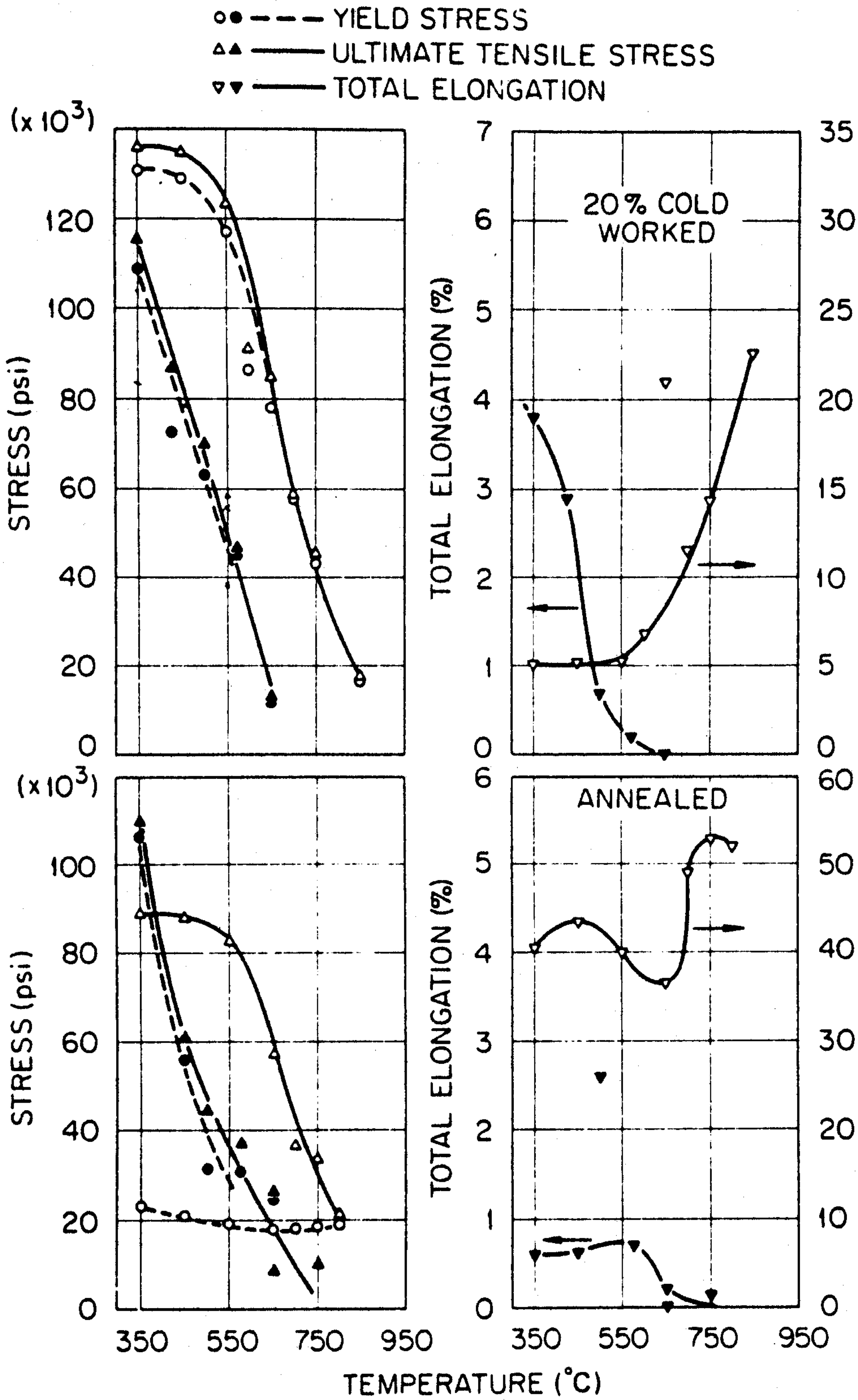

IRRADIATION CREEP COMPLIANCE AT
HIGH DOSES FOR 20% CW 316 S.S.
AND A DOSE RATE OF
1.6X10^-6 DPA/SEC
$\psi = \frac{\dot{\epsilon}}{\sigma}$
IRRADIATION CREEP COMPLIANCE (yr^-1 ksi^-1)
8X10^-4
6X10^-4
4X10^-4
2X10^-4
250
300
350
400
450
500
TEMPERATURE, °C

Figure 5


SATURATION STRESS, R/3ψ,
FOR 20% CW 316 S.S.
MAXIMUM
SWELLING
NOMINAL
SWELLING
STRESS (ksi)
0
10
20
30
250
300
350
400
450
500
TEMPERATURE, °C



Tensile properties of 20% cold-worked (upper figures) and annealed (lower figures) Type 316 stainless steel. Open symbols are for unirradiated material and filled symbols are for HFIR-irradiated samples (5.6 to 8.7 X 10^{26} n/m^2). Samples were irradiated near the test temperature to 40 to 60 dpa and 3000 to 4300 appm helium.

FRACTURE TOUGHNESS PREDICTION

MODE I FRACTURE TOUGHNESS

$$K_{Ic} = 2.89\,\sigma_Y \sqrt{\exp\left(\frac{\sigma_f}{\sigma_Y} - 1\right) - 1}\;\sqrt{\rho_o}$$

ρ_o, MINIMUM NOTCH RADIUS = 0.002 inch

σ_f, DECOHESION STRESS = 235 ksi

σ_Y, YIELD STRESS

MINIMUM CRACK SIZE

$$a < \frac{1}{\pi}\left(\frac{K_{Ic}}{\sigma}\right)^2$$

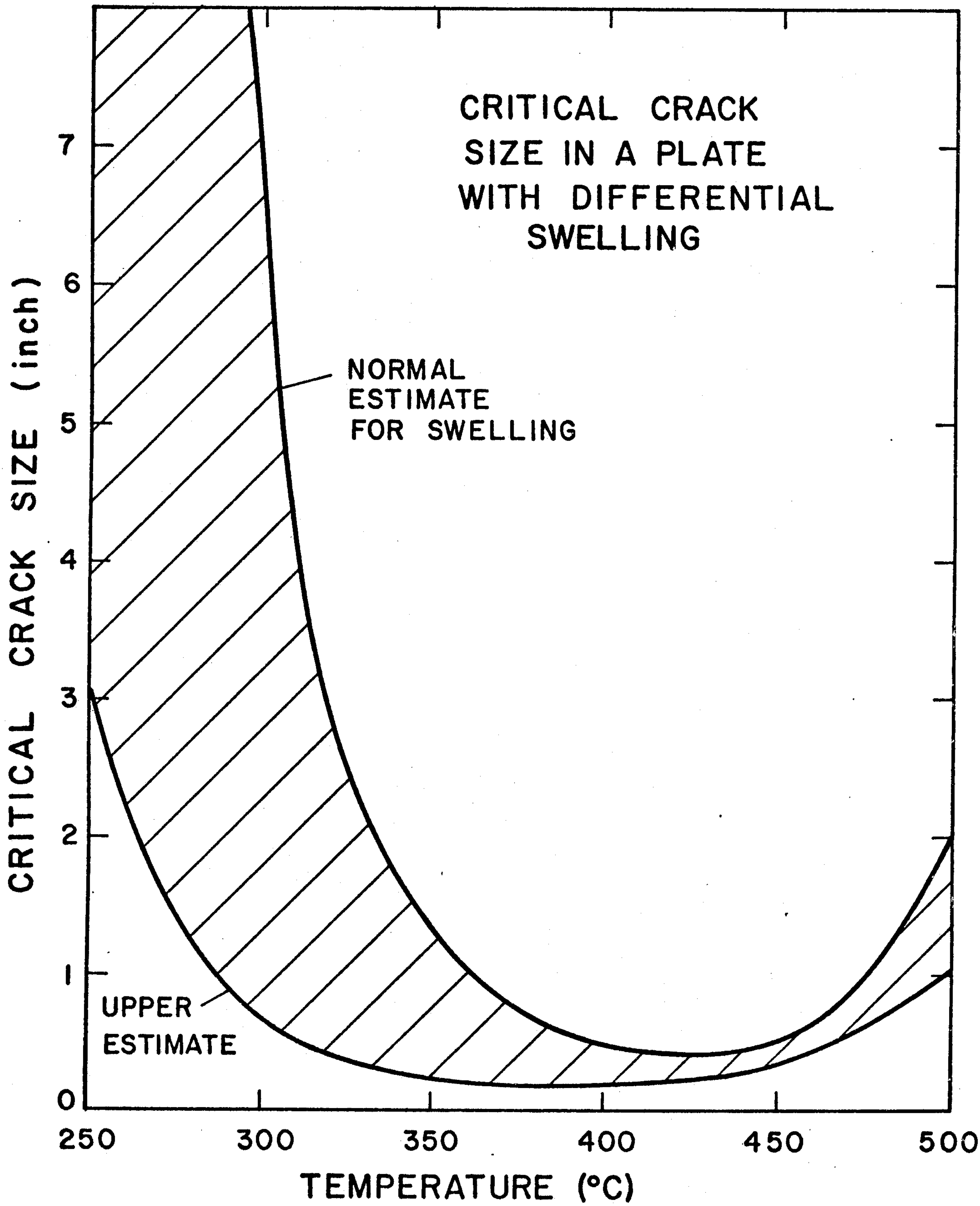

CRITICAL CRACK SIZE IN A PLATE WITH DIFFERENTIAL SWELLING
NORMAL ESTIMATE FOR SWELLING
UPPER ESTIMATE
CRITICAL CRACK SIZE (inch)
0
1
2
3
4
5
6
7
250
300
350
400
450
500
TEMPERATURE (°C)

Figure 6

INFLUENCE OF LIFETIME CONSIDERATIONS ON BLANKET DESIGN

- DESIGN TO STRESS LIMITS RATHER THAN DUCTILITY LIMITS

- MINIMIZE SWELLING TO MINIMIZE DIFFERENTIAL STRESS

IMPLICATIONS

- OPERATE STRUCTURE AT LOW T (250-300°C)

- DECOUPLE STRUCTURE TEMPERATURE FROM HEAT TRANSPORT MEDIUM