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EVALUATION OF FERRITIC ALLOY Fe-2¼Cr-1Mo AFTER NEUTRON IRRADIATION - IRRADIATION CREEP AND SWELLING

D. S. Gelles and R. J. Puigh

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D. S. Gelles and R. J. Puigh
Westinghouse Hanford Company
Richland, WA 99352

1.0 Summary

Irradiation creep and swelling measurements are reported for Fe-2-1/4Cr-1Mo after irradiation by fast neutrons over the temperature range 390 to 560°C. Diameter change measurements on thin walled pressurized tubes in a bainitic condition and density change measurements on rods in a non-standard condition were made following irradiation in the Experimental Breeder Reactor II. The irradiation creep specimens were irradiated to a fluence of 5.7×10^{22} n/cm^2 (E > 0.1 MeV) or 30 dpa and the swelling specimens were irradiated to a peak fluence of 2.4×10^{23} n/cm^2 or 115 dpa. These results have been used as a basis to establish in-reactor creep and swelling correlations for 2-1/4Cr-1Mo in a bainitic condition. The correlations predict moderate swelling and moderate irradiation enhanced creep at 390°C.

2.0 Introduction

The first wall and blanket structure are key components in a fusion reactor since its structural life can impact the reactor's performance which ultimately translates into the cost of electricity. To date a number of metals have been proposed for possible use as the structural material in the first wall and blanket and many have been included in conceptual power reactor designs. These conceptual designs have helped to show that while each material has attractive properties or features for use in design (low thermal stresses, high temperature strength, or low, long term radioactivity), they also have disadvantages and not one of these materials including 316 stainless steel has emerged as the overwhelming favorite for use in commercial reactors. The primary uncertainty regarding the use of these materials is a lack of radiation damage information, particularly with respect to the synergism between neutron damage and transmuted helium. In an effort to gain a better understanding of these effects and to ultimately develop a radiation resistant material, the Office of Fusion Energy (OFE) of the Department of Energy (DOE) created, in 1976, a program known as Alloy Development for Irradiation Performance (ADIP).

At the time the ADIP program was created there were more than 10 different alloy systems proposed for use in the first wall and blanket structure. Rather than study all of these materials, it was decided to group them into families or classes and emphasize only the most promising material within each group. These groups are referred to as paths and there are currently 5 paths in the ADIP program. These are Path A -- Austenitic Stainless Steel [Type 316 and modified 316 called prime candidate alloy (PCA)], Path B -- Fe-Ni-Cr precipitation strengthened alloys (625, X750, PE-16), Path C -- Reactive and Refractory Metals (Nb, V, and Ti alloys), Path D -- Innovative Materials and Concepts (long ranged ordered alloys, ceramics, composites), and Path E -- Ferritic Steels (HT-9, 9Cr-1Mo and 2-1/4Cr-1Mo).

The primary objective of the ADIP program (Reference 1) is to develop materials capable of operating in a fusion reactor up to a time integrated neutron exposure of 40 MW y/m^2. A secondary objective is to provide both materials and design data for use in lower performance, intermediate fusion systems such as ETR and DEMO. In the parallel path approach each of the candidates are independently brought through a series of sequential steps consisting of scoping studies, base research studies and alloy optimization. This type of approach is necessary since premature selection or rejection of an alloy could severely limit the design options available in the future. Currently, the only system to be in the alloy optimization stage is 316 stainless steel.

Since the inception of the ADIP program, the primary emphasis has been on irradiation experiments with the net result that roughly 7,000 specimens have been or are currently being irradiated. The specimens consist of tensile, creep-rupture, fatigue, fracture-toughness, and flaw-growth specimens as well as pressurized tubes and transmission electron microscopy (TEM) discs. The specimens cover the whole range of alloys including 20% cold worked (CW) type 316 stainless steel, ferritic steels, nickel alloys, titanium alloys, vanadium alloys, and long range ordered alloys. Because of the large number of specimens involved and a limited fusion budget, only a small fraction of these specimens have been tested. The remaining specimens have been archived for future testing when more funds become available or testing priorities change. Currently the bulk of the ADIP effort is being directed towards modified 316 stainless steel (PCA) and ferritic steels (HT-9 and 9Cr-1Mo). By initiating a program to test other candidate materials such as Fe-2-1/4Cr-1Mo steel, the data base for irradiated material can be substantially improved which will enable improved material development planning, life prediction and material selection.

Ferritic steels, particularly those containing 9-13% chromium are of interest to the LMFBR program for use in cladding and ducts because of the alloy's elevated temperature strength, creep resistance, compatibility with a liquid metal coolant, and availability from industry. For the same reasons that these steels are of interest to the breeder program, they are also of interest to the fusion reactor program for use in the first wall and blanket structure. However, for fusion reactor applications, ferritic steels offer specific advantages over 316 stainless steel; namely, low thermal stresses because of their better thermal conductivity and lower thermal expansion. While ferritic steels will have roughly twice the thermal stress resistance of 316 stainless steels, this resistance will be lower than for the other candidate materials such as the vanadium alloys. The biggest advantage with the ferritic alloys rests in its resistance to radiation damage. Experiments conducted on ferritic steels as part of the breeder reactor National Cladding/Duct Materials Development program revealed that, as a class, these materials are more resistant to void swelling and irradiation creep than 316 stainless steel.

While these initial scoping irradiations indicate that the ferritic steels have the potential for increased component lifetimes in comparison to 316 stainless steel, more information is needed about their resistance to neutron irradiation. Of particular concern is the shift in the ductile-to-brittle transition temperature and the swelling resistance at higher fluences. To increase the understanding of the behavior of ferritic steels to neutron irradiation, the breeder program in 1975 and the fusion program in 1979 initiated a number of irradiation experiments designed to provide information on swelling, creep, tensile, and fracture toughness behavior of a number of ferritic steels. The primary emphasis of these experiments was to examine ferritic steels capable of operating at temperatures <550°C such as 12Cr-1Mo (HT-9) and 9Cr-1Mo; however, the lower strength steel 2-1/4Cr- 1Mo was also included because of its wide spread unirradiated application in the chemical process and nuclear industries.

Currently the 2-1/4Cr-1Mo steel has not been seriously considered for use in either fusion or fission components because of its limited creep strength at temperatures above about 500°C. As a result the irradiated 2-1/4Cr-1Mo specimens have not been tested and DOE has no current plans to include them in their post-irradiation experiments. However, recent reactor studies such as STARFIRE and MARS indicate that acceptable electrical power generation can be achieved with first walls operating at temperatures ≤500°C. For temperatures ≤500°C, the 2-1/4Cr-1Mo steel in the normalized and tempered condition has properties equivalent to the 12Cr-1Mo steel and, as a result, becomes a viable alternative. An additional advantage is in its better weldability. The high Cr-Mo steels such as 12Cr-1Mo are more sensitive to cold cracking in weld heat affected zones than the lower chromium steels such as 2-1/4Cr-1Mo. For the large complicated structures used in fusion, which will likely require a number of welds, improved weldability can be a distinct advantage. Even though the 2-1/4Cr-1Mo steel appears to offer advantage over the other ferritic steels based on fabricability and at lower temperatures has equivalent properties, little is known about its radiation resistance. The present effort is designed to provide this information so that its potential can be further explored.

2.1 Objectives and Technical Approach

The objectives of this ferritic steel study are to develop an understanding of the response of the 2-1/4Cr-1Mo steel to neutron irradiation and to present the results of the mechanical property evaluations and swelling studies in a format consistent with its inclusion in the Materials Handbook for Fusion Energy Systems (MHFES). The results of these evaluations will be interpreted in terms of their impact on future studies of this alloy and its suitability for use in fusion reactor components.

2.2 Experimental Procedures

Materials for irradiation creep and swelling experiments were obtained from different sources. The creep specimens were fabricated from a Mannesmann heat #38649 provided by Climax Molybdenum Company and swelling specimens were fabricated from a Lukens Steel Company sample, heat number C4337-14S (also identified as alloy A-387-D). The compositions, as supplied by the vendors, are provided in Table 1. The Mannesmann heat was received in the form of a 12.7 cm long section of 7 cm wall pipe. The section was cut radially in 2 cm thick slices, which were subsequently rolled and machined into tubes according to the rolling schedule diagrammed in Figure 1. Tube dimensions were 0.457 cm OD x 0.417 cm ID. The specimens were heat treated according to the schedule given in Table 2. Endcaps of HT-9, a martensitic stainless steel, were electron beam welded to tubing segments 1.981 cm in length. This geometry was chosen to ensure an adequate wall thickness of 0.02 cm and yet optimize the use of the limited irradiation volume. One endcap had a capillary hole for pressurizaton. Each specimen was filled with He to the desired pressure and the closure weld for gas containment was made with a laser beam which passed through the glass port of the pressure vessel and sealed the capillary fill hole in the endcap. Specimen diameters were measured both before and after irradiation using a non-contacting laser system which has an accuracy of $\pm 2.5 \times 10^{-5}$ cm and has repeatability in the hoop strain measurement of 0.05 percent. The Lukens Steel Company heat was sectioned into random cross sections approximately 1 cm

TABLE 1

CHEMICAL ANALYSIS OF 2-1/4Cr-1Mo HEATS AS SUPPLIED BY THE VENDORS
(in weight percent)

Element	Creep Tubes[a]	Swelling Rods[b]
C	0.093	0.12
Mn	0.52	0.42
P	0.011	
S	0.011	
Si	0.17	0.21
Ni	0.40	0.16
Cr	2.15	2.17
Mo	0.95	0.93
Cu	0.16	
Al	0.003	
Fe	bal	bal

[a] Mannesmann Company heat #38649
[b] Lukens Steel Company heat #C4337-14S

TABLE 2

HEAT TREATMENTS GIVEN CREEP TUBES AND
SWELLING SPECIMENS PRIOR TO IRRADIATION

Specimen Type	Heat #	Heat Treatment*
Creep Tubes	38649	900°C/30 min./AC +700°C/1 hr/AC
Swelling specimens	C4337-14S	1010°C/1 hr/WQ + 843°C/2 hr/WQ

*temperature/time at temperature/cooling procedure
where AC = air cooled, WQ = water quenched

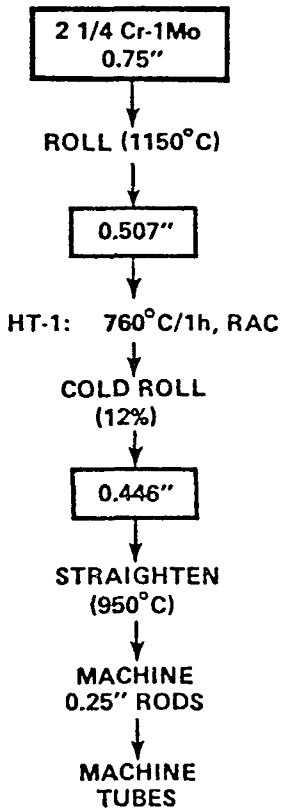

FIGURE 1. Rolling Schedule, Alloy 2-1/4Cr-1Mo.

in diameter and heat treated according to the schedule given in Table 2. The somewhat unusual heat treatment was based on information from reference 1. Specimens 0.3 cm diameter by 1.3 cm long were then machined from the stock. Swelling was determined from density measurements based on the Archimedian principle. Multiple measurements were made on each specimen with a typical measurement uncertainty of $\pm$0.05 percent.

Specimens were irradiated in the Experimental Breeder Reactor (EBR-II) located in Idaho Falls. The irradiation vehicle for the creep specimens identified as capsule B 329 which was part of the AAXIV experiment was a cylindrical tube 1.5 m in length and 2.0 cm in diameter. Inside were three subcapsules which were connected to the Na coolant flow by a capillary tube with an inlet at the bottom of the capsule and an outlet at the top of the capsule. The Na flow was necessary to achieve the desired lowest temperature in the capsule and permit gas release from the subcapsule should a creep specimen rupture. The dimensions of the insulating gas gap between the subcapsule and the outer capsule was designed to control the heat transferred from the gamma heated subcapsule to the reactor coolant which was flowing past the outer capsule wall. Calculations were performed to optimize the Na flow rate through the capsules so as to minimize the thermal gradient within a given subcapsule. The nominal design temperatures for each subcapsule was 400, 450, and 550°C. The capsule B329 was loaded into subassembly X359 which was irradiated in position 4C2 in EBR-II for cycles 109 through 111 and 113. The specimens were in the reactor for a period of 10680 MWD which corresponds to 4477 hours at temperature and a peak fluence exposure 2.8 x 10^{22} n/cm^2 (E > 0.1 MeV). The reconstitution of the AAXIV experiment which contained the ferritic creep specimens consisted of three separate B7 capsules. The capsules B331, B333 and B334 contained the ferritic pressurized tube specimens reconstituted from the AAXIV three temperature capsule (B329) and were designed for the irradiation temperatures of 550, 450 and 400°C, respectively. Capsule B334 was a weeper design and, therefore, the specimens were dirctly exposed to the sodium coolant. These B7 capsules werepart of subassembly X359a which was irradiated in position 4C2 in EBR-II for cycles 116 through 119. The specimens were in the reactor for a period corresponding to 10979 MWD which corresponds to 4603 hours at temperature and a peak fluence exposure of 2.9 x 10^{22} n/cm^2 (E > 0.1 MeV).

The irradiation temperatures were determined with thermal expansion devices (TED).[2] TEDs were located at the top and bottom of each sub-capsule and were used to indicate the maximum temperature to which the specimens were exposed during irradiation. The results of the analysis of the TED's are summarized in Table 3. The TED's indicate the peak temperature to which the specimens were exposed. The TED temperatures reported in Table 1 have been corrected for measured volume changes in the cladding material. The nominal irradiation temperature assumes that the average irradiation temperature is the midplane coolant temperature plus 90% of the temperature difference between the coolant and peak (TED) temperatures. In other words, the γ-heating at the specimen locations is assumed to vary $\pm$10% during the coarse of an irradiation.

TABLE 3

IRRADIATION TEMPERATURES FOR IN-REACTOR CREEP SPECIMENS

Capsule	Design Temperature (°C)	Peak Irradiaton Temperature (°C)
B329^{+}	400	392
	450	480
	550	560
B334	400	383
B333	450	475 $\pm$ 10
B331^{++}	550	570 $\pm$ 9

$^{+}$Inconel 600 TED in same level as specimens.
$^{++}$Average peak temperatures for TEDs above and below level containing specimens.

The irradiation vehicle for the swelling specimens identified as the AAI test was of similar outer dimensions. Inside were eight subcapsules, each of which contained identical specimen loadings immersed in sodium. The subcapsule temperatures were obtained by controlled gamma heat losses through an inert gas gap between the subcapsules and the capsule. Design temperatures were 400, 425, 455, 480, 510, 540, 595 and 650°C. A low fluence experimental test of this design used thermal expansion devices (TEDs) to check operating temperatures and the temperature uncertainties are estimated at ±25°C for the higher subcapsule temperatures. The major factor controlling this uncertainty was found to be variations in the reactor gamma heating rate. Heat transfer calculations based on those gamma heating values indicate that the actual operating temperatures were lower than the design temperatures by as much as 20°C. Reactor fluences given for both experiments are the product of the EBR-II flux for the appropriate reactor positions and the residence time of the vehicle in-reactor. The fluence uncertainty is estimated to be ±10%.

2.3 Results

The results of in-reactor creep measurements are presented in Table 4 and the results of swelling measurements are presented in Table 5. In each case, results of a lower fluence discharge are also given.[3,4] The creep results are plotted in Figures 2 through 7. Figures 2, 3 and 4 show diameter change as a function of fluence for each of the irradiation temperatures 390, 480 and 570°C. The diametral changes shown in these figures have been corrected for volumetric changes in the specimen by subtracting the diameter change for the unstressed specimen from the stressed specimen diametral strain at each temperature. In all but one case, diameter change increases with stress (the exception being at 390°C and low fluence where the variations are within the uncertainty of the measurement technique.) The high stress specimen (100 MPa) tested at 570°C had apparently failed prior to the lower fluence measurement (2.3×10^{22} n/cm^2) at a diameter strain of 2.3%. The stress dependence of the corrected diameter changes at 390, 480 and 570°C is shown in Figures 5, 6 and 7. Response at 390°C and at

TABLE 4

DIAMETER CHANGE MEASUREMENTS FOR 2-1/4Cr-1Mo PRESSURIZED TUBE SPECIMENS CONTAINED IN THE AAXIV EXPERIMENT[3]

Specimen Number	Temperature (°C)	Fluence ($\times 10^{22}$ n/cm^2)	Midwall Hoop Stress (MPa)	Diameter Change (%)
PJ54	392	2.0	0	0.002
PJ54	383	5.0	0	0.09
PJ61	392	2.0	50	0.038
PJ61	383	5.0	50	0.20
PJ64	392	2.0	75	0.033
PJ64	383	5.0	75	0.22
PJ67	392	2.0	100	0.041
PJ67	383	5.0	100	0.27
PJ53	480	2.6	0	0.023
PJ53	475	5.7	0	0.01
PJ60	480	2.6	50	0.037
PJ60	475	5.7	50	0.05
PJ63	480	2.6	75	0.090
PJ63	475	5.7	75	0.12
PJ66	480	2.6	100	0.102
PJ66	475	5.7	100	0.15
PJ52	560	2.3	0	-0.016
PJ52	570	5.4	0	-0.06
PJ58	560	2.3	50	0.308
PJ58	570	5.4	50	0.69
PJ62	560	2.3	75	0.483
PJ62	570	5.4	75	1.85
PJ65	560	2.3	100	2.290 (failed)
PJ65	570	5.4	100	2.33 (failed)

TABLE 5

SWELLING MEASUREMENTS ($-\Delta\rho/\rho_0$) FOR 2-1/4Cr-1Mo SPECIMENS CONTAINED IN THE AAI TEST[4], ρ_0 = 7.8414

Specimen Number	Temperature (°C)	Fluence ($\times 10^{23}$ n/cm^2)	Density (gm/cm^3)	Swelling (%)
94M6	400	1.40		0.12
94M7	400	1.60	7.8204	0.28
94L6	427	1.58		0.08
94L7	427	2.07	7.8315	0.14
94E6	454	1.32		0.09
94E7	454	1.55	7.8368	0.08
94F6	482	1.53		0.10
94F7	482	1.98	7.8384	0.05
94K6	510	1.72		0.09
94K7	510	2.41	7.8422	0.01
94G6	538	1.67		0.17
94G7	538	2.32	7.8237	0.22

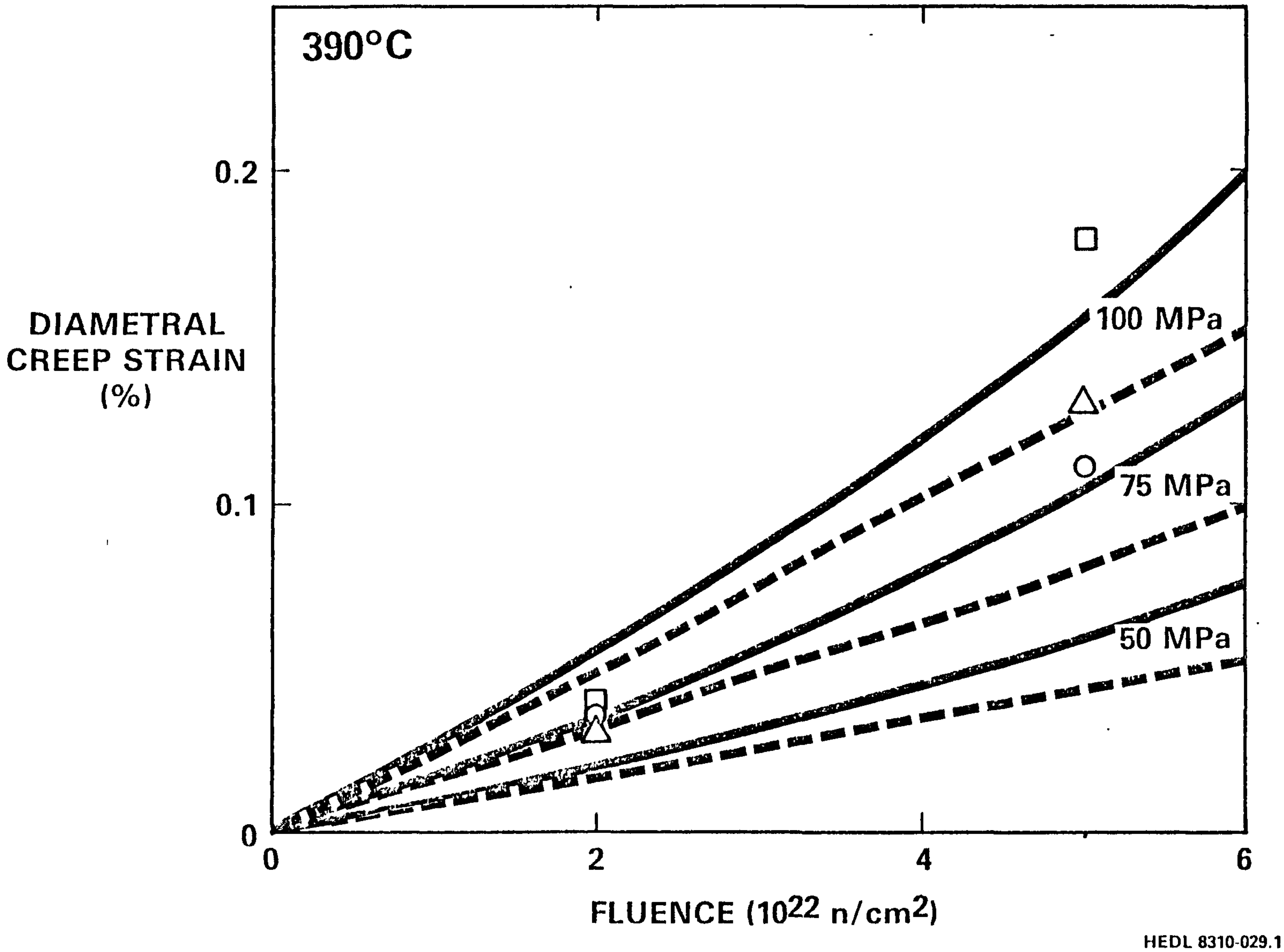

FIGURE 2. Diameter Change Corrected for Swelling as a Function of Fluence at 390°C for Midwall Hoop Stress Levels as High as 100 MPa. The solid areas define the in-reactor creep correlation prediction for the stress indicate. The dashed curves define the irradiation creep contribution without swelling enhanced creep.

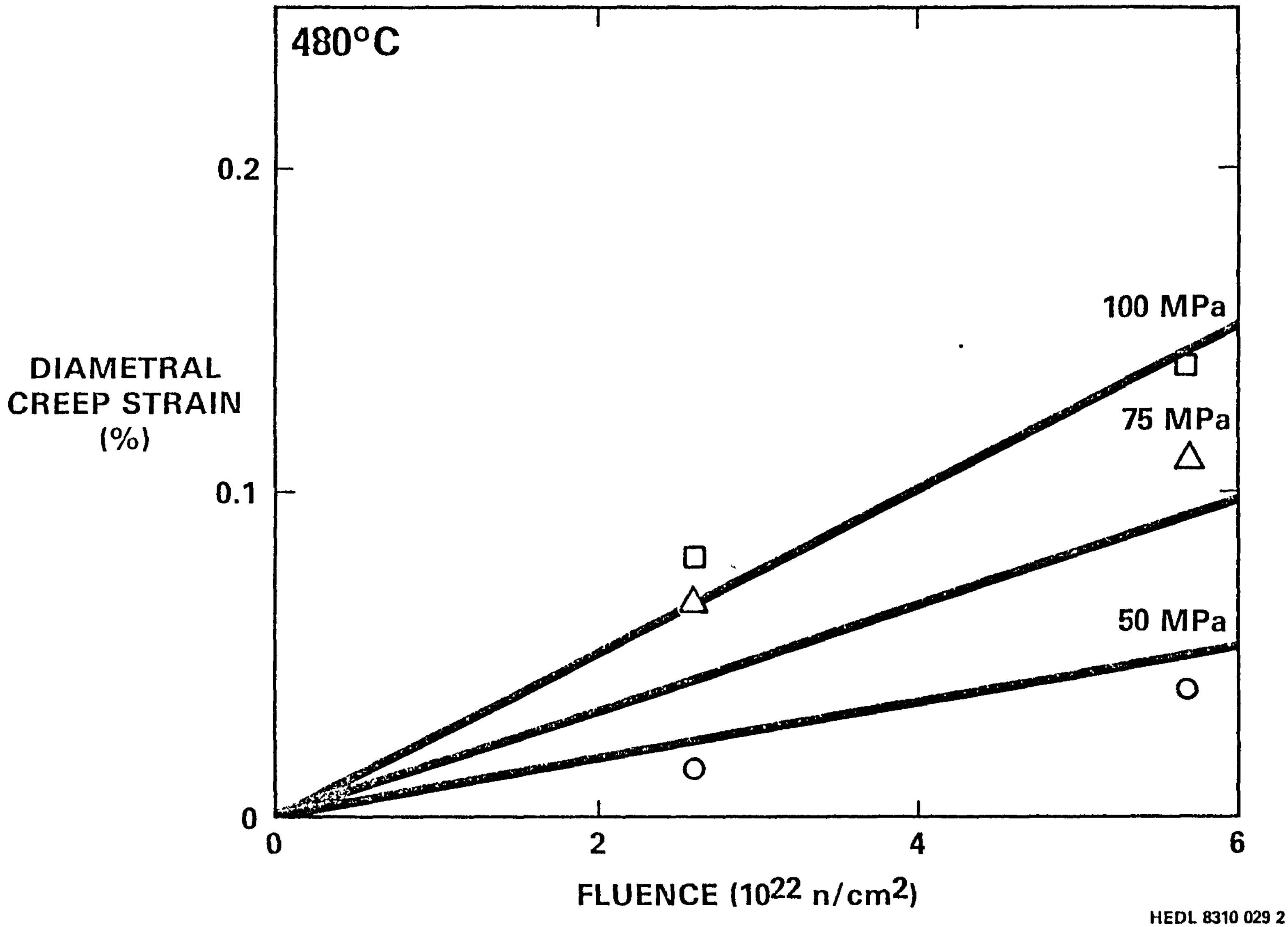

FIGURE 3. Diameter Change Corrected for Swelling as a Function of Fluence at 480°C for Midwall Hoop Stress Levels as High as 100 MPa. The curves define the in-reactor creep correlation prediction with thermal creep neglected for the stress values indicated.

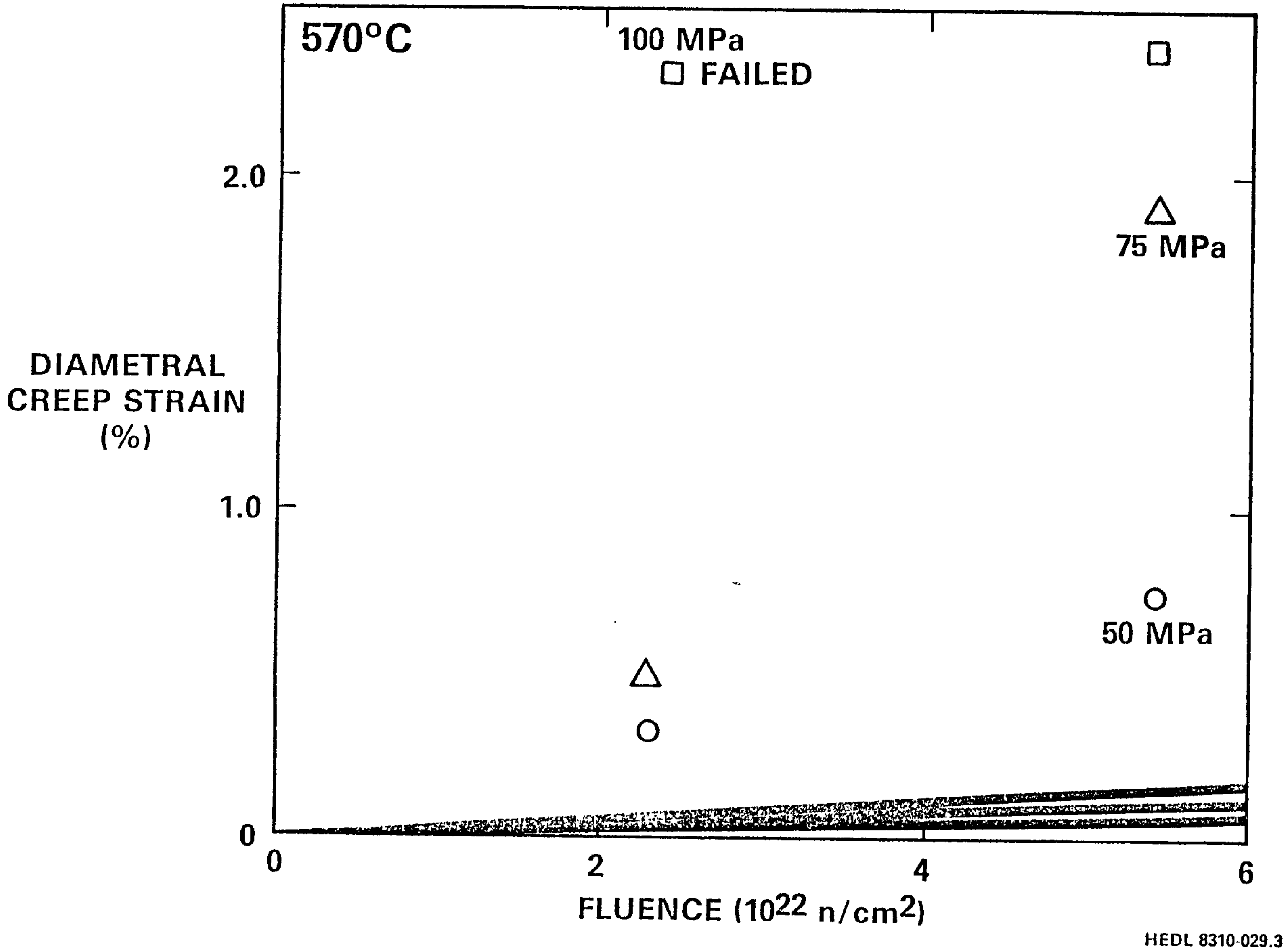

FIGURE 4. Diameter Charge Corrected for Swelling as a Function of Fluence at 570°C for Midwall Hoop Stress Levels as High as 100 MPa. Note that the 100 MPa specimen failed prior to 2.3 x 10^{22} n/cm^2. The curves define the in-reactor creep correlation prediction with thermal creep neglected.

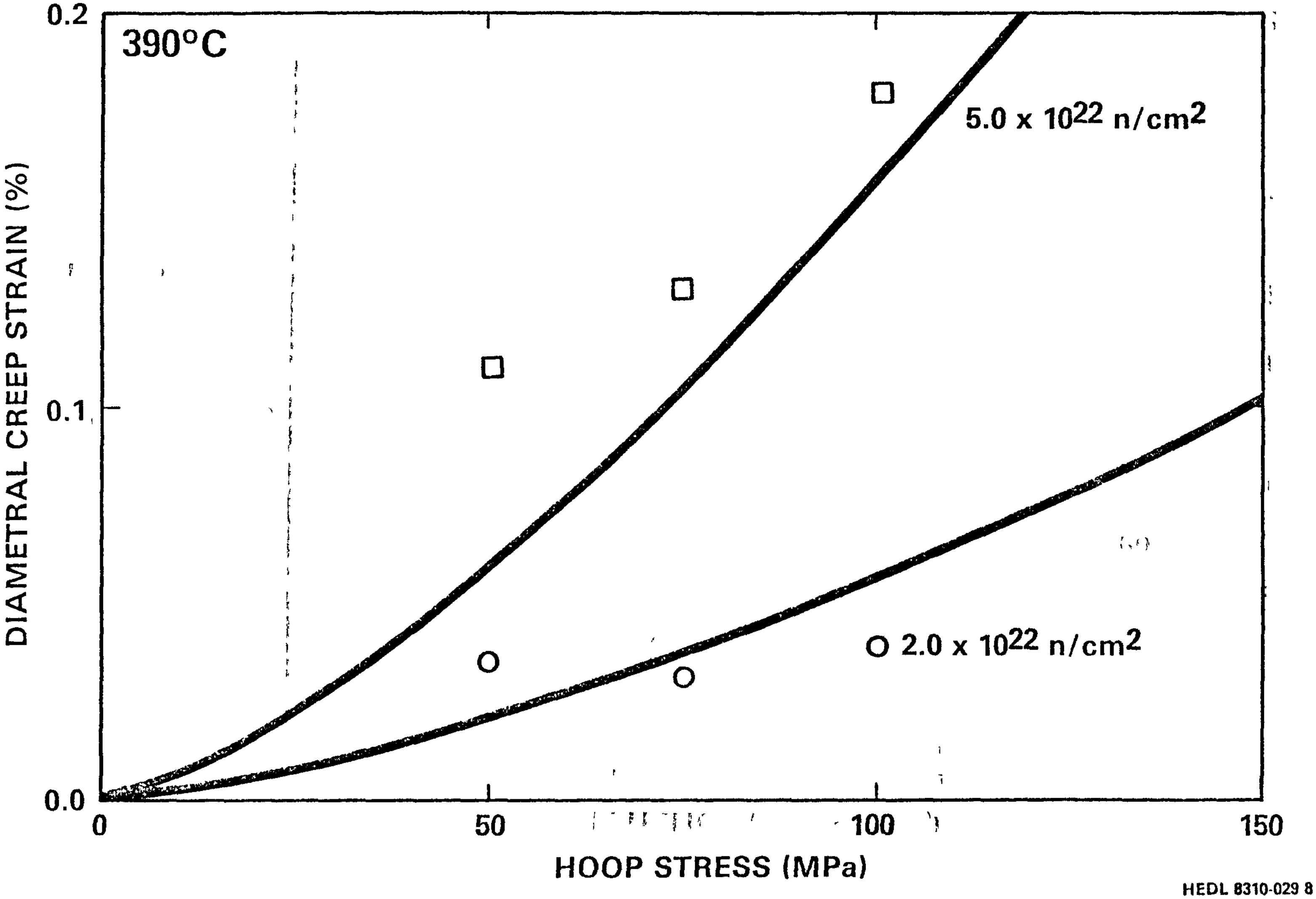

FIGURE 5. Diameter Change at 390°C Corrected for Swelling as a Function of Hoop Stress. The curves define the in-reactor creep correlation predictions.

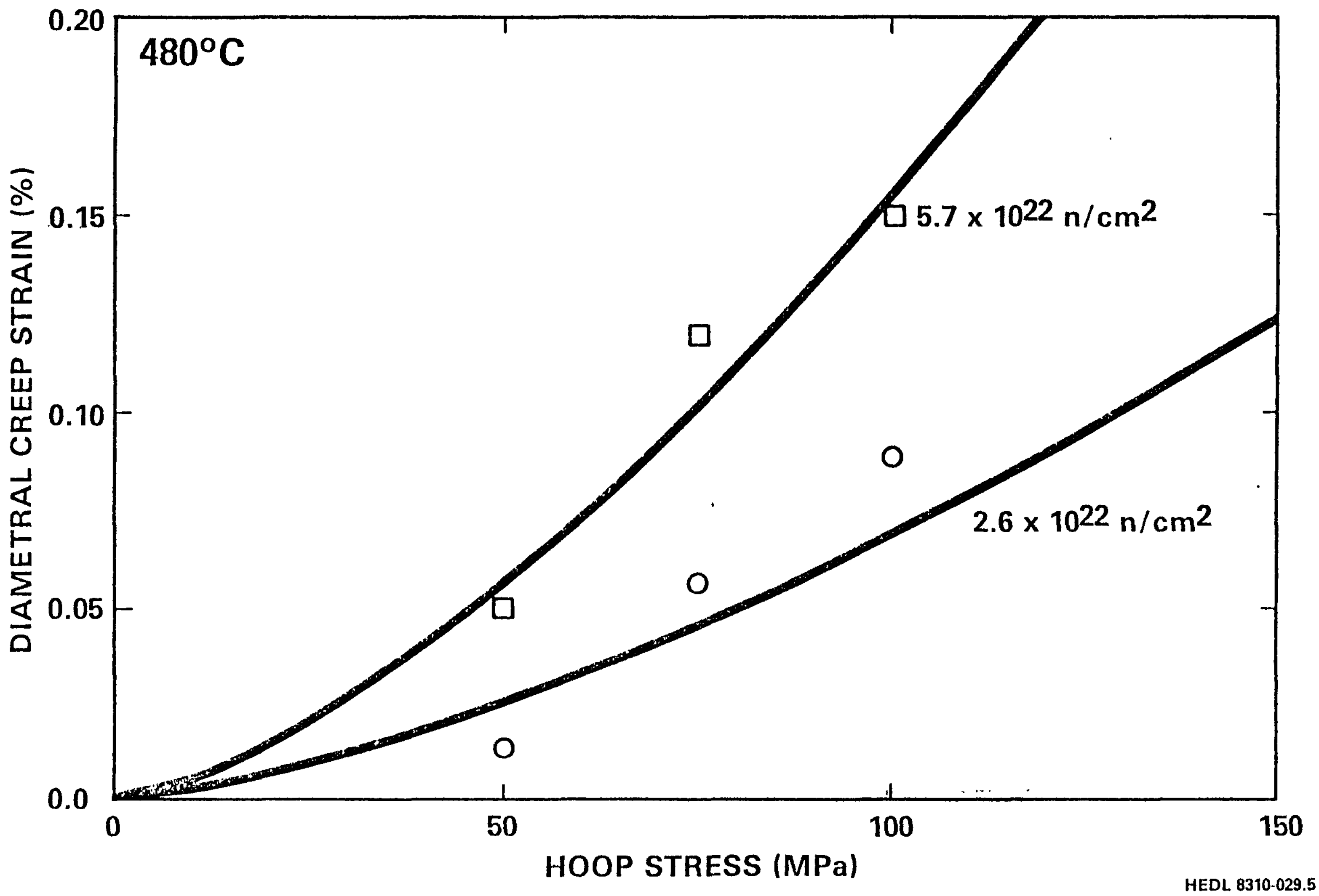

FIGURE 6. Diameter Change at 480°C Corrected for Swelling as a Function of Hoop Stress. The curves define the in-reactor creep correlation prediction with thermal creep neglected.

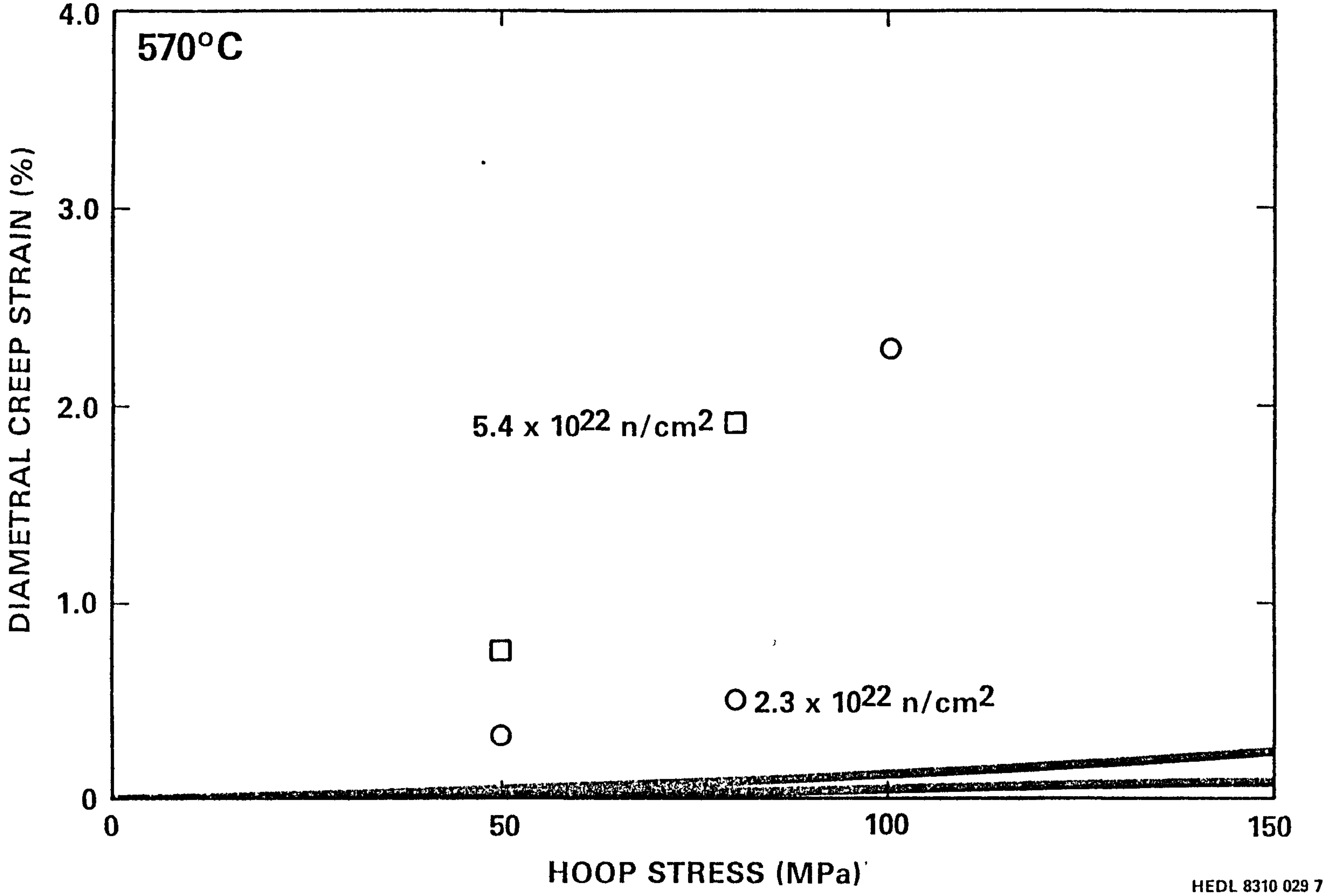

FIGURE 7. Diameter Change at 570°C Corrected for Swelling as a Function of Hoop Stress. The curves define the in-reactor creep correlation prediction with thermal creep neglected.

480° (Figures 5 and 6) can be adequately represented by a linear fit given the data scatter, but the onset of tertiary creep prevents such an analysis in Figure 7. It can also be shown by comparison of Figures 2 and 3 or 5 and 6, that in-reactor creep at the higher fluence is greater for the 380°C case than for the 460°C case. Such a response can occur when irradition creep is enhanced by swelling.

The pressurized tube data base can also provide swelling information. The diameter change for the unstressed conditions can be interpreted from equation 1 to give fractional swelling values. If one assumes isotropic swelling, then

$$S = \Delta V/V_0 \approx 3\ \Delta D/D_0 \qquad (1)$$

Therefore, swelling at 390°C to 5 x 10^{22} n/cm^2 can be estimated at 0.27% whereas at 480°C and 560, the swelling is negligible and densification of 0.18% occurs at 570°C.

The swelling values from Table 5 are plotted in Figure 8. Figure 8 also shows the results of the unstressed pressurized tubes for comparison. From Figure 8 it can be demonstrated that swelling is low in 2-1/4 Cr-1Mo to fluences as high as 2.4 x 10^{23} n/cm^2. Peak swelling occurs at the lowest temperature of 400°C but a secondary swelling peak is found at 540°C. This secondary peak is expected to arise as a result of precipitation rather than void swelling. However, the 400°C peak can be expected to be due to void swelling. A swelling rate of 0.08%/10^{22} n•cm^{-2} or 0.016%/dpa is predicted from the AAI results at 400°C. In comparison, the pressurized tubes produced comparable swelling values at a much lower fluence. This may be an effect due to heat-to-heat, fabrication or heat treatment variations. It may also be noted that densification is occuring at 510°C, an indication that precipitation is continuing at high fluence.

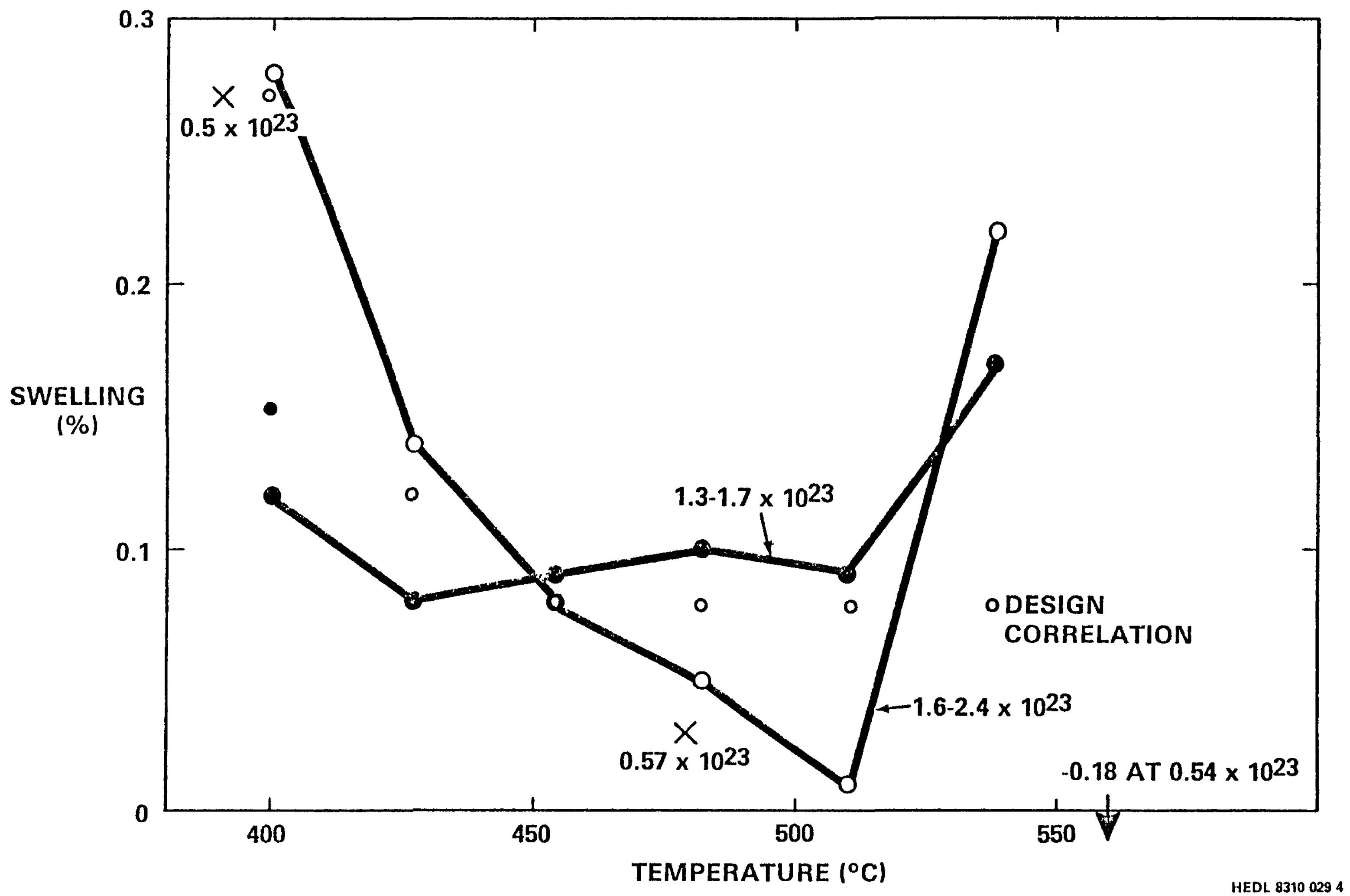

FIGURE 8. Swelling Results as a Function of Irradiation Temperature. Both Density Change and Unstressed Pressurized Tube Data are Shown. The smaller open and closed data points define the swelling correlation prediction for the fluences corresponding to the swelling results shown.

2.4 Discussion

The intent of this work is to develop an understanding of the in-reactor creep and swelling response of 2 1/4 Cr 1 Mo steel and to present the results in a format consistent for its inclusion in the Materials Handbook for Fusion Energy Systems. The latter objective requires that the results be provided in a design equation format. However, it is not within the scope of this project to provide a complete and defendable design equation. Nor is the data set sufficient by itself to provide a clear indication of the functional dependence required for such equations. Therefore, the approach which will be taken will be to assume the necessary functional dependence based on other martensitic steels and expermental ferritic alloys and then to establish the values of the necessary parameters in order to obtain an acceptable fit to the present data sets. Of considerable concern was the establishment of an in-reactor creep equation which was compatible at high temperature with out-of-reactor thermal creep data. This required that the thermal creep dependence of 2-1/4 Cr-1Mo be obtained from the literature.

2.4.1 Swelling Equation

The swelling equational form used is standard and consists of two functional relationships. Void swelling (So) is modeled using a bi-linear functional relationship with three adjustable parameters: a swelling rate, R, a swelling incubation parameter, τ, and a transition parameter, α. Each of these parameters can be specified as a function of temperature.[5] Concurrently, densification (D) is modeled using a functional relationship with two adjustable parameters: a steady state density, D* and a transition paremeter, λ, and again each of these parameters can be specified as a function of temperature. The equations are as follows.

$$\text{Swelling} = \frac{\Delta V}{V_o} (\%) = S_o - D, \qquad (3)$$

where

$$S_0 = R[\phi t + \frac{1}{\alpha} \ln\{\frac{1 + \exp[\alpha(\tau - \phi t)]}{1 + \exp(\alpha\tau)}\}], \quad (3a)$$

$$D = D^*[1 - \exp(-\lambda\phi t)],$$

where

R = steady-state swelling rate parameter, % per 10^{22} n.cm^{-2}
ϕt = fluence in units of 10^{22} n.cm^{-2} (E > 0.1 MeV)
α = transition paramter $(10^{22}$ n.cm$^{-2})^{-1}$
τ = incubation fluence in units of 10^{22} n.cm^{-2} (E > 0.1 MeV),

and τ will be given by

$$\tau = C_1 \exp[C_2 (T - C_3)^2]$$

T = temperature, °K.

2.4.2 In-Reactor Creep Equation

In order to emphasize the need for three terms to describe in-reactor creep in 2-1/4Cr-1Mo, the average creep rates are plotted in Figure 9 assuming that $\epsilon = \overline{B}\,\sigma^{1.5}\,\phi t$. A minimum value of $\overline{B} = 0.4$ is found at 480°C whereas the higher value obtained at 392°C can most easily be interpreted as swelling enhanced creep. Void swelling at 5 x 10^{22} n/cm^2 in 2-1/4Cr-1Mo is established by the present results and by reference 6. Higher values for $\overline{B}$ are also obtained at 550 and 560°C and are interpreted as thermal creep response.

The in-reactor creep is therefore expected to consist of three terms, irradiation creep, swelling enhanced irradiation creep and thermal creep. The functional dependence which has been selected to describe the in-reactor creep of 2-1/4Cr-1Mo is based largely on results for HT-9.[3,7-8] Those results recommend a stress exponent (n) of 1.5 which is somewhat different

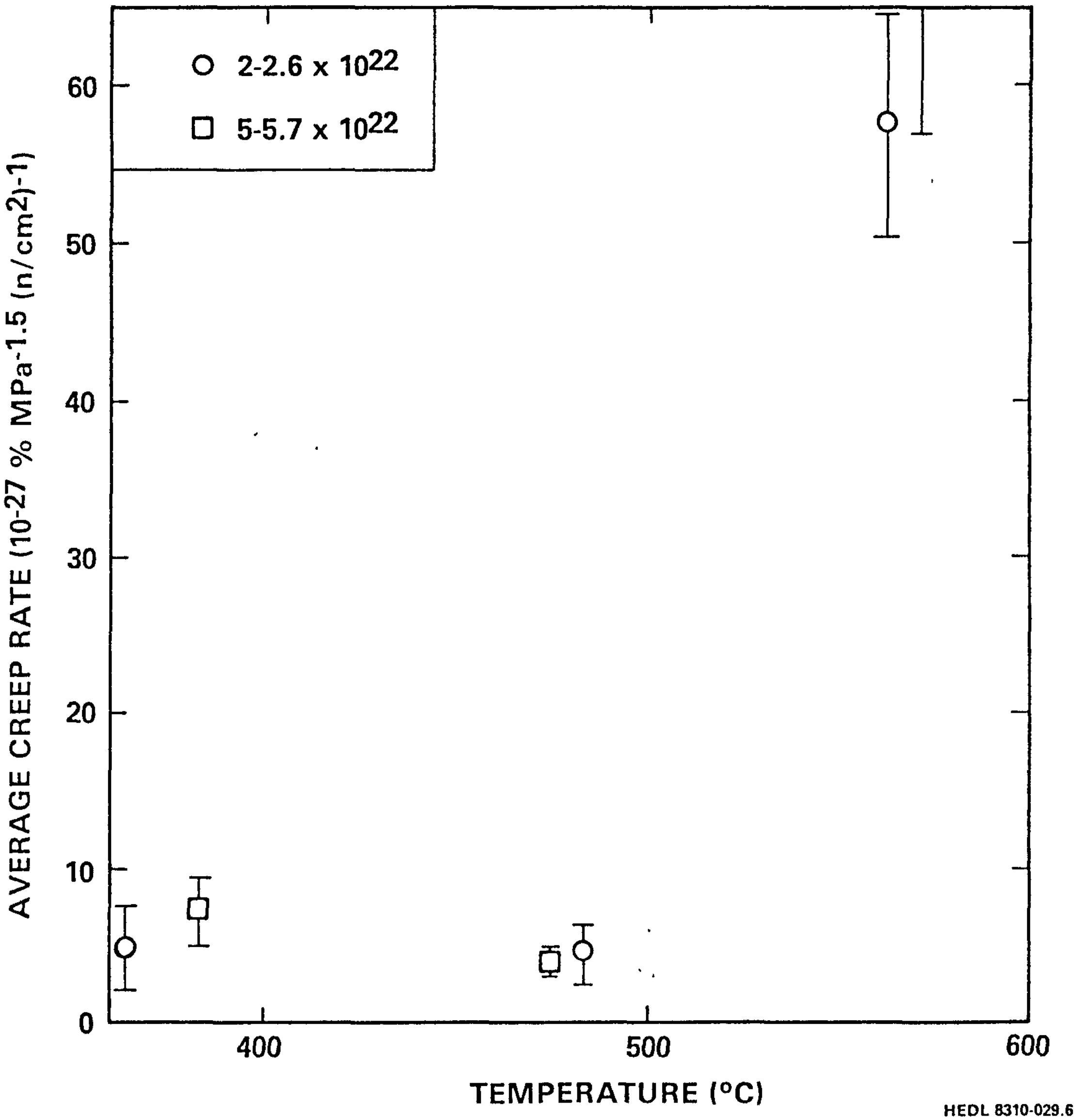

FIGURE 9. Average Creep Rate as a Function of Irradiation Temperature Using a Stress Exponent of 1.5.

from the linear response observed in Figures 5 and 6. Due to the data uncertainties in Figures 5 and 6, the stress exponent of 1.5 is still consistent with the data. Therefore effective in-reactor creep (ε) is defined as

$$\varepsilon = \varepsilon_I + \varepsilon_T \qquad (2)$$

where the irradiation dominated behavior can be described by

$$\varepsilon_I = \overline{B}\ \sigma^n\ Qt + DS\sigma \qquad (2a)$$

and the thermal behavior is defined by ε_T.

The parameters of interest are

ε = effective creep strain, $cm \cdot cm^{-1}$
σ = effective stress, MPa
n = stress exponent
t = time, hr
ϕt = neutron fluence in units of 10^{22}, $n{\cdot}cm^{-2}$ (E > 0.1 MeV)
B = average creep coefficient
D = swelling enhanced creep coefficient
S = fractional swelling rate, % per 10^{22} $n{\cdot}cm^{-2}$
T = temperature, °K
R = gas constant, 1.987 $cal{\cdot}mole^{-1}{\cdot}k^{-1}$

Evaluation of Design Equation Parameters

Insufficient data is available from the AAI test to establish a steady state swelling rate from the present data set. Instead, three data sets for Fe-Cr binary alloys have been used[10-12] which demonstrate a steady state swelling rate of $0.25\%/10^{22}$ $n{\cdot}cm^2$. This steady state swelling rate does not appear to be sensitive to irradiation temperature over the range

400 to 450°C[12] and therefore the temperature dependence of swelling found in the AAI test will be modeled by a temperature dependent incubation parameter, τ. The α parameter has been set at 0.5 based on recent results[13] on other commercial ferritic alloys in the AAI test where it is found that voids form at fluences on the order of 10^{23} n/cm^2 but steady state swelling is not achieved even at fluences on the order of 2 x 10^{23} n/cm^2. The peak swelling temperature was selected based on Fe-Cr binary alloy data[10-12] where peak swelling for Fe-3Cr is found to be in the range 400-425°C, but it was decided to shift the peak swelling temperature slightly downward for 2-1/4Cr-1Mo to 390°C (C_3 = 663°K) based on the present results for pressurized tube and AAI rod specimens from this study. A best fit of the AAI results given the above values for R, α and C_3 established C_1 = 2.0 x 10^{23} n/cm^2 and C_2 = 5 x 10^{-5} (°K)$^{-2}$.

D* was set at 0.08% based on the AAI results at 450°C which are in agreement with the pressurized tube results at 480°C. This densification is expected to be due to Mo_2C precipitation which develops rapidly and therefore x was arbitrarily set at 3 (10^{22} n/cm^2)$^{-1}$. Attempts at defining the complex temperature dependence for densification found at high fluence must await completion of the next phase of this study, microstructural examination of the AAI specimens. The above correlation for swelling in AAI specimens can be altered straightforwardly to describe swelling in the pressurized tubes and therefore account for heat-to-heat and heat treatment effects which arise in the comparison with the AAXIV test. All parameters were held constant except for C_1 and a fit was made for the 390°C pressurized tube results. A best fit ws found with C_1 = 0.75 x 10^{23} n/cm^2. A tabulation of the design correlation parameters is given in Table 6. Table 6 separates the correlation parameters into AAI and AAXIV values. This is intended to emphasize the differences in heat treatment and heat-to-heat variations between specimens in the two experiment.

In determining the parameters for the in-reactor creep correlation for 2-1/4Cr-1Mo the following assumptions were made. At 475°C the average creep rate was 4 x 10^{-27}%$\cdot$MPa$^{-1.5}$(n/cm^2)$^{-1}$. We assumed that this rate was an upper

TABLE 6

ASSIGNMENT OF DESIGN CORRELATION PARAMETERS
FOR SWELLING AND IN-REACTOR CREEP IN 2-1/4CR-1MO

	AAI	AAXIV
Swelling:		
	$R = 0.25\%/10^{22} n\cdot cm^{-2}$	
	$\alpha = 0.5\ (10^{22} n\cdot cm^{2})$	
	$C_1 = 2.0 \times 10^{23} n/cm^2$	$C_1 = 0.75 \times 10^{23} n/cm^2$
	$C_2 = 5.0 \times 10^{-5}\ (°K)^{-2}$	
	$C_3 = 663°C$	
	$D^* = 0.08\%$	
	$\lambda = 3\ (10^{22} n\cdot cm^2)^{-1}$	
Creep:		
		$B = 0.4 \times 10^{-6}\%(MPa)^{-1.5}$
		$D = 2.7 \times 10^{-5}\%/MPa\text{-}10^{22} n\cdot cm^{-2}$

bound for the irradiation induced creep in this material and have set B in equation 2a equal to this rate. The value for D in equation 2a was taken from reference 14. This value for D is typical of results from fitting in-reactor creep in several austenitic stainless steels. The swelling equation for this particular heat of 2-1/4Cr-1Mo has already been described in this report.

Evaluation of the thermal creep contribution ε_T has proven to be too large a problem for the scope of the present work. It has been shown that thermal creep response is very sensitive to the carbon content in 2-1/4Cr-1Mo.[15] Several models have been reported in the literature for describing thermal creep in 2-1/4Cr-1Mo. We chose for investigation the model for tertiary creep given in Reference 16. However, our analysis has not proven to be sufficiently reliable for extensive application. The approach taken[16] was as follows:

$$\varepsilon'_T = -\frac{1}{B} \ln (1 - AB*) \tag{4a}$$

or

$$\dot{\varepsilon}'_T = A \exp (B \varepsilon) \tag{4b}$$

where ε'_T and ε'_T are the true strain and strain rate and where A defines the minimum thermal creep rate and B is a constant which defines the strain at which the creep rate begins to increase significantly. The functional dependence for A and B to best fit the thermal creep data for the bainitic alloy with 0.12%[10] was

$$A = \text{maximum of} \begin{cases} 1.56 \times 10^{-6} \exp \dfrac{-121700}{RT} \quad \sigma^{14} \\ 1.46 \times 10^{-5} \exp \dfrac{-33950}{RT} \quad \sigma^{4} \end{cases} \tag{4c}$$

$$B = 0.206 - 1.17 \times 10^{-3} (T-273 + 6.3 \times 10^{-5} \sigma + 1.4 \times 10^{-6} (T-273)^2 \tag{4d}$$

However, this representation did not satisfactorily fit the AAXIV results at 560°C, nor did it provide a reasonable estimate for the activation energy which controls steady state creep, 121,7 kcal/mole. These results are not unexpected since the carbon content for our pressurized tubes is only 0.04%. The data in reference 10 clearly show that the rupture times decrease and the minimum creep rates increase with decreasing carbon content in this steel. Therefore, we expect to underpreduct the observed creep stains at 565°C for out data. It can be used however, to predict the thermal creep which might be observed at 460°C in comparison to thermal creep at 560°C. Equations 4a and 4b predict thermal creep of 0.1% diameter change at 460°C for a 100 MPa hoop stress with a value for the stress exponent of 4. However, this calculation represents a signifcant extrapolation beyond the data base used in the development of the model. Therefore the calculated strains for this condition have large uncertainties. For this reason it is difficult to assess what fraction of the creep strains observed at 480°C are due to thermal creep mechanisms. In the development of the in-reactor creep equation we have assumed that all the m easured strain at 480°C can be attributed to irradiation induced creep mechanisms. Therefore our equation is necessarily an upper bound for this component of the in-reactor creep strain.

It may be noted that the swelling correlation parameters α and τ can be defined completely based on the AAI results at 400°C. Density change values at fluences of 0, 1.4×10^{23} n/cm^2 and 1.6×10^{23} n/cm^2 give values of $\alpha = 1.06$ and $\tau = 1.5.5 \times 10^{22}$ n/cm^2 if R and D* are fixed as in Table 6. This corresponds at very high fluences to a swelling prediction which is no more than 0.4 percent higher than the correlation defined in Table 6. It is recommended that the values in Table 6 be used based on observed behavior in other commercial ferritic alloys,(13) i.e., $\alpha = 0.5$.

2.5 Conclusions

Diameter change has been measured for a series of pressurized tubes of 2-1/4Cr-1Mo steel in a bainitic condition following irradiation at 390, 480 and 570°C to $\sim 5.5 \times 10^{22}$n/cm^2 (E > 0.1 MeV). The maximum applied hoop stress was 100 MPa in each case. In-reactor creep was found to be lowest at

480°C, and somewhat higher at 390°C. At 570°C failure occurred within the first irradiation period under a 100 MPa hoop stress and evidence for tertiary creep was observed in the 75 MPa hoop stress specimen. These results are interpreted as swelling enhanced creep at 390°C and significant thermal creep contributions at 570°C.

Swelling has been measured for a series of 2-1/4Cr-1Mo steel specimens in a non-standard basinite/tempered-basinite condition following irradiation over the temperature range 400 to 540°C to fluences as high as 2.4 x 10^{23}n/cm^2 (E > 0.1 MeV). Swelling remained below 0.3 percent for all conditions examined and therefore this material si highly swelling resistant. A maximum swelling value of 0.28 percent at 400°C for a fluence of 1.6 x 10^{22}n/cm^2 (E > 0.1 MeV) is interpreted as void swelling whereas a value of 0.22 percent at 540°C and 2.4 x 10^{23}n/cm^2 (E > 0.1 MeV) is believed to be due to in-reactor precipitation.

Diameter change increases measured on unstressed pressurized tubes irradiated at 390°C to 5.0 x 10^{22} n/cm^2 (E > 0.1 MeV) indicate 0.27 percent swelling. This is interpreted as an effect of other heat treatment or composition variations on void swelling in 2-1/4Cr-1Mo steel.

Correlations have been developed to describe the in-reactor creep and swelling of 2-1/4Cr-1Mo steel based on these results.

2.6 References

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