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HIGH-TEMPERATURE FLAW ASSESSMENT PROCEDURE

FINAL REPORT

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LIQUID METAL FAST BREEDER REACTOR
EPRI/CRIEPI/NE JOINT STUDIES

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Engineering Technology Division

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DATE PUBLISHED: August 1991

Prepared for the
Electric Power Research Institute
[Research Project 2658-21 (DOE-ERD-86-586)]

As a part of the
Liquid Metal Fast Breeder Reactor EPRI/CRIEPI/NE Joint Study
on High-Temperature Flaw Assessment Procedure

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managed by
MARTIN MARIETTA ENERGY SYSTEMS, INC.
for the
U.S. DEPARTMENT OF ENERGY
under contract DE-AC05-84OR21400

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ABSTRACT

Described is the background work performed jointly by the Electric Power Research Institute in the United States, the Central Research Institute of Electric Power Industry in Japan and Nuclear Electric plc in the United Kingdom with the purpose of developing a high-temperature flaw assessment procedure for reactor components. Existing creep-fatigue crack-growth models are reviewed, and the most promising methods are identified. Sources of material data are outlined, and results of the fundamental deformation and crack-growth tests are discussed. Results of subcritical crack-growth exploratory tests, creep-fatigue crack-growth tests under repeated thermal transient conditions, and exploratory failure tests are presented and contrasted with the analytical modeling. Crack-growth assessment methods are presented and applied to a typical liquid-metal reactor component. The research activities presented herein served as a foundation for the *Flaw Assessment Guide for High-Temperature Reactor Components Subjected to Creep-Fatigue Loading* published separately.

1. INTRODUCTION

The ever-increasing demand for safety requires that stringent and conservative methodology be used in the design of liquid-metal reactor (LMR) components. Present design guidelines focus on prevention of the initial existence and subsequent propagation of cracks. However, most structures contain cracks or crack-initiating defects that are either inherent in the material or created during manufacturing and installation. To predict correctly the structural response to the in-service loading, it is necessary to assess and account for these existing defects. A joint effort between the Electric Power Research Institute (EPRI) in the United States, the Central Research Institute of Electric Power Industry (CRIEPI) in Japan, and Nuclear Electric plc (NE) in the United Kingdom was started in 1988 to develop a high-temperature flaw assessment procedure for reactor components. The first task of the collaboration was to produce a fracture mechanics data base for both types 304 and 316 stainless steels and a state-of-the-art survey,¹ which were followed in 1989 by a progress report.²

Following is the final report on the background work performed within the collaboration; it summarizes a number of experimental results and analytical procedures. Chapter 2 outlines the sources of data and discusses results of the fundamental deformation and crack-growth tests. Chapter 3 is devoted to the review of existing creep-fatigue crack-growth models and to identification of the most promising method. In Chap. 4 results of subcritical crack-growth exploratory tests, creep-fatigue crack-growth tests under repeated thermal transient conditions, and exploratory failure tests are presented and contrasted with the analytical modeling. Crack-growth assessment methods presented in Chaps. 3 and 4 are applied to a typical LMR component in Chap. 5. Finally, experimental and analytical results are summarized, and concluding remarks are made in Chap. 6.

2. MATERIALS DATA

2.1 EXPERIMENTAL INVESTIGATION OF FUNDAMENTAL DEFORMATION BEHAVIOR—CRIEPI

The objective of this task is to study the fundamental deformation characteristics of the test material. The material was austenitic type 304 stainless steel, annealed at 1100°C for 30 min. Basic material properties are given in Table 1.

2.1.1 Cyclic Deformation Tests

Cyclic deformation tests were conducted using tensile specimens (10 mm in diameter in the gage section) at 200, 400, 550, and 650°C. The experimental strain range stepup procedure is schematically shown in Fig. 1. A summary of tests is presented in Table 2. Specimens were subjected to axial strain-controlled cyclic loading with the strain rate of 10^{-3} s^{-1} and zero mean strain. The stress range ultimately attained for each strain range was used to construct a cyclic stress-strain curve. The stress range–strain range curves for the four temperatures under consideration are plotted in Fig. 2. Also given in Fig. 2 are the Ramberg-Osgood (R-O) type approximations of the experimental data at each temperature. Corresponding temperature-dependent material constants α and n (used in the R-O approximations) are presented in

Table 1. Fundamental characteristics of
type 304 stainless steel

Chemical composition, wt %	
Carbon	0.05
Silicon	0.78
Manganese	1.21
Phosphorus	0.02
Sulfur	0.001
Nickel	9.5
Chromium	18.7
Nitrogen	0.04
Mechanical properties	
At room temperature	
Proof stress, MPa	275
Tensile strength, MPa	608
Elongation, %	64
Hardness, Hv	163
At 550°C	
Proof stress, MPa	127
Tensile strength, MPa	382
Elongation, %	39
Hardness, Hv	—

Fig. 1. Strain range stepup procedure used for construction of cyclic stress-strain curve.
(a) Strain range stepup procedure and (b) stress range vs strain range diagram.

Table 2. Summary of CRIEPI cyclic deformation tests

T (°C)	$\Delta\epsilon$ (%)					
	0.3	0.5	0.7	1.0	1.4	2.0
200	X	X	X	X	X	X
400	X	X	X	X	X	X
550	X	X	X	X	X	X
650	X	X	X	X	X	X

Note: T = temperature
 $\Delta\epsilon$ = total strain range
 $\dot{\epsilon} = 0.1\%/s$

Fig. 2. Stress range vs strain range at various temperatures.

Table 3. Large cyclic hardening can be observed, particularly in the intermediate temperature range.

2.1.2 Creep Deformation Tests

Creep deformation tests, conducted at four different constant stress levels at 550 and 650°C, are summarized in Table 4. Variations of creep strain with time at 550 and 650°C are given in Figs. 3 and 4, respectively. Predictions were obtained with the EPICC creep equation, a

Table 3. Constants used in Ramberg-Osgood approximations for cyclic stress-strain relations

T (°C)	Constants		
	E	α	n
200	186,000	2.46×10^{-12}	3.54
400	172,000	3.06×10^{-10}	2.7
550	157,000	3.06×10^{-10}	2.7
650	147,000	5.08×10^{-14}	4.2

Note: $\Delta\epsilon = \frac{\Delta\sigma}{E} + \alpha(\Delta\sigma)^n$ with $\Delta\epsilon$ (mm/mm) and $\Delta\sigma$ (MPa)

T = temperature

Table 4. Summary of CRIEPI creep deformation tests

T (°C)	σ (MPa)			
	108	127	235	274
550			X	X
650	X	X		

Note: T = temperature
 σ = stress

Fig. 3. Creep curves obtained at 550°C.

Blackburn type equation developed by the Japan Society of Mechanical Engineers (JSME), with the coefficient α_R fitted to the test data. Parameters used in the EPICC equations are presented in Table 5; predictions are plotted in Figs. 3 and 4 as well. Calculated and experimental results agree fairly well. Results presented here were used for evaluating the creep-fatigue crack-growth data.

2.1.3 Cyclic Deformation Tests—NE

Cyclic deformation tests on type 321 stainless steel at 650°C have been performed by NE in a manner similar to those described in Sect. 2.1.1 for type 304 stainless steel. Again the R-O

Table 5. Parameters used in EPICC equations

$\epsilon_c = C_1[1 - \exp(-r_1 t)] + C_2[1 - \exp(-r_2 t)] + \dot{\epsilon}_m t$	
where	
r_1	$= S_1 t_R^{\alpha_1}$
r_2	$= S_2 t_R^{\alpha_2}$
C_1	$= \frac{G_1 \dot{\epsilon}_m^{\beta_1}}{r_1}$
C_2	$= \frac{G_2 \dot{\epsilon}_m^{\beta_2}}{r_2}$
$\dot{\epsilon}_m$	$= F t_R^{-\lambda}$
$\log_{10}(\alpha_R t_R)$	$= \begin{cases} B_0 + B_1 \log_{10} \sigma + B_2 (\log_{10} \sigma)^2 \\ \text{for } 2.0 \leq \sigma \leq 38.0 \\ [B_0 - B_2 (\log_{10} \sigma)^2] + (B_1 + 2B_2 \log_{10} \sigma) \log_{10} \sigma \\ \text{for } 0.01 \leq \sigma \leq 2.0 \end{cases}$
T	$425 \leq T \leq 650$
S_1	40.0
α_1	-0.6667
S_2	14.799
α_2	-0.85439
G_1	2.408
β_1	0.7908
G_2	0.81437
β_2	0.87084
F	$65.48 \exp\left\{-\frac{42,750}{8.31(T + 273.15)}\right\}$
λ	1.1296
B_0	$-16.07703 + \frac{22,968.2}{(T + 273.15)}$
B_1	$-\frac{2665.10}{(T + 273.15)}$
B_2	$-\frac{1763.01}{(T + 273.15)}$
α_R	1.0 for average creep curve

Note: ϵ_c = mm/mm
 σ = kgf/mm²
time = h
temperature = °C

Fig. 4. Creep curves obtained at 650°C.

equation was found to be a convenient description of observed material response in terms of relating the total strain range $\Delta\epsilon$ to total stress range $\Delta\sigma$:

$$\Delta\epsilon = \Delta\sigma/E + \alpha(\Delta\sigma)^m, \quad (1)$$

where E is Young's modulus.

For the type 321 stainless steel tested by NE, the R-O equation was found to be capable of providing a good fit to the stress-strain loading path as well as to the locus of the ranges of stress and strain as in Eq. (1). Further, the constants could be derived from Eq. (1) by assuming that the loading was symmetrical and that unloading to zero stress level was fully elastic. This leads to the loading path being described by:

$$\epsilon'(\sigma) = \sigma/E + \alpha(2\sigma)^m, \quad (2)$$

where ϵ' is the tensile-going strain at stress σ . However, in general the constants in Eqs. (1) and (2) are expected to differ.

2.1.4 Creep Deformation Tests—NE

Creep deformation tests on austenitic steels in the cyclically conditioned state have been performed within NE at constant strain to examine stress relaxation behavior. It has been found that stress relaxation under such conditions can be described by a creep equation relating creep strain rate $\dot{\epsilon}$ to stress σ :

$$\dot{\epsilon} = 2A' \sinh \left\{ (\sigma - \sigma_0)/B'\sigma_0 \right\}, \quad (3)$$

where σ_0 is the stress at the start of the dwell, and A' , B' , and σ_i are material and temperature-dependent constants. The constant σ_i is an internal or back stress, such that $\dot{\epsilon} = 0$ for $\sigma \leq \sigma_i$, and has been found to lead to an improved fit to the relaxation data. The values of the constants for type 321 stainless steel at 650°C are $A' = 3.6 \times 10^{-7} \text{ h}^{-1}$, $B' = 0.055$, and $\sigma_i = 51 \text{ MPa}$.

2.2 FUNDAMENTAL CRACK-GROWTH EXPERIMENTS—CRIEPI/NE

2.2.1 Cyclic Crack-Growth Tests—CRIEPI

Thin-walled tubular specimens shown in Fig. 5 were used in fundamental crack-growth tests (presented in Sects. 2.2.1 and 2.2.2). A crack starter, namely a 0.8-mm-diam hole, was machined in the gage section of each specimen.

Standard cyclic crack-growth tests were performed to obtain the fundamental crack-growth characteristics of the test material under cyclic loads. All tests were stress-controlled, with the frequency of 0.5 Hz. Test conditions are summarized in Table 6.

Fig. 5. Crack-growth test specimen (unit: 1 mm).

Table 6. Summary of CRIEPI cyclic crack-growth tests

T (°C)	$\Delta\sigma$ (MPa)			
	235	294	392	490
200				X
400			X	
550	X	X	X	X
650			X	

Note: T = temperature

$\Delta\sigma$ = nominal stress range (fully reverse loading)

frequency 0.5 Hz

Specimen elongation was measured along the 10-mm gage length and then used in the experimental evaluation of the J-integral type fracture mechanics parameters. Crack length was measured on the outer surface of the specimen by inspection under a microscope. Crack propagation rate was calculated by numerically differentiating the plots of the crack length vs the number of cycles. Data points used in the evaluation were those produced for the half crack length values between 1.6 and 2.6 mm.

In Fig. 6, crack-growth rates were correlated with the elastic J-integral range ΔJ_e given by:

$$\Delta J_e = \frac{\Delta K_{eff}^2}{E} \quad (4)$$

where ΔK_{eff} and E are effective stress-intensity factor range and Young's modulus, respectively. ΔK_{eff} was evaluated using a secant formula for center-cracked specimens:

$$\Delta K_{eff} = \left(\frac{\Delta P_{eff}}{w t} \right) (\pi a)^{0.5} \left[\sec \left(\frac{\pi a}{w} \right) \right]^{0.5} \quad (5)$$

Fig. 6. Correlation between crack-growth rate and elastic J-integral range.

Here ΔP_{eff} is the difference between the maximum load and the crack-opening load estimated from load-displacement curves, w is the circumferential length at midthickness of the specimen, t is the thickness, and a is the half crack length measured at midthickness.

Judging from the shape of the hysteresis loop under the present test conditions, crack closure either did not occur at all or occurred in the vicinity of the compressive peak load. Therefore, ΔP_{eff} is approximately equal to the total load range ΔP . The graphs of crack-growth rate vs ΔJ_e at 200, 400, 550, and 650°C are shown in Fig. 6. Comparison of the data obtained at different temperatures for the same stress conditions indicates that the large scatter in Fig. 6 is primarily due to the temperature dependence.

Elastic-plastic effective fatigue J-integral range ΔJ_{eff} was calculated as a sum of elastic and plastic contributions ΔJ_e and ΔJ_p . As shown in Fig. 7, the value of ΔJ_p was computed from the load-displacement curve with the method originally proposed by Rice et al.³ for center-crack specimens. Correlation between the crack-growth rate and ΔJ_{eff} is presented in Fig. 8 for all test conditions. All data fall within a narrow band. No temperature dependence (akin to that demonstrated in Fig. 6) is observed. The following power-law relation between the crack-growth rate and ΔJ_{eff} was proposed:

$$\frac{da}{dN} = 3.89 \times 10^{-5} \Delta J_{\text{eff}}^2 \quad (6)$$

It is seen in Fig. 8 that Eq. (6) approximates experimental results fairly well. Comparison of graphs in Figs. 6 and 8 clearly demonstrates the advantage of using ΔJ_{eff} vs ΔK_{eff} in the elastic-plastic case. It was also ascertained that the scatter of data presented here is no greater than that of the data reported by Ohtani⁴ for various metallic materials and different specimen geometries.

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Fig. 7. Experimental determination of effective elastoplastic fatigue J-integral range.

Fig. 8. Correlation between crack-growth rate and effective elastic-plastic fatigue J-integral range.

2.2.2 Creep-Fatigue Crack-Growth Tests—NE/CRIEPI

2.2.2.1 Cyclic displacement data for type 321 stainless steel—NE

Creep-fatigue crack-growth data have been produced for both base and simulated heat-affected zone (HAZ) type 321 stainless steel at 650°C in air and reported by Gladwin et al.⁵ Before testing, the type 321 stainless steel was aged for 200 h at 750°C. Further heat treatment was used to produce simulated HAZ material. Following heat treatment, compact tension (CT) specimens of two thicknesses (12.5 and 12 mm) were machined from the plate material. Pure fatigue, fully reversed displacement-controlled tests with constant displacement tensile hold periods and constant load tests were performed. Detailed measurements of load and crack length during each cycle were made so that the total crack-growth could be separated into cyclic and hold period (creep) components.

The crack-growth rate data are correlated in Fig. 9 with the values of C^* deduced from the experimentally measured rates of load drop during the hold periods. Also included in Fig. 9 are data from the constant load tests. It can be seen that there is some scatter in the data. However,

Fig. 9. Creep crack-growth response of aged type 321 stainless steel and HAZ at 650°C. Comparison of cyclic and static data.

no systematic difference between the base material and the simulated HAZ or between the cyclic and static data was observed. The data can be fitted by the equation

$$\dot{a} = 0.005(C^*)^{0.9}, \quad (7)$$

where $\dot{a}$ is measured in meters per hour and C^* is measured in megapascals-meters/hour. This equation is in reasonable agreement with data presented by Ohtani et al.⁶ on type 321 stainless steel at 650 and 700°C, as noted by Gladwin et al.⁵

2.2.2.2 Constant load data for type 316 stainless steel—NE

Constant load tests on solution-treated type 316 stainless steel at 600°C have been performed by Bolton.⁷ CT specimens of various thicknesses (1.5, 2, and 2.5 in.) were tested to examine size dependence. The crack-growth rate data are correlated with the values of C^* deduced from the experimentally measured displacement rates given in Fig. 10. The data are well fitted by the equation

$$\dot{a} = 0.005(C^*)^{0.85}, \quad (8)$$

where $\dot{a}$ is measured in meters per hour and C^* is measured in megapascals-meters/hour. The data do not show any dependence on specimen size. Note that Eq. (8) is similar to Eq. (7) obtained for type 321 stainless steel.

Fig. 10. Creep crack-growth response of type 316 stainless steel at 600°C for CT and SENT specimens of various thickness.

These tests have been complemented by two further tests on single-edge-notch tension (SENT) specimens with a gage length of 40 mm, a width of 10 mm, and a breadth of 4 mm. The data lie close to the line given by Eq. (8) as shown in Fig. 10.

Creep-fatigue crack-growth tests on type 316 stainless steel are being performed using displacement controlled cycles with tensile hold periods ranging from 0.5 to 192 h. Geometrically similar CT specimens of various thicknesses (1, 1.5, 2, and 2.5 in.) are being used in this study. So far, three 1-in. specimens and one 2-in. specimen have been tested. Results for crack length as a function of time are shown in Figs. 11–13. At present these data are

Fig. 11. Crack length vs time for the 0.5-h dwell creep-fatigue tests on 1-in. CT specimens.

Fig. 12. Crack length vs time for the 0.5-h dwell creep-fatigue test on 2-in. CT specimens.

Fig. 13. Crack length vs time for the 8-d dwell creep-fatigue test on 1-in. CT specimens.

being separated into cyclic and creep components of crack growth so that the crack-growth rate during the dwell can be correlated with C^* as in Fig. 10.

2.2.2.3 Stress-controlled tests on type 304 stainless steel—CRIEPI

Stress-controlled fully reversed cyclic tests were conducted at 550°C. Each cycle included hold periods at both maximum and minimum stress levels, which ranged from 10 min to 5 h. Test conditions are summarized in Table 7.

Change in half crack length with loading cycles is shown in Fig. 14, where the effect of stress hold can be observed. In Fig. 15, crack-growth rates are plotted vs the hold time for several crack lengths. As the hold time increases from 0 to 10 min, a slight decrease in the crack-growth rate is observed. This decrease can be explained by increasing deformation

Table 7. Summary of CRIEPI stress-controlled creep-fatigue tests

T (°C)	t_H (h)		
	1/6	1	5
550	X	X	X

Note: T = temperature

t_H = hold time

$\Delta\sigma = 490$ MPa (fully reverse loading)

Fig. 14. Variation of half crack length with loading cycles. Effect of stress hold.

Fig. 15. Crack-growth rate vs hold time for various crack lengths.

resistance of material due to dynamic strain aging. For hold times >10 min, crack-growth rate per cycle increases consistently. Crack-growth rates produced for the 5-h hold period are ~1 order of magnitude larger than those produced for zero hold time.

A combination of the effective fatigue J-integral range ΔJ_{eff} and the creep J-integral range ΔJ_c was used to correlate creep-fatigue crack-growth. The method for experimental determination of ΔJ_{eff} and ΔJ_c from the load-displacement curves is schematically shown in Fig. 16. The shape of the hysteresis loops gave no indication of crack closure near the compressive peak, thus $\Delta J_{\text{eff}} = \Delta J_f$ was assumed.

Fig. 16. Experimental determination of creep and fatigue J-integral ranges.

Crack-growth rates obtained during tests with and without stress holds are plotted vs ΔJ_{eff} in Fig. 17. Note that due to softening of the material during the stress hold periods, the value of ΔJ_{eff} increases with increasing hold times for the same stress amplitude and crack length. The plot of the creep J-integral range vs the half crack length (see Fig. 18) points to a nearly linear relation between these quantities. Increase in crack-growth rate due to introduction of hold time was calculated by subtracting cyclic contribution, obtained from Eq. (6) in Sect. 2.2.1, from the observed crack-growth rate. This increase in crack-growth rate is plotted vs ΔJ_c in Fig. 19 for the hold times of 1 and 5 h. Current test results compare well with the data reported by Ohtani et al.⁶: experimental data presented here fall within the scatter band of the data reported in Ref. 6 with the exception of a few data points.

2.2.2.4 Displacement-controlled tests on type 304 stainless steel—CRIEPI

A series of displacement-controlled fully reversed cyclic tests was conducted at 550°C. Test conditions are summarized in Table 8. Displacement rate was controlled within the gage length of 10 mm. Nominal strain range was 0.8%. Two different test histories employed strain rates of 10^{-3} and $1.67 \times 10^{-6} \text{ s}^{-1}$ on the tensile portion of the cycle. A single strain rate of 10^{-3} s^{-1} was used on the compressive portion of the cycle in all tests.

Figure 20 presents the crack-growth rate as a function of the effective fatigue J-integral range ΔJ_{eff} for the displacement-controlled tests, together with the data band produced by stress-controlled cyclic crack-growth tests described in Sect. 2.2.1. In displacement-controlled tests, the crack-growth rate increases as the crack continues to propagate, although only slight

Fig. 17. Crack-growth rate vs fatigue J-integral range for stress hold tests.

Fig. 18. Variation of creep J-integral range with half crack length.

Fig. 19. Increase in crack-growth rate due to stress hold vs creep J-integral range.

Table 8. Summary of CRIEPI
displacement-controlled
creep-fatigue tests

T (°C)	$\dot{\epsilon}_t$ (%/s)	
	0.1	0.000167
550	X	X

Note: T = temperature
 $\dot{\epsilon}_t$ = strain rate for tension-going
 $\dot{\epsilon}_c = 0.1\%/s$ = strain rate for
compression-going
 $\Delta\epsilon = 0.8\%$

Fig. 20. Crack-growth rate vs fatigue J-integral range for displacement-controlled crack-growth tests.

changes in ΔJ_{eff} are observed. This result suggests the need for a detailed assessment of the effect of prior creep damage on crack-growth rates.

The data points produced in tests, where the strain rate of $10^{-3} s^{-1}$ was used in both tension and compression, fall within the data band generated in stress-controlled cyclic tests. Conversely, the crack-growth rates produced in tests, employing different strain rates in tension and in compression, are approximately by a factor of 2 higher than those obtained in the

constant strain rate tests. This result points to the importance of loading-rate effect in the evaluation of cyclic contribution to crack growth.

3. IDENTIFICATION OF THE POTENTIAL CREEP-FATIGUE CRACK-GROWTH MODEL

3.1 SIMPLIFIED METHODS—NE

Within the NE, simplified methods for calculating creep-fatigue crack growth are currently being developed as part of the R5 assessment procedure for the high-temperature response of structures. The procedure separates the total crack growth per cycle into fatigue and creep components:

$$\frac{da}{dN} = \left(\frac{da}{dN} \right)_f + \left(\frac{da}{dN} \right)_c \quad (9)$$

The fatigue component is described by a modified Paris law:

$$\left(\frac{da}{dN} \right)_f = C(\Delta K_{eff})^l, \quad (10)$$

where C and l are constants. The effective stress-intensity factor range ΔK_{eff} represents the opening part of the total stress-intensity factor range as depicted in Fig. 21. In the simplified approach, it is expressed as a fraction of the total stress-intensity factor range ΔK as follows:

$$\Delta K_{eff} = q_0 \Delta K \quad (11)$$

The fraction q_0 is a function of the ratio of the minimum and maximum stress-intensity factors during the cycle as shown by the line in Fig. 22. This line is an approximation based on data collected on a wide range of ferritic and austenitic steels. This was confirmed as a conservative

Fig. 21. Crack-opening parameter q_0 . Evaluation of q_0 .

Fig. 22. Definition of the parameter q_0 used to define ΔK_{eff} .

representation by the results on type 321 stainless steel described in Sect. 2.1.3. Experimental values of q_0 are compared to the analytical line in Fig. 23.

Although Eq. (10) is a simple representation of fatigue crack-growth data, it is found that the constants C and l are dependent on the hold time in the cycle because the crack is expected to grow through material that has seen significant creep damage during the dwell time. The effect of total time on fatigue crack-growth data for the type 321 stainless steel is shown in Fig. 24. However, only the crack-growth during the fatigue part of the cycle is shown in Fig. 24. The creep crack-growth component has been separated and was presented in Fig. 9.

As creep crack-growth rates follow Eq. (7), the creep contribution to the total crack growth in Eq. (9) can be represented as

$$\left(\frac{da}{dN} \right)_c = \int_0^{t_h} AC * q \, dt, \quad (12)$$

where t_h is the total time in the cycle, and A and q are constants in the creep crack-growth equation.

Fig. 23. Effect of the R ratio on the q_0 for type 321 stainless steel at 650°C.

A difficulty with the C^* approach in Eq. (12) is that C^* has to be evaluated for cracks in complex geometries. Within NE, approximate reference stress methods have been developed to provide estimates of C^* . The approximation is

$$C^* = \sigma_{\text{ref}} \dot{\epsilon}_{\text{ref}}^C R' , \quad (13)$$

where the reference stress σ_{ref} is defined by

$$\sigma_{\text{ref}} = P \frac{\sigma_Y}{P_L(\sigma_Y, a)} , \quad (14)$$

and R' is a length parameter

$$R' = \frac{K^2}{\sigma_{\text{ref}}^2} . \quad (15)$$

Here P is the magnitude of the applied load, and P_L is the corresponding plastic collapse load for a yield stress σ_Y and crack size a . K is the value of the elastic stress-intensity factor at the load P , and $\dot{\epsilon}_{\text{ref}}^C$ is the creep strain rate from uniaxial creep data at the reference stress level.

Fig. 24. Cyclic response of aged type 321 stainless steel and HAZ at 650°C.

The reference stress approach is useful for practical applications for the following reasons:

1. In Eq. (13) it is necessary to calculate only the stress-intensity factor and the limit load. Such solutions are widely available because of their use in low-temperature fracture assessment procedures such as R6.
2. Equation (14) is not restricted to secondary creep described by a simple power law. It allows realistic creep data, including primary, secondary, and tertiary stages to be incorporated in the creep strain rate at the reference stress. The effect of cyclic hardening or softening on the material creep data can also be readily included.
3. Strain-hardening rules can be used to allow for the increase in stress levels as a crack grows, by interpreting the creep strain rate as the strain rate at the current reference stress and at the creep strain accumulated under the reference stress history.

The validity of using realistic creep laws in applying strain-hardening rules has been checked for a range of materials by comparing Eq. (13) with the test data, for which C^* can be

estimated from experimental displacement rate measurements or experimental load drop measurements. In Fig. 25 the predictions of Eq. (13) are compared with the experimental data described in Sect. 2.1.3 for type 316 stainless steel. A plane stress reference stress has been used to evaluate C_{ref}^* . Several double-edge-notched tension (DENT) specimens were tested as well. In this case C_{ref}^* is compared to C^* ($\dot{a}_{DENT}$). The values of C^* ($\dot{a}_{DENT}$) are obtained by entering Fig. 10 at the experimentally measured crack-growth rates ($\dot{a}_{DENT}$) for the DENT specimens and reading off the corresponding values of C^* according to the fit of Eq. (8). The good agreement in Fig. 25 indicates that the use of the reference stress estimate of C^* in conjunction with data ($\dot{a}C^*$) derived from CT specimens provides a good estimate of crack growth in other geometries (in this case, the DENT geometry).

Fig. 25. Comparison of the predicted value of C^* , C_{ref}^* with that deduced from experimental data, C_{exp}^* , for type 316 stainless steel, for CT specimens of various thickness and for DENT specimens.

Under cyclic displacement loading it is necessary to include the variation of creep strain rate during the dwell period as the stress levels relax in Eq. (13). It has been found that the rate at which the reference stress falls can be represented as:

$$\dot{\sigma}_{ref} = -E \frac{\dot{\epsilon}_{ref}^C}{\mu}, \quad (16)$$

where μ is a factor estimated from the elastic and creep compliances ($\mu \cong 2$ for the CT specimens). Predictions of Eq. (13) for the cyclic data given Fig. 9 are shown in Fig. 26. It is seen that the reference stress predictions are close to the experimental data points.

Fig. 26. Comparison of the predicted value of C^* , C_{ref}^* , with that deduced from experimental data, C_{exp}^* , for type 321 stainless steel (after Gladwin et al.⁵).

In the reference stress predictions shown in Fig. 26, it should be noted that creep data were obtained from stress relaxation tests on the cyclically hardened material. Had creep data from constant load tests on virgin material been used, the good agreement in Fig. 26 would not have been obtained. Results similar to those shown in Fig. 26 have been obtained for austenitic weld metals and for ferritic steels. These suggest that the reference stress methods developed for constant load can be extended in a simple manner to predict crack growth under cyclic conditions. The effect of the creep-fatigue interaction appears to be reflected in the effect of the total cycle time on the fatigue crack growth, as illustrated in Fig. 24.

3.2 CYCLIC CRACK-GROWTH MODELING—CRIEPI

3.2.1 General Electric (GE)/EPRI Method^{8,9}

Results of cyclic crack-growth tests described in Sect. 2.2.1 demonstrate that the experimentally determined effective fatigue J-integral range ΔJ_{eff} correlates crack growth under rapid cyclic loading fairly well. It is therefore important to establish a method for analytical evaluation of ΔJ_{eff} . Two such analytical evaluation procedures are considered here. Note, however, that because the crack closure was not observed in experiments presented in Sect. 2.2.1, these procedures do not account for the effect of crack closure (i.e., $\Delta J_{eff} = \Delta J_f$).

The first method uses nonlinear finite-element solutions tabulated in a nondimensional form for typical specimen geometries. These fully plastic solutions were produced by GE/EPRI^{8,9} for ductile fracture analysis. However, they can also be used to estimate ΔJ_f as long as the cyclic stress-strain relation is represented by the R-O equation of the following form:

$$\Delta \varepsilon = \frac{\Delta \sigma}{E} + \alpha (\Delta \sigma)^n, \quad (17)$$

where $\Delta \varepsilon$ and $\Delta \sigma$ are the strain and stress ranges at steady-state cycling, E is Young's modulus, α and n are temperature-dependent material constants. Results presented in Fig. 2 indicate that for a range of temperatures Eq. (17) approximates the behavior of the test material fairly well.

Like the J-integral in the elastic-plastic monotonic fracture analysis, the fatigue J-integral range can be represented as the sum of elastic and plastic contributions:

$$\Delta J_f = \Delta J_e + \Delta J_p. \quad (18)$$

For a center cracked panel:

$$\Delta J_e = \frac{\Delta K^2}{E}, \quad (19)$$

$$\Delta J_p = \alpha \alpha \left(\frac{c}{b} \right) h_1 \left(\frac{a}{b}, n \right) \left(\frac{\Delta P}{2ct} \right)^{n+1}, \quad (20)$$

where a , b , c , and t represent half crack length, half specimen width, half ligament length, and specimen thickness, respectively; and h_1 is the nondimensionalized fully plastic solution for the J-integral tabulated as a function of a/b and n (Ref. 9). To apply these equations to the tubular specimen employed in the present study, the following substitution was made:

$$b = \pi r, \quad (21)$$

where r represents the mean radius of the specimen. For relatively short cracks considered here, this approximation should not introduce significant error into ΔJ_f .

Values of ΔJ_f calculated with Eq. (18) were compared to those determined from the experimental load-displacement hysteresis loops. Results presented in Fig. 27 demonstrate good agreement except for small stress ranges, where ΔJ_f is overestimated. A possible explanation for this discrepancy is that the cyclic stress-strain relation in the present study was established based on the strain-range step-up tests and therefore cannot truly represent the steady-state behavior, especially in the small strain regime.

Fig. 27. Comparison of fatigue J-integral range values obtained by reference stress method with those determined experimentally.

3.2.2 Reference Stress Method

The reference stress method was developed as a tool for evaluating the inelastic deformation of structures based on results of elastic analysis.¹⁰ According to the reference stress method, ΔJ_f can be represented as follows:

$$\Delta J_f = \frac{E \Delta \epsilon_{ref}}{\Delta \sigma_{ref}} \Delta J_e, \quad (22)$$

where $\Delta \sigma_{ref}$ is the reference stress given by

$$\Delta \sigma_{ref} = \frac{\Delta P}{2ct}, \quad (23)$$

and $\Delta \epsilon_{ref}$ is the strain range corresponding to $\Delta \sigma_{ref}$ on the material cyclic stress-strain curve. $\Delta \epsilon_{ref}$ can be obtained by substituting $\Delta \sigma_{ref}$ for $\Delta \sigma$ in Eq. (17):

$$\Delta \epsilon_{ref} = \frac{\Delta \sigma_{ref}}{E} + \alpha (\Delta \sigma_{ref})^n. \quad (24)$$

Values of ΔJ_f calculated with Eq. (22) were compared to those obtained experimentally and to those obtained with the GE/EPRI method in Figs. 28 and 29, respectively. Results in Fig. 29 show that the reference stress method yields somewhat lower values of ΔJ_f than the

Fig. 28. Comparison of fatigue J-integral range values obtained by GE/EPRI method with those determined experimentally.

Fig. 29. Comparison of fatigue J-integral range values obtained by reference stress method with those determined by GE/EPRI method.

GE/EPRI method. This trend becomes more pronounced as the stress range and plastic contribution to ΔJ_f increase. However, agreement with the experimental values seems acceptable for application purposes.

Note that this technique requires neither the fully plastic solutions nor the approximation of the cyclic stress-strain relation by a R-O type equation. It can therefore be applied in cases where linear elastic and limit load solutions exist.

3.3 CREEP-FATIGUE CRACK-GROWTH MODELING—CRIEPI

3.3.1 Finite-Element Analyses

3.3.1.1 Finite-element model and loading conditions

A general-purpose finite-element code (MARC) was employed in finite-element calculations. The focus and objective of the finite-element work was a detailed study of the analytical evaluation methods of the fracture mechanics parameters. A center-cracked panel (shown in Fig. 30) was analyzed. This panel was modeled after the geometry of the cylindrical test specimen used in the creep-fatigue crack-growth tests. Plane stress conditions were assumed.

Analytical and corresponding experimental loading conditions are schematically shown in Fig. 31. In the analyses, the actual creep-fatigue cyclic loading was simulated by monotonic loading followed by a constant load hold time.

Due to symmetry of both structure and loading conditions, only one-quarter of the panel was modeled. The finite-element mesh is shown in Fig. 32. Eight-noded isoparametric elements were used. Five paths employed for the evaluation of J-integral and C(t)-integral are presented

Fig. 30. Center-cracked panel geometry.

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Fig. 31. Loading conditions used in test and in finite-element analysis.

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Fig. 32. Finite-element model and integration paths.

in Fig. 32. For the monotonic part of the loading history, J-integral was calculated according to the path-integration formula. Conversely, during the stress hold periods, C(t)-integral was calculated for the same paths.

The semistress range vs semistrain range relation established in the fundamental cyclic deformation tests (Sect. 2.1.1) was used to determine the elastic-plastic properties of the material. The EPICC equations with and without primary creep strain terms were used, with $\alpha = 1.4$ determined from the fundamental creep deformation test (see Sect. 2.1.2). Conditions of the analyses are summarized in Table 9.

Table 9. Summary of finite-element analysis employing EPICC equations

Condition	Case			
	A	B	C	D
Holding stress, MPa	245	245	98	98
Holding time, h	5	5	1000	1000
Creep equation	No primary creep strain	With primary creep strain	No primary creep strain	With primary creep strain

3.3.1.2 Results of analyses

Variation in J-integral calculated at five integration paths is shown in Fig. 33. The J-integral values obtained from paths 2–4 are consistent and in good agreement. Path 1 (the

Fig. 33. J-integral vs applied stress.

shortest path) yields a lower value compared to the other paths. This can be explained by the poor accuracy of line integration at path 1. Results of J-integral evaluation by the GE/EPRI method and the reference stress method are also included in Fig. 33. Good agreement between the path integral values (except path 1) and the values obtained by the GE/EPRI method inspires confidence in the present finite-element calculation.

Variations in $C(t)$ -integral calculated at four paths are shown in Figs. 34–37. Because the line integration at path 1 is not sufficiently accurate, path 1 was not included in Figs. 34–37. For paths 2–4, shorter paths tend to yield larger values of $C(t)$ for a short period of time. However, the path-dependency decreases with time.

To evaluate $C(t)$ during transient creep for elastic-power-law creep materials, Ehlers and Riedel¹¹ proposed the following expression:

$$C(t) = \frac{J_{el}}{(m+1)t} + C^* , \quad (25)$$

where J_{el} is the elastic J-integral at the start of the stress hold period. To include effects of instantaneous plasticity preceding the hold time and the transient creep during the hold time, the following expression was employed to estimate $C(t)$:

$$C(t) = \frac{J}{(n+1)t} + C^* \left[\frac{\dot{\epsilon}_c(\sigma_{ref}, t)}{\dot{\epsilon}_{cN}(\sigma_{ref})} \right] , \quad (26)$$

Fig. 34. Variation of $C(t)$ during stress hold. Case A, $\sigma_H = 245$ MPa, $t_H = 5$ h, and primary creep is not included in calculation.

Fig. 35. Variation of $C(t)$ during stress hold. Case B, $\sigma_H = 245$ MPa, $t_H = 5$ h, and primary creep is included in calculation.

Fig. 36. Variation of $C(t)$ during stress hold. Case A, $\sigma_H = 98$ MPa, $t_H = 1000$ h, and primary creep not included in calculation.

Fig. 37. Variation of $C(t)$ during stress hold. Case B, $\sigma_H = 98$ MPa, $t_H = 1000$ h, and primary creep included in calculation.

where J represents the elastic-plastic J-integral at the start of the stress hold period, $\dot{\epsilon}_{cN}$ is power-law creep strain rate that serves as the basis for evaluating C^* , and $\dot{\epsilon}_c$ is the actual creep strain rate estimated by a more realistic creep strain equation. The ratio of $\dot{\epsilon}_c$ to $\dot{\epsilon}_{cN}$ calculated at the reference stress σ_{ref} represents the significance of primary creep strain rate compared to steady-state creep strain rate. In the absence of the primary creep strain terms, it simply reduces to unity.

According to the GE/EPRI method, C^* can be estimated in a similar manner:

$$C^* = \kappa a \left(\frac{c}{b} \right) h_1 \left(\frac{a}{b}, m \right) \left(\frac{P}{2ct} \right)^{m+1}, \quad (27)$$

where κ and m are material constants. Dependence of the steady-state creep strain rate $\dot{\epsilon}_{cN}$ on the applied stress σ is represented as:

$$\dot{\epsilon}_{cN} = \kappa \sigma^m. \quad (28)$$

The EPICC equation without the primary creep strain terms was used to determine the values of κ and m for the present test material at 550°C . With $\dot{\epsilon}_{cN}$ and σ measured in hour^{-1} and in megapascals, respectively, the constants were determined to be: $\kappa = 4.52 \times 10^{-32}$ and $m = 10.9$.

Results of values of $C(t)$ obtained from Eq. (26) are given in Figs. 34–37. The value of $C(t)$ estimated by Eq. (26) starts out larger than any of the corresponding path integration values

at the beginning of the transient period but approaches them with time, finally attaining good agreement with the path integration values at the end of the hold times. Although it is expected that a somewhat larger value of $C(t)$ could be predicted in the neighborhood of the crack tip by employing a more refined mesh, Eq. (26) is considered to give a reasonable and conservative representation of the near-tip singularity during the stress hold periods.

Also shown in Figs. 34–37 are the values of $C(t)$ calculated with Eq. (26), where the stress redistribution effect represented by the first term was neglected and only the second term was used. For the cases of no primary creep, $C(t)$ reduces to the steady-state value of C^* . Figures 34 and 36 demonstrate that without primary creep strain terms the value of C^* is considerably below the values of C^* determined by path integration values and with Eq. (26). In contrast, when the primary creep strain terms are included in the analysis, the second term of Eq. (26) yields a fairly good approximation of C^* values obtained by path integration (with the exception of a short time interval directly after the start of the hold period), as can be seen in Figs. 35 and 37. These observations are independent of the applied stress level.

3.3.2 Comparison of Experimental and Analytical Results—CRIEPI

3.3.2.1 Creep J-integral range

In the creep-fatigue tests conducted at CRIEPI, the ΔJ_c values were measured experimentally using displacement increments during the stress hold periods. Therefore comparison of theoretical and experimental results at each moment during the hold times can only be made with respect to ΔJ_c rather than to $C(t)$. Although a rigorous theoretical definition of ΔJ_c has not been made, ΔJ_c can be approximated with the time integral of the $C(t)$ -integral.

Presented in Fig. 38 is a comparison of the ΔJ_c values obtained by time-integration of the $C(t)$ at path 2 with the experimentally determined values of ΔJ_c for three hold times. Figure 38 also shows the values of ΔJ_c obtained by integrating the second term of Eq. (26). It is evident that the agreement between experimental and analytically determined values of ΔJ_c is improved significantly when primary creep strains are included in the analytical evaluation procedure. A fairly good agreement with the experimentally determined values of ΔJ_c may also be obtained by integrating the second term of Eq. (26).

3.3.2.2 Crack-growth history

Crack-growth predictions were made for the three types of creep-fatigue crack-growth tests described in Sect. 2.2.2. Analytical predictions were based on the followings assumptions:

1. Crack-growth rate per cycle is calculated as the sum of fatigue and creep contributions:

$$\frac{da}{dN} = C_f \Delta J_f^{m_f} + C_c \Delta J_c^{m_c} \quad (29)$$

Calculations used the values of $C_f = 3.89 \times 10^{-5}$ and $m_f = 1.6$, which were determined from the fatigue crack-growth tests in Sect. 2.2.1. Calculations also used the values of $C_c = 3.36 \times 10^{-3}$ and $m_c = 1.1$, which were derived from Ref. 4 (in this case, the test results did provide sufficient information for determining these constants).

Fig. 38. Comparison of predicted creep J-integral values with those obtained experimentally.

2. ΔJ_f was estimated by using Eq. (20) in Sect. 3.2.1.
3. ΔJ_c was estimated by the following expression:

$$\Delta J_c = \int_0^H C^* \left[\frac{\dot{\epsilon}_c(\sigma_{ref}, t)}{\dot{\epsilon}_{cN}(\sigma_{ref})} \right] dt, \quad (30)$$

where the C^* was obtained using Eq. (27) and the $\dot{\epsilon}_c$, by using the EPICC equation.

Predicted crack-growth histories are compared with the corresponding test results in Fig. 39. The predictions and the test data agree fairly well.

One might argue about the effectiveness of using the creep strain equation based on standard constant stress creep tests for predicting the creep behavior under creep-fatigue conditions. However, the effect of cyclic loading on the subsequent creep behavior was found to be surprisingly small for type 316 stainless steel.¹² The authors expect that a similar observation would be true for type 304 stainless steel.

Results presented above demonstrate the effectiveness of the present approach. However, experimental investigation of the more realistic loading conditions expected for actual components could be useful.

Fig. 39. Comparison of predicted and experimentally observed crack-growth behaviors.

3.4 FINITE-ELEMENT ANALYSES OF SIMPLE GEOMETRIES—NE

3.4.1 Load-Controlled Transient Conditions

The finite-element program BERSAFE has been used to produce results for $C(t)$ for both CT and SENT geometries. Following initial elastic loading, creep analyses were performed under constant load for the creep law

$$\dot{\epsilon}^c = A\sigma^n, \quad (31)$$

with $n = 5$. Results are presented by normalizing $C(t)$ by the analytical estimate of the steady-state value (denoted C^* and obtained from handbook style solutions) and in terms of a dimensionless time:

$$\tau = \frac{E'C^*t}{K^2}, \quad (32)$$

where $E' = E/(1 - \nu^2)$, with E being Young's modulus and ν being Poisson's ratio. For comparison purposes, results of simplified reference stress estimation formulae have also been calculated. In these

$$C_{\text{ref}}(t) = C_{\text{ref}}^* \frac{(1 + t/t_{\text{ref}})^{n+1}}{(1 + t/t_{\text{ref}})^{n+1} - 1}, \quad (33)$$

where

$$t_{\text{ref}} = \frac{1}{EA\sigma_{\text{ref}}^{n-1}}, \quad (34)$$

and

$$C_{\text{ref}}^* = AK^2\sigma_{\text{ref}}^{n-1}. \quad (35)$$

Note that the reference stress estimate affects both the steady-state estimate and the redistribution time.

Figure 40 presents results for a CT specimen with $a/w = 0.5$ in plane strain. The reference stress approximation conservatively overpredicts the finite-element results for all times. Figures 41–44 present results for the SENT specimen with $a/w = 0.25$ in plane strain for a range of loadings from tension to pure bend. The plane strain reference stress approximation overpredicts the finite-element results at all times for pure bending and gives good predictions for pure tension and combined tension and bending. Any slight nonconservatism in the reference stress estimates for tensile loading of short cracks are unlikely to introduce nonconservatism in practical assessments because of other conservatisms such as the use of data from deeply cracked bend specimens. Also shown in Figs. 40–44 are the dimensionless redistribution times t_{red} corresponding to $t = t_{\text{ref}}$. It can be seen that steady-state creep behavior is essentially reached at these times. Note that the estimate of Ehlers and Riedel¹¹

$$C(\tau) = C^* \left[1 + \frac{1}{(n+1)\tau} \right], \quad (36)$$

Fig. 40. Comparison of reference stress approximation with finite-element results for CT specimen with $a/w = 0.5$ in plane strain under load control.

Fig. 41. Comparison of reference stress approximation with finite-element results for SENT specimen with $a/w = 0.25$ in plane strain under load controlled tension.

Fig. 42. Comparison of reference stress approximation with finite-element results for a SENT specimen with $a/w = 0.25$ in plane strain under load controlled tension and bending ($M/Pa = 0.125$).

Fig. 43. Comparison of reference stress approximation with finite-element results for SENT specimen with $a/w = 0.25$ in plane strain under load controlled tension and bending ($M/Pa = 0.25$).

Fig. 44. Comparison of reference stress approximation with finite-element results for SENT specimen with $a/w = 0.25$ in plane strain under load controlled bending.

which is the sum of the long- and short-time limits of Riedel and Rice,¹³ is close to the more recent estimate of Ainsworth and Budden¹⁴ as given by Eq. (33); alternatively

$$C(\tau) = C^* \frac{(1+\tau)^{n+1}}{(1+\tau)^{n+1} - 1}, \quad (37)$$

which is based on summation of long- and short-time limits of J .

Although Eq. (37) appears to be more complex than Eq. (36), it is more convenient to integrate and results in the approximate estimate

$$\int_0^t C(t)^{n/n+1} dt \cong C^{*n/n+1} t \left[1 + \frac{\sigma_{\text{ref}}}{E \epsilon_{\text{ref}}^c(T)} \right], \quad (38)$$

The integral in Eq. (38) is proportional to the creep strain near the crack tip. Thus Eq. (38) shows that this creep strain is increased above that which would have been accumulated under steady-state creep by the factor of $[1 + \text{elastic strain at the reference stress/creep strain at the reference stress}]$. Because the power of C^* in Eqs. (7) and (8) is approximately $n/(n+1)$, this same factor may be used to incorporate the effects of transient creep on creep crack growth.

For the CT specimen, the parameter C_t developed by Saxena¹⁵ has been calculated from the computed load-line displacement rate and is compared to $C(t)$ in Fig. 45. It can be seen that $C_t \ll C(t)$ for $\tau \ll 1$, similar to the results reported by Leung et al.¹⁶ Indeed Saxena¹⁵ showed that C_t does not have the $1/t$ dependence that $C(t)$ has as $t \rightarrow 0$ [see Eqs. (36) and (37)].

3.4.2 Displacement-Controlled Transient Conditions

The finite-element studies of Sect. 3.4.1 have been extended to examine displacement-controlled situations. Instantaneous values of the applied load at various times into a period of constant displacement have been calculated. The corresponding values of the $C(t)$ parameter have also been obtained. An estimate of the load drop has also been obtained using reference stress methods as given by Eq. (16). A value of followup factor $\mu = 2$ was used for the CT geometry and for the SENT specimen. The value of $C(t)$ is then obtained from the instantaneous value of reference stress as in Eq. (34). The finite-element and reference stress estimates of $C(t)$ are compared in Figs. 46–48. The reference stress estimate overpredicts the finite-element values for each of the geometries and loading cases at all times. It can be noted that, in some cases, this is partly because the reference stress method overestimates the steady-state value of C^* under load control. As a consequence, the load drop is overestimated by the reference stress method. This would tend to lead to nonconservatism in the reference stress approach, but the effect is not sufficient to outweigh the conservatism in the steady load estimate, as demonstrated by Figs. 46–48.

3.4.3 Thermal Stress Conditions

Finite-element analyses under combined mechanical and thermal loading have been performed for a center-cracked plate (CCP) with $a/w = 0.2$ in plane strain and a thick-walled

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Fig. 45. Comparison of variation of C_t by Saxena¹⁵ with that of $C(t)$.

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Fig. 46. Comparison of reference stress approximation with finite-element results for CT specimen with $a/w = 0.5$ under displacement control.

Fig. 47. Comparison of reference stress approximation with finite-element results for CT specimen with $a/w = 0.25$ under displacement-controlled tension.

Fig. 48. Comparison of reference stress approximation with finite-element results for SENT specimen with $a/w = 0.25$ under displacement-controlled bending.

cylinder ($r_o/r_i = 2.88$) with a fully circumferential internal crack with $a/w = 0.069$. As with the analyses for load controlled and displacement controlled geometries, power-law creep with $n = 5$ was used. In both cases the initial elastic loading was a combination of a constant mechanical axial load and thermal stresses due to a quadratic temperature gradient. The magnitude of the thermal stresses was such that

$$K_M + K_T = 2.4 K_M \quad \text{for the CCP,}$$

$$K_M + K_T = 3 K_M \quad \text{for the cylinder,}$$

where the subscripts M and T refer to mechanical and thermal loading, respectively. The finite-element values of $C(t)$ have been normalized by the steady-state values of C^* (as deduced from finite-element analyses for mechanical loading only) and are plotted against normalized time in Figs. 49–52. At short times, it is expected that

$$C(t) \rightarrow \frac{(K_M + K_T)^2}{(n+1)E't} \text{ as } t \rightarrow 0 ; \quad (39)$$

Fig. 49. CCP axial stress and parabolic temperature field. Finite-element values of $C(t)$ normalized by steady-state values of C^* , as deduced from finite-element analyses for mechanical loading only and plotted against normalized time based on combined load.

Fig. 50. CCP axial stress and parabolic temperature field. Finite-element values of $C(t)$ normalized by the steady-state values of C^* , as deduced from finite-element analyses for mechanical loading only, and plotted against normalized time based on mechanical load only.

Fig. 51. Axial stress and radial temperature loading. Finite-element values of $C(t)$ normalized by steady-state values of C^* , as deduced from finite-element analyses for mechanical loading only, and plotted against normalized time based on combined load.

Fig. 52. Axial stress and radial temperature loading. Finite-element values of $C(t)$ normalized by steady-state values of C^* , as deduced from finite-element analyses for mechanical loading only, and plotted against normalized time based on mechanical load only.

therefore, the normalized time of Eq. (32) has been used with K taken as $K_M + K_T$ in Figs. 40 and 42. However, as the thermal stresses relax with time, it is also of interest to examine the data in terms of the normalized time based on mechanical load only as shown in Figs. 50 and 52. Also shown in Figs. 49–52 are the approximation formulae of Eqs. (36) and (37). It is apparent that, for both geometries, the approximation formulae derived for mechanical loading

are accurate for the thermal loading case if the time-scale for normalization is based on the total initial stress-intensity factor. It is expected that behavior in general will be more complex as the time-scale for relaxation of the thermal stresses will depend on the degree of elastic followup. This is to be pursued by performing further finite-element analyses.

4. EXPERIMENTAL RESULTS AND INTERPRETATION

4.1 SUBCRITICAL CRACK-GROWTH EXPLORATORY TESTS

4.1.1 Surface Crack-Growth Tests Under Cyclic Bending Load—CRIEPI

4.1.1.1 Experimental arrangements and results

Two surface crack-growth tests (PB-1 and -2) were performed under cyclic mechanical loading at 550°C. Experimental apparatus and test specimens are shown in Figs. 53 and 54, respectively. Three initial flaws (0.3 mm wide) were introduced on both sides of each specimen by an electric discharge method.

Specimens were subjected to a predominantly bending stress. Bending moment was applied cyclically through the arms welded to both ends of the specimen. Stroke was controlled to keep the surface strain varying between -0.2% and $+0.2\%$ with the rate of $\sim 0.1\%/s$. The site

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Fig. 53. Test apparatus for surface crack-growth tests under mechanical loading.

Fig. 54. Surface crack-growth tests specimen (unit: 1 mm).

of the surface strain measurement is shown in Fig. 54. For specimen PB-1, stroke was held constant at the peak load for 30 min during cycles 5800–7073. For specimen PB-2, hold periods were introduced during cycles 827 to failure. Room-temperature cyclic loading of smaller amplitude was periodically introduced to generate “beachmarks” on the fracture surface.

Status of crack B obtained by surface replication of the specimen PB-1 is shown in Fig. 55. A number of fine branches emanating from the main crack can be observed. Fracture surface of the specimen PB-1 is shown in Figs. 56–58. Experimental results indicated that crack growth was not significantly affected by the stroke hold periods.

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Left Side

Right Side

× 50

× 50

N=4200(End of①)

× 20

× 20

N=9800(End of②)

× 10

× 10

N=14400(End of③)

× 10

× 10

N=18300(End of④)

Fig. 55. Status of crack B as observed on specimen surface.

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CRACK A ($a_0 = 4$ mm, $2c_0 = 32$ mm)

10mm

Fig. 56. Fracture surface around crack A, specimen PB-1.

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CRACK B ($a_0 = 4$ mm, $2c_0 = 16$ mm)

10mm

Fig. 57. Fracture surface around crack B, specimen PB-1.

ORNL PHOTO 5488-89

CRACK C ($a_0 = 4$ mm, $2c_0 = 8$ mm)

Fig. 58. Fracture surface around crack C, specimen PB-1.

4.1.1.2 J-integral evaluation

It is recognized that analytical evaluation of surface cracks is generally more difficult than that of the through-wall cracks. This can be attributed to the limited availability of the fully plastic J-solutions and limit-load solutions for surface crack problems. This class of problems, however, is important in practical applications. Thus development of simplified analytical methods applicable to surface crack problems is desirable. In the present study, three different simplified methods were used to analyze the surface crack-growth tests described above. These analytical techniques are presented here.

I. Utilization of fully plastic solutions. Yagawa and Ueda¹⁷ presented nonlinear J-integral solutions for a plate with the semielliptical crack under tension and bending. These solutions were based on finite-element analyses employing power-law material representation. Using Yagawa-Ueda solutions together with the linear elastic K-solutions proposed by Newman and Raju,¹⁸ J-integral values in the directions of depth J_d and width J_s were estimated:

$$J_d = \frac{K_d^2}{E'} + \kappa \epsilon_0 \sigma_0 t f_1\left(\frac{a}{t}, \frac{a}{c}, n\right) \left(\frac{M}{M_0}\right)^{n+1}, \quad (40a)$$

$$J_s = \frac{K_s^2}{E} + \kappa \epsilon_0 \sigma_0 t f_2\left(\frac{a}{t}, \frac{a}{c}, n\right) \left(\frac{M}{M_0}\right)^{n+1}. \quad (40b)$$

Here K_d and K_s are the stress-intensity factor solutions at maximum depth and on the surface, respectively; $E' = E/(1 - \nu^2)$, t , a , and c are plate thickness, crack depth, and half crack width, respectively. M_0 is the reference moment defined by

$$M_0 = \sigma_0 b t^2; \quad (41)$$

f_1 and f_2 are the nondimensional functions corresponding to the global (average) values of J_d and J_s , respectively. Bending moment calculated for an uncracked plate using the material cyclic stress-strain curve was used in the analysis.

II. Reference stress approach. The reference stress approach yields the following expressions for J_d and J_s :

$$J_d = \frac{E \epsilon_{ref}}{\sigma_{ref}} \left(\frac{K_d^2}{E'} \right), \quad (42a)$$

$$J_s = \frac{E \epsilon_{ref}}{\sigma_{ref}} \left(\frac{K_s^2}{E} \right). \quad (42b)$$

Here σ_{ref} is the reference stress given by

$$\sigma_{ref} = \frac{M}{M_L} \sigma_0; \quad (43)$$

ϵ_{ref} is the corresponding strain on the uniaxial stress-strain curve, and M_L is the collapse moment defined for the perfectly plastic material with the yield stress of σ_0 . Collapse moments derived for a plate with the rectangular crack rather than with the semielliptical one were used in this study:

$$M_L = \frac{\sigma_0 [b^2 t^2 - 2abct + a^2 c(2b - c)]}{2b}, \quad \text{for } \frac{a(2b - c)}{b} \leq t; \quad (44a)$$

$$M_L = \frac{\sigma_0 [b(b - 2c)t^2 + 2abct - a^2 c(2b - c)]}{2(b - c)}, \quad \text{for } \frac{a(2b - c)}{b} > t. \quad (44b)$$

III. Elastic method. Sakon and Kaneko¹⁹ demonstrated that, for test specimens with a through-wall crack, the values of J-integral produced in elastic analysis do not differ significantly from those produced in elastic-plastic analysis. In view of that, only an elastic calculation was used to determine the J-integral in the present study. First, an elastic bending moment (which is considerably higher than the actual bending moment) corresponding to the applied surface strain was calculated. Then the J-integral values were determined as

$$J_d = \frac{K_d^2}{E'}, \quad (45a)$$

$$J_s = \frac{K_s^2}{E}. \quad (45b)$$

Here the stress-intensity factors, K_d and K_s , were used for the elastically calculated bending moment.

4.1.1.3 Evaluation of effective J-integral range, ΔJ_{eff}

Fatigue J-integral ranges in the directions of depth and width, $\Delta J_{f(d)}$ and $\Delta J_{f(s)}$, were obtained by multiplying the corresponding J-integral values by four:

$$\Delta J_{f(d)} = 4J_d, \quad (46a)$$

$$\Delta J_{f(s)} = 4J_s. \quad (46b)$$

At present, knowledge of the closure behavior of the fatigue crack growing under large-scale yielding is rather limited. Therefore parameter q_0 (see Sect. 3.1), determined experimentally under small-scale yielding, was employed to calculate the effective J-integral ranges:

$$\Delta J_{eff(d)} = q_0^2 \Delta J_{f(d)}, \quad (47a)$$

$$\Delta J_{eff(s)} = q_0^2 \Delta J_{f(s)}. \quad (47b)$$

The value of $q_0 = 0.8$ corresponding to $R = K_{min}/K_{max} = -1$ was used.

Evaluation of ΔJ_c . Experiments revealed only a small decrease in load during the stroke hold periods. Thus, to simplify the evaluation of ΔJ_c , load was assumed to remain constant. It is recognized that the fully plastic solutions are presently not available for the power-law exponents greater than five. Therefore the reference stress concept was used to arrive at the following expressions for ΔJ_c in both directions of crack growth:

$$\Delta J_{c(d)} = \frac{K_d^2}{\sigma_{ref}} \int_0^{t_H} \dot{\epsilon}_{ref}^c dt \quad (48a)$$

$$\Delta J_{c(s)} = \frac{K_s^2}{\sigma_{ref}} \int_0^{t_H} \dot{\epsilon}_{ref}^c dt \quad (48b)$$

Crack-growth predictions. Crack-growth laws outlined in Sect. 3.3.2 were used to predict crack-growth behavior. Predictions of crack-growth behavior obtained with methods I–III described above are shown in Figs. 59–62 for three cracks in PB-1 and one crack in PB-2. Also shown in Figs. 59–62 are corresponding experimental results. In general, predictions produced with methods I and II agree fairly well with the test data. Also, the crack-growth rate predicted with method III is greater than that observed in experiments by a factor of approximately 2. Predicted effect of hold times was small, which corresponds to the test results. Considering the scatter in the basic crack-growth data, agreement between predictions and test

Fig. 59. Comparison between experimentally observed and predicted crack-growth behavior for crack A, specimen PB-1.

Fig. 60. Comparison between experimentally observed and predicted crack-growth behavior for crack B, specimen PB-1.

Fig. 61. Comparison between experimentally observed and predicted crack-growth behavior for crack C, specimen PB-1.

Fig. 62. Comparison between experimentally observed and predicted crack-growth behavior for crack A, specimen PB-2.

results appears to be reasonable. However, an investigation of larger creep crack-growth contributions would well complement the present study.

4.1.2 Creep-Fatigue Crack-Growth Tests Under Repeated Thermal Transient Conditions

4.1.2.1 Thermal-shock tests of preflawed thick-walled cylinders—EPRI

Two thermal-shock tests of preflawed thick-walled cylinders were performed. Material was AISI type 304 stainless steel, reference heat 9T2796. Specimen 1 was machined from a 4-in.-diam bar stock, and specimen 2 from a 2-in.-diam bar stock. Before machining, material was annealed at 2000°F for 45 min for specimen 1 and at 1900°F for 30 min for specimen 2. Specimen design is shown in Fig. 63. A single 0.006-in.-wide circumferential flaw was manufactured by the electric discharge method on the outer surface in the gage section of each specimen. Two types of flaw geometries, schematically shown in Fig. 64, were employed.

The testing equipment was constructed by combining a thermal-shock facility, shown in Fig. 65, with the axial load capability. The complete assembly is shown in Fig. 66. Specimens were subjected to a combination of a thermal-shock cycle (see Fig. 67) and sustained axial load. For specimen 1 the axial load was 60,000 lb; for specimen 2, it was 51,800 lb. Both specimens failed during the hold period at 598°C. The number of thermal-shock cycles completed before failure was 57 and 40 for specimens 1 and 2, respectively. Test results are summarized in Table 10, where the wall thickness, flaw type, imposed axial load P , nominal axial stress σ , and number of complete cycles to failure N are given for each specimen. (The flaw was not taken into account while calculating nominal stress σ .)

ORNL-DWG 89-14994

UNIT: 1 in

Fig. 63. Thick-walled cylindrical thermal-shock specimen.

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UNIT: 1 in.

FLAW TYPE NO. 1

FLAW TYPE NO. 2

FLAW LOCATED IN THE MIDDLE OF THE GAGE SECTION

Fig. 64. Two types of circumferential flaws used in thick-walled cylindrical thermal-shock specimens.

Fig. 65. Thermal-shock facility.

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Fig. 66. Thermal-shock testing facility combined with axial load capability.

Fig. 67. EPRI thermal-shock cycle imposed on the outer surface.

Table 10. AISI type 304 stainless steel thermal-shock tests on preflawed cylinders

Specimen No.	Wall thickness (in.)	Flaw type	P (lb)	σ (MPa)	N
1 T	0.75	A	60,000	146	57
2 T	0.75	C	51,800	140	40
1	0.1875	B	40,000	256	1
2	0.1875	B	20,000 (11 cycles)	147	
			25,000 (2 cycles)	184	
			30,000 (2 cycles)	220	15
3	0.1875	B	30,000	194	11
4	0.1875	B	25,000	161	168

Note: P = imposed axial load
 σ = axial stress
 N = number of cycles to failure

Tests presented here combine both thermal and mechanical types of loading. In addition, both creep and fatigue aspects of crack growth must be considered while analyzing experimental results. Thus, in the future, these tests would be well suited for validation of the high-temperature flaw assessment procedure developed within the current program.

4.1.2.2 Thermal-shock tests of preflawed cylinders—CRIEPI

Experimental Arrangements. Cyclic thermal-shock tests on preflawed cylinders were performed to verify high-temperature crack-growth evaluation methods. The testing system is schematically shown in Fig. 68. Cylindrical test specimens were machined from a 6-in. schedule 160 pipe. Outer diameter and wall thickness of the test specimens were 160 and 15 mm, respectively. A thermal-shock cycle was applied to the outer surface of the specimen by moving

Fig. 68. Testing system for thermal-shock tests of preflawed cylinders.

a chamber housing "hot" and "cold" plena first up and then down along the axis of the specimen (240-mm total distance) and holding the chamber in its lowest position for 14.5 min. The thermal-shock cycle is schematically depicted in Fig. 69. The two plena contained liquid metal (similar to solder) at 550 and 150°C.

Before testing, initial flaws were introduced at two circumferential planes of each specimen (see Figs. 70 and 71). The flaws were machined by an electric discharge method and varied in size and shape. Distances between the flaws were considered to be sufficient to preclude any flaw interaction until the crack growth becomes very large.

Shown in Fig. 72 is the crack grown in 1000 thermal cycles from the flaw BO-2 on the outer surface of section B-B'. The crack initiated at the corners of the initial flaw and grew in the direction perpendicular to the pipe axis. The view of the corresponding fracture surface given in Fig. 73 demonstrates that the crack growth in the width direction was larger than that in the depth direction. Similar crack-growth behavior was observed for flaws BO-1 and -3. Crack growth emanating from the rest of the initial flaws was negligible.

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Fig. 69. CRIEPI thermal-shock cycle.

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Fig. 70. Location of flaws in cylindrical specimens (unit: 1 mm).

Fig. 71. Schematic of flaw geometries used in two circumferential planes.

(X10)

- Position Section B-B, Outer Surface
- Notch No. BO-2
- Notch shape $2c=16\text{mm}$, $a=4\text{mm}$, $a/c=0.5$
- No. of Cycles $N=1000$

Fig. 72. Crack-growth from flaw BQ-2 after 1000 thermal cycles.

Fig. 73. Fracture surface of crack growing from flaw BO-2.

4.1.3 Creep-Fatigue Crack-Growth Tests Under Displacement-Controlled Cycling—NE

The creep-fatigue tests on austenitic fillet welded features are complete. Results are presented below in the form of endurance curves and crack-growth data. A creep-fatigue crack-growth analysis is performed on the specimens to predict the numbers of cycles to failure. The approach taken is based on the approximate reference stress methods of analysis described in Sect. 3.1.

Two geometries were tested, each consisting of a flat strip of AISI type 321 stainless steel, a 2-mm-thick fillet, welded with AISI type 347 filler metal to a flat strip of AISI type 321 stainless steel 3.25 mm thick. The geometries, termed cantilever and wishbone arrangements, are shown schematically in Fig. 74. These are designed so that failure occurs in the region of the weld in the cantilever geometry and in the thinner strip in the wishbone geometry. After manufacture, the weld features were given an accelerated aging treatment at 750°C for 200 h.

Creep-fatigue tests were carried out at 650°C in air by fully reversed bending of the long arms of the specimens (Fig. 74) over a fixed displacement range. The displacement was held constant at the maximum ($+\delta$, see Fig. 74) for periods of either 10, 30, or 300 min. During several tests on the cantilever arrangement, displacement was held constant at the minimum ($-\delta$) for 30 min. In addition, three tests with no displacement hold periods were performed. The fatigue cycles were sinusoidal in all cases. Three creep-fatigue tests of the wishbone type specimens were interrupted at intervals, and the extent of crack growth at the surface was optically measured after polishing one surface of the specimens. All the tests were continued beyond the point where the maximum load during the cycle fell to half of its original value.

To provide additional data to check the analytical predictions, load relaxation tests were carried out on both cantilever and wishbone specimens. A cantilever specimen was loaded in

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Fig. 74. Schematic of test specimen geometries.

tension by displacing the moment arm by ~ 3.5 mm. This displacement was held constant, and the relaxed loads were measured at intervals during the 500-h test period. Similarly, a wishbone specimen was loaded in tension by displacing the moment arm by ~ 6 mm. Relaxed loads were measured over a period of 100 h. After the hold period, one fully reversed fatigue cycle was performed, and load relaxation from the new load level was recorded for ~ 1000 h.

4.1.3.1 Metallography

Cantilever arrangement. The majority of cantilever specimens failed by creep-fatigue crack growth along the HAZ close to the weld fusion boundary. The specimens subjected to tensile hold periods initiated intergranular cracks during the hold time at the unfused area, close to the weld root. The specimens subjected to compressive hold periods initiated intergranular cracks during the hold time at the weld toe. Transgranular fatigue cracks, which were observed to propagate from the opposite side to the creep-fatigue cracks, also tended to follow the fusion boundary. The specimen tested under pure fatigue cycling cracked in the weld metal.

Wishbone arrangement. The majority of wishbone tests failed by creep-fatigue crack growth across the thin base metal strip (AISI type 321 stainless steel). During the displacement hold, multiple cracking was observed on the surface held in tension. (As many as ten intergranular regularly spaced cracks were recorded.) Transgranular fatigue cracks propagated on the

side held in compression. The pure fatigue tests failed by transgranular cracking on both sides of the thinner strip. Again, multiple fatigue cracks were observed.

4.1.3.2 Cyclic load-displacement behavior

The applied loads at maximum and minimum displacement were measured and plotted vs the number of cycles in Fig. 75. In the cantilever tests with the tensile hold period [Fig. 75(a)], the maximum load falls off gradually over the duration of testing, and the minimum load remains approximately constant until a late stage in life. The load-displacement hysteresis loops display a marked change in compliance in the compressive quadrant due to crack closure [Fig. 76(a)]. The crack closure effect is enhanced by the abutment of the heel of the displaced strip at the weld root against the relatively stiff short strip (see Fig. 74). The hysteresis loops

Fig. 75. Peak loads vs cycles behavior, experimental results. (a) Cantilever specimen C5 and (b) wishbone specimens W9 and W10.

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Fig. 76. Cyclic load-displacement behavior. (a) Cantilever specimen C5 held in tension, (b) cantilever specimen C8 held in compression, and (c) wishbone specimens W9 held in tension.

obtained for a cantilever specimen subjected to loading with compressive hold periods are shown in Fig. 76(b). Crack closure is observed in the tensile quadrant and abutment closure, in the compressive quadrant. In these tests, the major creep-fatigue crack propagates inward from the weld toe at the outer surface.

In the wishbone tests with tensile hold periods, the maximum and minimum loads remain approximately constant for a large fraction of life before abruptly falling toward the failure point [Fig. 75(b)]. As the load falls rapidly in the wishbone specimens, the shape of the cyclic load-displacement loop suddenly changes due to crack closure [Fig. 76(c)]. Crack closure is not evident until this late stage in the life of the wishbone specimens.

4.1.3.3 Creep-fatigue endurance curves

Results of all tests reported here are summarized in Fig. 77. The number of cycles to failure, N_f^{exp} , is defined as the number of cycles required for the maximum load to fall to one-half of its original value. The equivalent nominal elastic strain range is calculated by assuming a completely elastic applied displacement range. The ASME Code Case N-47²⁰ curve T-1420, with design factors removed to produce a material failure curve, is presented in Fig. 77 as well.

A significant scatter is observed in the endurance data obtained for the tests with displacement hold time. There appeared to be no difference between results produced for cantilever type specimens subjected to tensile and compressive hold periods. The tests with the 300-min tensile hold period yielded results that were at the bottom of the scatter band produced for the tests with the 30-min hold periods.

Fig. 77. Creep-fatigue endurance curves. (a) Cantilever specimens and (b) wishbone specimens.

4.1.3.4 Crack-growth correlations

Three wishbone creep-fatigue tests were interrupted for crack length measurements at intervals of ~80 cycles. The major crack length vs number of cycles data was fitted with a quadratic function, and the rate of crack growth per cycle da/dN was determined at each of the measurement stations. The elastic stress-intensity factor range ΔK was calculated using an expression for SENT specimens. The effective stress-intensity factor range, ΔK_{eff} , was calculated using the approach described in Sect. 4.1.1. Because q_0 could not be measured experimentally until the crack became very deep, the value $q_0 = 0.84$ was taken from the result of Sect. 4.1.1.

The correlation between the crack-growth rates and ΔK_{eff} is shown in Fig. 78 for the two wishbone type specimens subjected to the creep-fatigue loading. Note that the crack-growth rates in Fig. 78 are the total rates. Because the total crack length was measured optically, it was not possible to separate the creep and cyclic components as done in Sect. 4.1.1. However, the 30-min hold period is believed to be insufficient to cause a large increase in the growth rate due to fatigue.

4.2 SUBCRITICAL CRACK-GROWTH MODELING

4.2.1 Finite-Element Analyses of the Isothermal Crack-Growth Tests—CRIEPI

Shown in Sect. 4.1.2, ΔJ_f can be calculated using the finite-element solutions and the material cyclic stress-strain curve for through-wall cracks. To examine the applicability of this

Fig. 78. Creep-fatigue crack-growth rate correlations.

method to surface crack problems, an analytical study of the test described in Sect. 3.1.2 was conducted.

For surface cracks the fully plastic solutions, which have so far appeared in the literature,¹⁷ are generally limited with regard to the geometric parameters and loading conditions. Thus elastic-plastic finite-element analyses were used to estimate ΔJ_f for the surface crack-growth test performed in this study. A finite-element model, consisting of 20-noded isoparametric elements, is shown in Fig. 79. Due to the symmetry of the geometry and loading, one-quarter of the plate was modeled. Three different crack sizes were analyzed; corresponding finite-element meshes are shown in Fig. 80. All three cracks were semielliptical with the same aspect ratio and approximately represented the experimentally observed growth of crack B. Elastic-plastic analysis employed a semicyclic stress-strain curve, representing the relation between half strain range and half stress range in the cyclically saturated condition. The J-integral was calculated with the virtual crack extension method²¹ along the evaluation paths shown in Fig. 81. Because the semicyclic stress-strain relation was employed, the ΔJ_f was calculated by multiplying the J-integral by four.

Distribution of ΔJ_f along the crack front is presented in Fig. 82 for five different load levels as a function of nominal bending strain. The ΔJ_f shows two peaks along the crack front, one at the deepest point of the crack and the other between 10 and 20° measured counter-clockwise from the surface of the plate. Crack growth rate at the 90° station was determined

Fig. 79. Finite-element model of one-quarter of surface crack specimen.

Fig. 80. Finite-element models for three crack sizes.

from the beachmarks on the fracture surface and then plotted vs the ΔJ_I values obtained by a linear interpolation of the calculated results against crack depth (see Fig. 83). Resulting data points fall within the data band produced in the fundamental crack-growth tests for the through-wall cracks. Thus data obtained in the through-wall crack tests can be used to analyze surface crack behavior.

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Fig. 81. Paths for J-integral evaluation by virtual crack extension method.

Fig. 82. Variation of fatigue J-integral range along crack front.

Fig. 83. Crack-growth rate vs fatigue J-integral range.

4.2.2 Finite-Element Analyses of CRIEPI Thermal-Shock Tests on Preflawned Cylinders—CRIEPI

4.2.2.1 Analytical evaluation of test results

Results of CRIEPI experiments outlined in Sect. 4.1.2 were evaluated with finite-element analysis. Finite-element calculations employed an axisymmetric model of an uncracked cylinder and simplified crack-growth histories corresponding to those obtained experimentally. A finite-element model consisting of eight-noded isoparametric elements and imposed boundary conditions are shown in Figs. 84 and 85, respectively. Thermal transient analysis and elastic stress analysis were performed. To simulate the temperature distribution produced during the test, transient heat conduction analysis was carried out with the general-purpose finite-element code MARC. The coefficient of heat transfer between the outer surface of the cylinder and the liquid metal was determined by optimizing agreement between measured and predicted temperature distributions.

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Fig. 84. Finite-element model of uncracked cylinder.

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Fig. 85. Boundary conditions used in finite-element analysis of uncracked cylinder.

Through-wall temperature distributions in sections A-A' and B-B' at four moments of time within the thermal cycle are shown in Fig. 86. The largest through-wall temperature gradient was produced just after the upward movement of the chamber, that is, 15 s after the beginning of the thermal cycle. Axial stress distributions calculated elastically for each temperature profile are shown in Fig. 87. Large bending thermal stresses with some nonlinear contribution were predicted for the time of 15 s (when the highest through-wall temperature gradient occurs). Note that the values of stress obtained on the outer surface exceed the material 0.2% proof stress

Fig. 86. Through-wall temperature distribution in (a) section A-A' and (b) section B-B' at four points within thermal cycle.

for the maximum temperature of the thermal cycle (≈ 150 MPa). Thus, it can be concluded that significant plastic deformation took place in experiments.

4.2.2.2 Determination of stress-intensity factors

For the nonlinear stress distribution shown in Fig. 87, neither the fully plastic Yagawa-Ueda solution nor the Newman-Raju elastic solution applies. Shiratori²² used the weight functions obtained in finite-element analysis and tabulated nondimensional stress-intensity factor solutions for a semielliptical surface crack subjected to the stress distributions of Fig. 88 along the crack surfaces. Based on stress distributions for uncracked structures approximated by

$$\sigma(\xi) = (A\xi^3 + B\xi^2 + C\xi + D)\sigma_0, \quad (49)$$

Fig. 87. Through-wall distribution of elastically calculated axial stress: (a) section A-A' and (b) section B-B'.

the stress-intensity factor K can be estimated as:

$$K = AK_3 + BK_2 + CK_1 + DK_0, \quad (50)$$

where K_0 and K_3 are the stress-intensity factors corresponding to each of the stress distributions shown in Fig. 88.

In the present study the weight function method was used to determine the stress-intensity factor. To accomplish this, stress distributions within the cylinder wall were approximated with the third-order polynomials of the global coordinate in the r -direction. Then parameters A , B , C , and D in Eq. (49) were obtained via the transformation of coordinates.

Fig. 88. Stress distributions along crack front.

4.2.2.3 Crack-growth predictions

Crack growth was predicted using the effective J-integral ranges given by:

$$\Delta J_{\text{eff}(d)} = \left[q_0 \left(\frac{\Delta K_d}{E'} \right) \right]^2, \quad (51a)$$

$$\Delta J_{\text{eff}(s)} = \left[q_0 \left(\frac{\Delta K_s}{E} \right) \right]^2. \quad (51b)$$

(This representation does not account for the effects of plastic deformation.) Crack-growth law, derived from fatigue crack-growth tests on center-cracked panel specimens, was used:

$$\frac{da}{dN} = 9.64 \times 10^{-5} (\Delta J_{\text{eff}})^{1.44}. \quad (52)$$

Because of low stress levels produced during the hold periods, acceleration of crack growth due to creep was not taken into account.

In Fig. 89 crack-growth predictions are compared with the experimental results for the three flaws located on the outer surface, in section B-B'. It is seen that crack growth during the first 500 thermal cycles is overpredicted. This discrepancy may be due to the fact that a certain number of cycles was required for initiating cracks from the initial flaws. Conversely, the rate of crack growth in the depth direction during the last 500 cycles was somewhat underestimated. A detailed investigation of such underprediction is recommended, particularly because a similar method, when applied to tests under purely mechanical loading, yielded conservative results. Environmental and thermomechanical effects may be responsible for the discrepancies. In addition, calculation of stresses on the surface subjected to direct thermal shock may need to be examined further.

Fig. 89. Comparison between predicted crack-growth behavior and that observed in thermal-shock test.

4.2.3 NE Finite-Element Analyses

4.2.3.1 Analysis procedure

For the purposes of analysis, the specimens are idealized as collections of beams built-in where they are welded to the rigid mounting block (Fig. 74). The lateral displacement imposed upon the uncracked beam δ^{uc} is related to the curvature along the beam by standard relationships. The additional displacement due to the presence of a single crack is written in terms of the elastic-plastic rotation due to the crack Θ_{ep}^c :

$$\delta^c = \Theta_{ep}^c l_1 \quad \text{for cantilever specimen ,} \quad (53)$$

$$\delta^c = \Theta_{ep}^c l_2 \quad \text{for cantilever specimen .} \quad (54)$$

Reference stress approximations are used to analyze the plastic response and the creep behavior of the uncracked and the cracked beam. The elastic-plastic curvature for the fatigue cycle is approximated by

$$\kappa_{ep} = \kappa_e \frac{\epsilon_{ep}(\sigma_r^{uc})}{\epsilon_e(\sigma_r^{uc})}, \quad (55)$$

where κ_e is the elastic curvature and σ_r^{uc} is the uncracked body reference stress for a bending beam given by

$$\sigma_r^{uc} = \frac{4M}{w^2}. \quad (56)$$

Here M is the calculated maximum moment per unit width, ϵ_{ep} and ϵ_e are the elastic-plastic and elastic contributions to strain, determined from constitutive relations for AISI type 321 stainless steel and taken from the full-range cyclic stress-strain curve. During the hold period, the creep curvature rate may be computed as

$$\dot{\kappa} = \dot{\kappa}_e + \dot{\kappa}_c = \dot{\kappa}_e + \kappa_e \frac{\dot{\epsilon}_c(\sigma_r^{uc})}{\epsilon_e(\sigma_r^{uc})}, \quad (57)$$

where $\dot{\epsilon}_c(\sigma_r^{uc})$ is the creep strain rate in relaxation for AISI type 321 stainless steel.

The uncracked ligament is assumed to be in plane strain and is modeled as a line spring embedded in the beam. Additional elastic rotation due to the presence of a single crack is given by Ewing²³:

$$\Theta_e^c = 2 \frac{(1-\nu^2)}{E} \alpha_b \left(\frac{a}{w} \right) \frac{6M^*}{w^2}, \quad (58)$$

where $\alpha_b(a/w)$ is a nondimensional compliance integral for bending, and M^* is the moment at the ligament. Elastic-plastic rotation from the reference stress approximation is

$$\Theta_{ep}^c = \Theta_e^c \frac{\epsilon_{ep}(\sigma_r^c)}{\epsilon_e(\sigma_r^c)}, \quad (59)$$

the cracked body reference stress is

$$\sigma_r^c = \frac{4M^*}{m(a/w)(w-a)^2}, \quad (60)$$

and the plane strain, Tresca limit-load function $m(a/w)$ is given by Miller.²⁴ Cyclic stress-strain curve for aged material was used to compute Θ_{ep}^c .

During the hold period, rotation due to one crack is approximated by

$$\dot{\Theta}^c = \dot{\Theta}_e^c + \dot{\Theta}_c^c = \dot{\Theta}_e^c + \Theta_e^c \frac{\dot{\epsilon}_c(\sigma_r^c)}{\epsilon_e(\sigma_r^c)} \quad (61)$$

4.2.3.2 Creep-fatigue crack growth

The amount of crack growth per cycle was calculated as the sum of fatigue and creep (hold period) contributions, in accordance with Eq. (9) of Sect. 3.1. The procedure is outlined in a flow chart in Fig. 90. In the analysis, no allowance was made for creep crack initiation. Fatigue crack growth per cycle was calculated from Eq. (10) of Sect. 3.1. Values of the coefficient C and the exponent l used in Eq. (10) were determined for the material in which the cracking occurred (either the AISI type 347 stainless steel weld metal or the AISI type 321 stainless steel base metal).

Fig. 90. Analysis procedure.

For the symmetrical load cycle considered here, the stress-intensity factor range was determined from twice the calculated maximum bending moment per unit thickness. Effects of plasticity were included by using the full-range cyclic stress-strain curve for the aged AISI type 321 stainless steel in the reference stress J estimates.

The creep crack growth during the hold periods was determined from Eq. (12) of Sect. 3.1. During the fatigue cycle, which is assumed to be symmetrical throughout the lifetime of the specimens, the maximum load P is found by solving the following nonlinear relation for the known displacement δ :

$$\delta = \delta^{uc} + \gamma \delta^c, \quad (62)$$

where γ is the number of assumed cracks along the thinner strip length in the wishbone type specimens ($\gamma = 3$ for the wishbone specimens and $\gamma = 1$ for the cantilever specimens). Sensitivity of the predictions to the value of γ was examined and found not to be unduly strong. The initial crack length was assumed to be equal to one grain diameter ($a_0 = 0.05$ mm) for the wishbone specimens and for the cantilever specimens that were held with the notch root in compression. For the cantilever specimens subjected to tensile hold periods, the initial crack length was taken as the measured notch depth at the weld root. Results obtained for the wishbone specimens appeared to be reasonably insensitive to the assumed initial crack length.

Given the load and the instantaneous crack length, the effective equivalent stress-intensity range was determined, and hence the increment in the cyclic crack-growth contribution was found.

During the dwell period, the displacement was held constant at its maximum value, and the equation governing load drop rate was solved simultaneously with the creep crack-growth law.

The calculations were continued by incrementing the crack length at each step in the numerical solutions, until the predicted maximum load dropped to half of its initial value, consistent with the definition of failure adopted in the experimental procedure.

4.2.3.3 Initial load

As a check on the beam/line spring model, independent of the crack-growth laws, the initial measured and calculated loads were compared. Although the total applied displacement range was controlled accurately, it was not necessarily applied symmetrically because the central zero was difficult to set. Therefore, the means of the measured tensile and compressive loads were used for the comparison with the calculated loads. Figure 91 shows good agreement between the measured and calculated loads. Also note that the wishbone specimens had a significant amount of plasticity in the deforming beam.

4.2.3.4 Load relaxation

Load relaxation as a function of time is shown in Fig. 92 for the two geometries. Calculations were based on the assumption that the displacement was both applied and measured instantaneously. This results in overestimating the initial load. The calculated relaxation rate tends to be overestimated as well.

Fig. 91. Comparison between measured and calculated initial loads.

Fig. 92. Comparison between experimental and predicted load relaxation. (a) Cantilever specimen and (b) wishbone specimen.

4.2.3.5 Analytical results for lifetime and cyclic response

The cycles to failure calculated from the reference stress analysis outlined previously are compared with the experimental data in Fig. 93. Both calculated and experimental results yield a faster fall off in load at the end of life for the wishbone specimens than for the cantilever ones (Fig. 75). A comparison between the predicted and measured semiload ranges obtained for the wishbone specimen W9 is presented in Fig. 94. Results in Fig. 95 demonstrate good agreement between the measured and predicted total crack growth for the wishbone specimen W9. Predicted cyclic and creep crack-growth contributions to the predicted total crack growth are shown in Fig. 95 as well.

4.3 EXPLORATORY FAILURE TESTS—EPRI

4.3.1 Internal Pressurization Tests of Preflawn Pipes

A series of four internal pressurization tests has been performed. Material was AISI type 304 stainless steel, reference heat 9T2796. Before machining, material was annealed for 25 min at 2000°F. Thin-walled pipe specimens were machined to dimensions shown in Fig. 96.

Fig. 93. Comparison between experimental and calculated cycles to failure under various creep-fatigue conditions.

Fig. 94. Predicted vs experimental semiload range for wishbone specimen W9.

Fig. 95. Predicted vs experimental crack length for wishbone specimen W9.

Fig. 96. Thin-walled pressurized pipe specimen.

A small axial flaw was manufactured in the middle of the gage section on the outer surface of each specimen with the electric discharge method. Flaw geometries A and B, schematically shown in Fig. 97(a) and (b), were used. The flaws were replicated. Typical shadowgraphs of the flaw replicas are shown in Fig. 98 (a-d). Originally each specimen was intended to contain a single flaw. However, after specimens 1 and 2 (each containing a flaw of type B) were tested, a decision was made to introduce two additional flaw geometries C and D [see Fig. 97(b)]. A second flaw was manufactured in specimens 3 and 4. As a result specimen 3 contained flaws A and C; and specimen 4, flaws A and D. The flaws were located at the opposite ends of a diameter to preclude any significant flaw interaction. All flaws were 0.006 in. wide.

The testing facility consisted of a clamshell furnace and a pressurization unit, shown in Figs. 99 and 100, respectively. Internal pressurization was accomplished with nitrogen under manual control. The pressurized pipe specimen assembly included a filler plug and a blast shield to minimize the consequences in case of "dynamic" failure. A schematic of the specimen with filler plug and core is presented in Fig. 101. Capacitance strain gages were employed to measure the crack-mouth-opening displacement for each flaw. Preflawned specimens are shown in Fig. 102. A detail of the specimen with the strain gage is given in Fig. 103.

All specimens were subjected to stepwise (see Fig. 104) loading at constant temperature. Specimen 1, tested at room temperature, was loaded up to 16 MPa in a stepwise manner with 30-min hold periods at 1-MPa intervals. No significant plastic deformation was observed. Specimen 1 was unloaded to zero load, reloaded to 18.6 MPa, at which point a 30-min hold period was imposed, and then unloaded to zero load. The specimen did not exhibit any significant inelastic behavior and consequently did not fail.

Fig. 97. Quasi-semicircular flaw: (a) geometry A, (b) geometries B, C, and D.

Specimen 2 was pressurized to 17 MPa at 538°C. The internal pressure of 17 MPa was held constant for 21.2 h, when the failure occurred. The failure was of the leak-before-break type. Considering results obtained for specimens 1 and 2, a decision was made to introduce a larger flaw (geometries C and D) into specimens 3 and 4 to accelerate failure.

Specimen 3, containing flaws A and C, was subjected to stepwise loading to 14 MPa with 1-h hold periods at 7-MPa intervals at 538°C. Specimen 3 failed via leak-before-break at flaw C during the subsequent increase in internal pressure. Pressure at failure was 15 MPa. The same loading history was imposed upon specimen 4 at 593°C. Like No. 3, specimen 4 failed via leak-before-break at flaw D during the increase in internal pressure from 14 MPa. Collapse pressure was 17 MPa.

It is noteworthy that for all specimens subjected to internal pressurization, failure of the leak-before-break type was observed. None failed in a catastrophic manner. A detail of the failed specimen 2 in the vicinity of the flaw is shown in Fig. 105. The failure sites in specimens 1, 3, and 4 are similar in appearance.

Internal pressurization tests are summarized in Table 11, where the number of flaws, flaw geometries, test temperature T , test history, approximate axial σ_z and circumferential σ_θ stresses at maximum internal pressure reached, and pressure at failure P_f are given for each specimen. Axial σ_z and circumferential σ_θ stresses were calculated using the classical plasticity solution for a thin-walled tube subjected to internal pressure.

ORNL PHOTO 5134-91

Fig. 98. Shadowgraphs of axial flaw machined in thin-walled pressurized pipe specimens.

ORNL PHOTO 8159-89

Fig. 99. Furnace used in exploratory tests on thin-walled preflawed pressurized pipes.

ORNL PHOTO 8099-82

Fig. 100. Pressurization unit used in exploratory tests on thin-walled preflawed pressurized pipes.

ORNL-DWG 89-15030

Fig. 101. Schematic of thin-walled pressurized pipe specimen with filler plug and core.

ORNL PHOTO 8158-89

Fig. 102. Thin-walled pressurized pipe specimens prepared for testing.

Fig. 103. Detail of pressurized pipe specimen instrumented with capacitance strain gage at flaw.

ORNL-DWG 91M-3062 ETD

MAXIMUM HOLD TIME = 24 h
MAXIMUM INTERNAL PRESSURE = 19 MPa

Fig. 104. Stepwise internal pressure loading.

ORNL PHOTO 8488-98

Fig. 105. Detail of failed pressurized pipe specimen.

Table 11. AISI type 304 stainless steel internal pressurization tests on preflawed pipes

Specimen No.	Number of flaws	Flaw geometries	T (°C)	Test history	σ_z (MPa)	σ_θ (MPa)	P_f (MPa)	L_r	$L_r^{\max}$
1	1	B	20	Stepwise loading to 16 MPa with 30-min hold periods at 1-MPa interval; unloading to zero load; reloading to 18.6 MPa, 30-min hold period; unload to zero load	132	264			
2	1	B	538	Load to 17 MPa, 21.2-h hold period (until failure)	121	241	17	2.37	2.0
3	2	A,C	538	Stepwise loading to 14 MPa with 1-h hold periods at 7-MPa intervals; load to 15 MPa—failure	106	213	15	2.09	2.0
4	2	A,D	593	Stepwise loading to 14 MPa with 1-h hold periods at 7-MPa intervals, load to 17 MPa—failure	121	241	17	2.68	1.9

Note:

T = test temperature

σ_z = axial stress at maximum pressure

σ_θ = circumferential stress at maximum pressure

P_f = test pressure

$L_r, L_r^{\max}$ = axial stress at maximum pressure

4.3.2 Thermal-Shock Tests of Preflawn Thin-Walled Cylinders

Four thermal-shock tests of preflawn thin-walled cylinders were performed. Material was AISI type 304 stainless steel, reference heat 9T2796. Specimens 1 and 2 were machined from a 4-in.-diam bar stock, specimens 3 and 4 from 2.5-in.-diam tubing. Before machining, material was annealed at 2000°F for 45 min for specimens 1 and 2 and at 1900°F for 30 min for specimens 3 and 4. Specimen design was similar to that of the thick-walled thermal-shock specimens (see Fig. 63); however, in this case, the inside diameter was 1.625 in. A single 0.006-in.-wide circumferential flaw was manufactured on the outer surface in the gage section of each specimen. The testing facility and the thermal-shock cycle are described in Sect. 4.1.2.

The sustained axial loads were 40,000; 30,000; and 25,000 lb for specimens 1, 3, and 4, respectively. All specimens failed during the hold period at 598°C. The number of complete thermal shock cycles to failure was 1, 11, and 168, for specimens 1, 3, and 4, respectively. Note that the result obtained for specimen 4 represents an anomaly. Nothing in the prior history of the material or specimen offers an explanation for such an unusually high number of cycles to failure. Specimen 2 was subjected to 11 thermal-shock cycles with an axial load of 20,000 lb; 2 cycles with an axial load of 25,000 lb; followed by 2 cycles with the load of 30,000 lb. A total of 15 thermal-shock cycles was completed before failure during the hold period at 598°C.

A summary of test results (wall thickness, flaw type, imposed axial load P , approximate axial stress σ , and number of complete cycles to failure N for each specimen) is presented in Table 10.

4.4 FAILURE MODELING

4.4.1 Modeling of Internal Pressurization Tests of Preflawn Pipes

Results of EPRI internal pressurization tests of preflawn pipes have been analyzed using the Central Electricity Generating Board's (CEGB's) R6 procedure.²⁵ As the wall thickness is small, the applied stress intensity factor is low, and assessments have only been performed in terms of the collapse load parameter L_r defined by

$$L_r = \frac{P}{P_L(a, b, \sigma_Y)} \quad (63)$$

where P_L is the collapse pressure for a material with yield stress σ_Y for a defect of dimensions a and b . There are a number of collapse expressions for surface defects in Miller's review.²⁴ For the defects in the current series of tests, these give similar results; in the notation of Fig. 97(b), the collapse pressure is

$$\frac{P_L R}{\sigma_Y t} = \frac{(1 - b/t)}{\left[1 - \frac{b/t}{(1 + 1.05a^2/\pi t)^{1/2}} \right]} = f(a, b) \quad (64)$$

Values of the function $f(a,b)$ are included in the table in Fig. 2(b). The corresponding values of L_r are

$$L_r = \frac{PR/t}{f\sigma_Y} = \sigma_\theta / f\sigma_Y \quad (65)$$

These are given in Table 10 along with values of

$$L_r^{\max} = \sigma_f / \sigma_Y \quad (66)$$

where $\sigma_f = 1/2 (\sigma_u + \sigma_Y)$ is a flow stress. Failure avoidance is ensured in requiring $L_r \leq L_r^{\max}$. As the failure pressures in these tests correspond to $L_r > L_r^{\max}$, the limiting pressure allowed by R6 is lower than the failure pressure. The factor of conservatism ($L_r / L_r^{\max}$) is in the range 1.03–1.38. A similar degree of conservatism has been observed in other analyses of pipe tests as indicated in Table 12 based on Table 3 of R6 validation document.²⁶ However, note that in these tests failure was not solely by plastic collapse but was also influenced by ductile tearing.

4.4.2 Modeling of Thermal-Shock Tests of Thin-Walled Cylinders

Results of EPRI thermal-shock tests of thin-walled cylinders were analyzed using the CEGB R6 procedure.²⁵ The purpose of these analyses was to calculate the cycles-to-rupture.

The R6 procedure requires an equation for the limit load of a partially cracked structure. Herein a semielliptical crack of variable depth was approximated with a segmental crack with a uniform crack depth around the segment. Then the equation given by Miller in Sect. 9.8.1 of Ref. 24 was employed. The segmental angle was taken to be the angle subtended by the major

Table 12. R6 Analyses of pressure vessel tests (see Ref. 25 for further details)

Test	Loading	Material	Defect	Factor of conservatism
CEGB pressure vessel test 1	Internal pressure	A 533 B	Part-thickness axial	1.11
CEGB pressure vessel test 2	Internal pressure and residual stress	Ferritic weld	Part-thickness axial	1.34
HSST vessel 8A	Internal pressure	Ferritic weld	Part-thickness axial	1.17
Stainless steel pipe test 1	Bending	304 SS	Through-thickness circumferential	1.23
Ferritic steel pipe test	Bending	Carbon steel	Through-thickness circumferential	1.25
Stainless steel pipe test 2	Pressure and bending	304 SS and weld	Part-thickness circumferential	1.43
CEGB C-Mn pressure vessel test	Pressure	C-Mn steel	Through-thickness axial crack	1.08

diameter of the ellipse. The uniform depth was taken to be the minor radius of the ellipse. *The ASME Boiler and Pressure Vessel Code*²⁷ (Section XI-Division 1, Article A-3000) was used for stress-intensity evaluation. One-dimensional thermal transient analyses and accompanying thermal elastic stress analyses were performed using the finite-element program PLACRE.²⁸ The stress profile produced for the end of the upshock was used in the present calculations.

Thermal and elastic property data for type 304 stainless steel from the *Nuclear System Materials Handbook* were used. The 0.2% offset yield stress was selected as 90 MPa at 598°C based on the measurements of Sikka et al.²⁹ for reannealed heat 9T27906 of type 304 stainless steel. The data on fracture toughness employed herein were those reported by Mills³⁰ at 538°C (the temperature in the present tests varied from 150 to 598°C). Because no material data on creep crack growth for type 304 stainless steel were readily available, the creep crack growth was not included in the calculation.

In all four cases, present analyses predict fracture due to exceeding the R6 maximum load-carrying capacity (~111 kN for the strength of the material at the maximum cycle temperature of 598°C). Thus specimens are predicted to fracture as soon as they reach 598°C. Specimen 2 (see Sect. 4.3.2 for specimen identification) is predicted to survive the lowest load of 89 kN without significant crack growth, but to fracture as soon as it reaches 598°C after the load is raised to 111 kN. The calculated crack growth is very small and does not contribute to failure in any significant way.

While predictions are no doubt conservative, it should be noted that in these tests creep crack growth can play a considerable role. It would be useful to perform a more detailed analysis including creep crack growth in the future.

5. APPLICATION OF IDENTIFIED CRACK-GROWTH ASSESSMENT METHODS TO A TYPICAL LMR COMPONENT

A main vessel of a pool-type LMR was selected as a typical LMR component for the application of the identified crack-growth assessment methods. The inner diameter and the wall thickness of the vessel were taken as 16 and 40 m, respectively. The operating temperature was taken to be 510°C. Thermal transient analysis and elastic stress analysis of the uncracked vessel under normal startup/shutdown conditions were performed.

Four sections near the top sodium level were chosen for the crack-growth assessment. Initial flaws of 8-mm depth and 40-mm half crack width were assumed to be located at both inner and outer surfaces. Circumferential cracks were assumed in the areas of large axial bending stress, and axial cracks, in those of large hoop membrane stress. Conditions of the analysis are summarized in Table 13.

Method (3) described in Sect. 4.1.1 with the Newman-Raju stress-intensity factor equation for plates was employed to predict crack-growth history at the eight crack locations. Effect of creep crack growth was neglected because of low stress levels during steady-state operation. No consideration was given on the effect of stress ratio; that is, $q_0 = 1$ was always assumed. Because of the limit of the Newman-Raju stress-intensity factor equations, calculation was terminated as soon as the crack depth exceeded 80% of the wall thickness.

Crack growth with cycles is plotted in Figs. 106 and 107 for the depth and width directions, respectively. Predicted crack-growth rates in both directions are small, and the amounts of total crack growth are negligible when only the normal design conditions of <500 cycles are considered. It was estimated that 20,000 to 1,000,000 cycles would be necessary for the crack depth to reach 80% of the wall thickness.

Table 13. Conditions of crack-growth analysis

Notation	Crack orientation	Crack location	$\Delta\sigma_m$ (MPa)	$\Delta\sigma_b$ (MPa)
AI	Circumferential	Inner surface	2.1	135.7
AO	Circumferential	Outer surface		
BI	Circumferential	Inner surface	2.1	139.7
BO	Circumferential	Outer surface		
CI	Axial	Inner surface	134.3	22.9
CO	Axial	Outer surface		
DI	Axial	Inner surface	100.2	21.3
DO	Axial	Outer surface		

Note: $\Delta\sigma_m$ = membrane stress range normal to crack
 $\Delta\sigma_b$ = bending stress range normal to crack

Fig. 106. Predicted change in crack depth with operation cycles.

Fig. 107. Predicted change in half crack width with operation cycles.

It was also predicted that the circumferential cracks subjected to bending-dominant stress state would grow more in the width direction than in the depth direction. The opposite was observed for the axial cracks subjected to membrane-dominant stress state. Figure 108 shows variations in the aspect ratio of the crack during the crack-growth process. Effect of the predominant stress can be clearly seen. At the final stage of analysis, the aspect ratio varies from ≈ 0.2 for the case of the bending-dominant state of stress to ≈ 0.6 for the case of the membrane-dominant state of stress. Such information can be used to determine the postulated leak rate in a safety assessment.

Fig. 108. Predicted change in crack aspect ratio with operation cycles.

6. CONCLUDING REMARKS

During the course of the collaboration, several approaches for modeling creep-fatigue crack growth have been examined. A useful approach is one in which the total crack growth is separated into fatigue and creep components. An effective J-integral range has been found suitable for describing the fatigue crack growth per cycle; simplified methods for estimating ΔJ have been developed and validated by comparison with crack-growth data obtained for surface cracks in plates under bending and surface cracks in cylinders under thermal-shock conditions. The C^* parameter has been found suitable for describing the creep crack growth per cycle; simplified methods for estimating C^* have been developed and validated by comparison with measurements on fracture mechanics test specimens and detailed finite-element analyses for several geometries.

The results of the collaboration have been used to successfully complete a flaw assessment guide. This has been validated by comparing the individual elements of the procedure to experimental and numerical results and by application of the overall approach to creep-fatigue crack growth in thin welded-feature specimens. The procedure tended to predict conservatively the number of cycles to failure. Validation of the final failure load or crack size has been performed by comparison with pressurization tests on thin-walled tubes with surface defects.

Several remaining issues have been identified. These include the effect of prior creep damage on final failure and on crack growth rates, the extent of crack closure in the presence of extensive plasticity, the treatment of combined mechanical and thermal stresses, the treatment of elastic followup, and the extent to which the early part of the transient creep response influences crack growth.

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