Status Report on Assessment of Environmentally Assisted Fatigue for LWR Extended Service Conditions

Nuclear Engineering Division

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by

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ABSTRACT

This report provides an update on an earlier assessment of environmentally assisted fatigue for light water reactor (LWR) materials under extended service conditions. This report is a deliverable in September 2013, under the work package for environmentally assisted fatigue in the Light Water Reactor Sustainability (LWRS) program. The overall objective of this LWRS project is to assess the degradation by environmentally assisted cracking/fatigue of LWR materials, such as various alloy base metals and their welds used in reactor coolant system piping. This effort is to support the U.S. Department of Energy LWRS program for developing tools to predict the aging/failure mechanism and to correspondingly predict the remaining life of LWR components for anticipated 60-80 year operation. The Argonne National Laboratory work package can broadly be divided into the following tasks:

1. Development of mechanistic-based predictive model for life estimation of LWR reactor coolant system piping material (base and weld metals) subjected to stress corrosion cracking and/or corrosion fatigue
2. Performance of environmentally assisted cracking/fatigue experiments to validate and/or complement the activities on mechanistic model development.

The specific accomplishments include

Tensile testing of 316 SS base metal specimens under room and elevated temperature.

Tensile testing of 316 SS-316 SS similar metal weld metal specimens under room and elevated temperature.

Fatigue testing of 316 SS base metal specimens under room temperature.

Mechanistic modeling: Evaluation of extended finite element method for dynamic crack initiation and propagation modeling.

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ABBREVIATIONS

ANL	Argonne National Laboratory
CF	Corrosion Fatigue
CTE	Coefficient of Thermal Expansion
DOE	Department of Energy
EAF	Environmental Assisted Fatigue
FEM	Finite Element Method
LWR	Light Water Reactor
LWRS	Light Water Reactor Sustainability
NPP	Nuclear Power Plant
NRC	Nuclear Regulatory Commission
RT	Room Temperature
SCC	Stress Corrosion Cracking
SS	Stainless Steel
XFEM	Extended Finite Element Method

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1 Introduction

Currently, no mechanism-based modeling framework is available for developing models for environmental fatigue damage that can be used to compute usage factors and remaining lives of components operating in light-water reactor (LWR) environments. Accurate estimation of usage factors and remaining life estimation of safety-critical NPP components will help to schedule their maintenance or replacement so that the risk of catastrophic failure can be minimized. Currently, usage factors and remaining fatigue lives of reactor components are calculated using empirical equations which cannot be extrapolated reliably beyond the range of database from which they were derived. Mechanisms and mechanics-based computer models will help reduce our current dependence on test-based empirical methods and improve our understanding of environmental damage and ageing phenomena.

Under the light water reactor sustainability (LWRS) environmental fatigue program, ANL is trying to develop an advanced usage factor and remaining life estimation model framework that can be used for assessing long term structural integrity of not only light water reactor but also advanced liquid metal and gas-cooled reactor components. However, the proposed mechanistic model will require computer-based modeling, limited testing for generating material properties as well as tests to validate the model. Figure 1.1 shows the schematic of a proposed usage factor and remaining life estimation model framework.

Figure 1.1 Schematic of usage factor and remaining life estimation model framework

Under the LWRS program, the following two major activities are currently in progress at Argonne National Laboratory:

- a) Mechanistic modeling of environmental fatigue damage in LWR reactor coolant system (RCS) piping materials
- b) Environmental tensile and/or fatigue testing of RCS pipe base, similar and dissimilar metal welds.

The tentative mechanistic modeling and experiment plans are as follows:

1.1 Planned mechanistic modeling activities:

Mechanistic models are being developed to assess the ageing behavior of reactor coolant system (RCS) piping comprising of low alloy steel (LAS) and stainless steel (SS), both base metal and weld metals. Since ageing is a time dependent phenomenon, efforts are being made to develop mechanistic models that are formulated as evolutionary. Figures 1.2 and 1.3 show schematics of the parameters that affect environmental ageing in RCS components and the various steps that are involved in the proposed mechanistic modeling framework, respectively.

Figure 1.2 Schematic of parameters that affect environmental fatigue in reactor coolant system components

Figure 1.3 Schematic of mechanistic modeling framework for reactor coolant system component environmental fatigue and usages factor estimation

1.2 Planned testing activities:

Limited laboratory tests will be performed to extract the material properties required for the above mentioned mechanistic model, to understand the ageing behavior and to flesh out different independent variables that affect the ageing mechanism. Tensile and fatigue experiments of hour glass type specimens are planned at room temperature, elevated LWR temperature and under in-air and LWR coolant water chemistry conditions. At present, testing activities are limited to the material grades used in a typical US PWR type reactor coolant system (RCS) piping. For example, A533 B low alloy steel (LAS) and 316 stainless steel (SS) grades, which are often used for reactor pressure vessel (and nozzle) and RCS piping, respectively, are being considered. Both base metal and weldment (316 SS-316 SS similar metal weld and 316 SS-533B LAS dissimilar metal weld) specimens will be tested under tensile and/or fatigue loading. Table 1.1 shows the tentative test matrix.

Table 1. 1316 SS-316 SS similar metal weld fabrication parameters

Test conditions (→)	In-air room temperature		In-air LWR temperature		LWR water & temperature
	Tensile	Fatigue	Tensile	Fatigue	Fatigue
Specimen type (↓)					
316 SS base					
533 B LAS base					
316 SS-316 SS (along the weld)					
316 SS-316 SS (across the weld)					
316 SS-533 B LAS (along the weld)					
316 SS-533 B LAS (across the weld)					

1.3 Organization of this report

The report summarizes some of the work conducted during FY 2012-2013. The accomplishments are described through the following sections.

Section 1: Introduction

Section 2: 316 SS base and 316 SS -316 SS similar weld metal specimen fabrication

Section 3: Room & elevated temperature Fatigue test set-up

Section 4: Room temperature tensile test of 316 SS base metal specimens

Section 5: Room temperature fatigue test of 316 SS base metal specimens

Section 6: Room temperature tensile test of 316 SS -316 SS similar metal weld specimen

Section 7: Elevated temperature tensile test of 316 SS base metal specimens

Section 8: Elevated temperature tensile test of 316 SS -316 SS similar metal weld specimen

Section 9: Estimation of effect of thermal strain and coefficient of thermal expansion in 316 SS base and 316 SS -316 SS similar metal weld material

Section 10: Mechanistic modeling: Evaluation of extended finite element method for dynamic crack initiation and propagation modeling through steam generator tube rupture simulation

2 316 SS-316 SS Similar Metal Weld and Specimen Fabrication

During the initial phase of the work, it was decided to start tensile/fatigue testing of 316 SS base metal and 316 SS-316 SS similar metal weld specimens. The material and geometry information are briefly discussed below.

2.1 316 SS base metal specimen

The 316 SS specimens used in the current work were fabricated from 316 SS plate. As described by the manufacturer, the plate was water quenched and mill annealed at 1900°F. The heat number for the material is P91576 and its chemical composition is given in Table 2.1. Hourglass specimens conforming to ASTM standard E8/E8M [1] and E606 [2] were fabricated for both tensile and fatigue testing of the base metal. The specimens were fabricated along the rolling direction of the 316 SS plate, as shown in Figure 2.1. The dimensions of the specimen are given in Figure 2.2. Figure 2.3 shows a photograph of the as-fabricated specimen.

Table 2. 1 Chemical composition of Type 316 SS base metal (Heat P91576)

Chemical composition (in wt%)									
C	Cr	Cu	Mn	Mo	N	Ni	P	S	Si
0.21	17.37	0.2	1.6	2.12	0.067	10.77	0.018	0.010	0.46

Figure 2.1 Cutting plane with respect to plate rolling direction for hourglass specimen

Figure 2.2 Geometry of 316 SS tensile/fatigue specimen

Figure 2.3 Fabrication drawing of 316 SS tensile/fatigue specimen

2.2 316 SS-316 SS similar metal weld specimen

The 316 SS-316 SS similar metal weld specimens are fabricated from 316 SS-316 SS similar metal welded plates. The weld plates were fabricated using the above-mentioned 316 SS plates and shielded metal arc welding (SMAW) process. The plates were joined with a multi-pass double V-weld using E316-16 grade filler material. Table 2.2 shows the material composition of the filler material. Figure 2.4 shows the schematic of weld sequences and Table 2.3 shows the details of the weld sequence fabrication parameters.

Table 2.2 Chemical composition of E316-16 grade filler metal (Heat 06814)

Chemical composition (wt%)									
C	Cr	Cu	Mn	Mo	N	Ni	P	S	Si
0.05	18.96	-	0.94	2.49	-	11.88	0.028	0.017	0.31

Figure 2.4 Weld sequence 316 SS-316 SS similar metal weld plate

Table 2.3 316 SS-316 SS similar metal weld fabrication parameters

Pass number	Filler metal wire diameter (in)	Current (A)	Voltage (V)	Torch travel speed (in/min)
1	3/32	60-90	21-25	6-7
2	1/8	90-120	24-28	7-8
3	1/8	90-120	24-28	7-8
4	1/8	90-120	24-28	7-8
5-18	5/32	120-160	27-30	7-8

Two type of weld plates were fabricated, weld along the rolling direction and weld perpendicular to the rolling direction of original 316 SS plate. Figure 2.5a shows the two types fabricated weld plates with respect to rolling direction. Figure 2.5b shows the image of weld plate after first weld pass. From each type of weld plate two types of specimens were fabricated, one along the weld fusion zone and the other across the weld fusion zone. Figures 2.6 and 2.7 show schematics of specimen geometry with respect to along and across the weld fusion zone, respectively. The specimens aligned along the fusion zone will be tensile/fatigue tested for generating material properties to be used in the mechanistic model. On the other hand, specimens aligned across the weld zone will be tensile/fatigue tested for validation of coupon level mechanistic model, as mentioned earlier (see Figure 1.1). The geometry of all of the above-mentioned weld specimens are kept the same as the geometry of the base metal specimens shown in Figure 2.2.

Figure 2.5 a) Two types of weld plate with respect to rolling direction, b) Weld shape after first weld pass (the root side is facing up)

Figure 2.6 Schematic of along the weld specimen

Figure 2.7 Schematic of across the weld specimen

2.1 Summary

This section briefly described the specimen geometry material specifications for 316 SS base metal and 316 SS-316 SS similar metal specimens. This section also described the welding processes used for fabricating the 316 SS-316 SS similar metal weld plates from which the weld specimens were fabricated.

3 In-air Room & elevated temperature Fatigue test set-up

At ANL for the requirement of LWRS program two test frames are being configured/updated. One test frame will specifically be used for in-air condition tensile and fatigue testing, while the other one will specifically be used for the LWR water condition testing. The in-air test frame can handle both room and elevated temperature tensile and fatigue testing.

In the present report in-air test results (both under room and elevated temperature conditions) are discussed. Following subsections briefly describes the in-air room and elevated temperature test set up.

3.1 In-air room temperature test setup

A hydraulically controlled MTS test frame was used for the tensile and fatigue tests described in this report. The test set up (frame with the installed specimen) for the room temperature tensile/fatigue tests can be seen in Fig. 3.1. In general, measurements were collected by the following built-in or added-on sensors:

- a) Built-in test frame load cell
- b) Built-in test frame actuator position sensor for actuator position measurement
- c) Added-on displacement (stroke) sensor for crosshead position measurement
- d) Added-on extensometer for strain measurement
- e) Added-on ultrasonic sensor system in-house built for online/real-time fatigue crack monitoring

For the current in-air tests, an extensometer-based strain signal is used as feedback to control the axial strain of the test specimens. However, for environmental testing it may not be feasible to insert the extensometer into the test chamber, and the controller feedback has to be obtained from either the actuator position sensor built into the test frame or the added-on crosshead displacement sensor, which can be mounted outside the environmental chamber. A different test frame with the environmental chamber is being configured at ANL's low cycle fatigue laboratory for future use in the LWRS-related environmental fatigue tests.

Figure 3.1 In-air room temperature tensile/fatigue test frame with specimen and various instruments

3.2 Fatigue testing of 316 SS base metal specimens and resulting data analysis

For elevated temperature tensile/fatigue testing the test setup shown in Fig. 3.1 is augmented with heating element. Induction type heating is used to locally heat the specimen. Figure 3.2 shows the augmented test section for elevated temperature testing. A LEPEL induction heating system was used for the purpose. Specific coil shape (diameter and number of turn) designed to achieve the required temperature in the test specimen. There are total 15 thermocouples spot welded onto pull rod and specimen (9 on the specimen and 6 on the pull rod) to monitor and control the temperature. Figure 3.3a shows the schematic of locations of these thermocouples, whereas Fig. 3.3b shows a magnified view of instrumented specimen.

Figure 3.2 a) Test section with induction heating coil b) LEPEL induction heating system c) Close view of induction heating coil and specimen

Figure 3.3 a) Location of thermocouples on pull rod and specimen b) Close view of the specimen with spot welded thermocouples

4 Room temperature tensile test of 316 SS base metal specimens

4.1 Introduction

Room-temperature tensile tests are needed for 316 SS base metal and weld material to establish the baseline material behavior, such as stress-strain curve, yield stress, and ultimate tensile strength. Based on these baseline behavior, the test parameters for room temperature fatigue and, subsequently, the parameters for environmental fatigue tests will be determined. In addition, these room-temperature material properties can be used for mechanistic modeling through finite element simulation. This section presents the results from two tensile tests on 316 SS base metal specimens tested in air at room temperature. Tests were conducted at two strain rates, 0.0001/s (0.01%/s) and a higher rate of 0.001/s (0.1%/s). Note that, although the room-temperature tensile properties of 316 SS are available in the literature, they may not be representative of the particular heat and material composition of the ANL fatigue specimens.

4.2 316 SS room temperature tensile test results

The above mentioned tensile test results are described below. The test numbers for the are 0.01 and 0.1%/s strain rates are T01 and T02, respectively.

4.2.1 Estimated phase-1 (up to 2%) stress-strain curve

The stress-strain curves estimated using the extensometer and load cell signal are shown in Fig. 4.1. A hardening effect that is dependent on strain rate is evident in this figure. The flow stress curve of the test specimen at the higher strain rate is higher than that at the lower strain rate. The rate dependency of the flow stress that is evident in the tensile tests has to be included in fatigue modeling. Also, these stress-strain curves provide the elastic modulus and yield stress required for finite element analysis. The estimated elastic moduli and 0.2% offset yield stresses for the two tensile tests are given in Fig. 4.1 and Table 4.2. It is to be noted that the tensile tests were conducted in two stages: first up to 2% strain and then from 2% up to final tensile rupture or fracture strain. In the first stage, the tests were conducted using strain control mode and with extensometer output as the control parameter. On the other hand, the second stage of the tests was conducted using displacement control mode with the test frame position sensor output as the control parameter. The tests had to be conducted in two stages because of the maximum measurement limit of the extensometer used. This particular extensometer is a high temperature fatigue rated extensometer, which will be used for all the in-air room and LWR temperature tensile and fatigue tests planned. Note that the 2% strain limit for the extensometer is not a serious constraint for the fatigue tests, which will generally have axial strain amplitudes $\leq 1\%$. Although the extensometer reading was available up to 2% strain, the other sensors (position and stroke) output are extrapolated to obtain stress-strain curve beyond the 2% strain limit. The details are discussed in the following subsections.

Figure 4.1 Strain-versus-stress plot estimated from measurements of extensometer and load cell outputs.

4.2.2 Estimated full (both phase-1 and 2) stress-strain curve

As mentioned earlier, the extensometer used in the test has maximum strain amplitude of 2%. Although this limit is adequate for strain-control fatigue testing, it may not be sufficient for use in finite element modeling, in which the locally accumulated plastic strain may exceed 2%. The higher strain limit for the stress-strain curve can be estimated by using the measured displacements from the added-on crosshead position (stroke) sensor or the actuator position sensor built into the test frame (see Fig. 3.1). The original displacement-versus-stress curve corresponding to the measurements from the crosshead position and actuator position sensors are shown in Figures 4.2 and 4.3, respectively. Comparing these curves, we find that the crosshead displacement sensor has a more limited range. This limitation is due to the use of a ceramic displacement sensor, which has a limited measurement range of 0.635 mm (0.025 in.). Note that both the extensometer and the crosshead displacement sensor will be used for the future in-air tensile/fatigue testing at elevated temperature. However, unlike the extensometer, which cannot be inserted inside an environmental chamber for environmental fatigue testing, the crosshead position sensor is located outside the environmental chamber, and its data will be used along with a calibration curve to control the axial strain in the specimen during the future fatigue testing under LWR water chemistry.

Figure 4.2 Crosshead displacement (stroke) versus stress

Figure 4.3 Actuator position versus stress

Figures 4.2 and 4.3 show the measured stress as a function of displacement. For input to finite element analysis, it is necessary to convert these load-displacement curves to equivalent stress-strain curves. It is also necessary to estimate the equivalent strain from the measured displacements so that approximate strain-controlled fatigue test could be conducted in situations where extensometers cannot be used. This estimation could be performed by mapping known displacement to known strain and then, predicting unknown strains from the known displacements. For simplicity, a mapping function can be established between known displacements with known strain through least squares fitting. Using the estimated parameters of the mapping function, we can estimate the unknown strain from the known or measured displacements. The known strain at a given instant of time ε_t can be expressed as

$$\varepsilon_t = \frac{l_t - l_o}{L_{eff}} = \left(\frac{1}{L_{eff}}\right)l_t + \left(\frac{-l_o}{L_{eff}}\right) \quad (4.1)$$

where l_t is the known or measured displacement at time t , l_o is the initial displacement, L_{eff} is

the effective gauge length, and $\frac{1}{L_{eff}}$ and $\frac{-l_o}{L_{eff}}$ are the unknown parameters that can be estimated through least squares fitting. The crosshead displacement (stroke) and actuator position are plotted with respect to known strain in Figures 4.4 and 4.5, respectively.

Figure 4.4 Crosshead displacement (stroke) with respect to known strain

Figure 4.5 Actuator position with respect to known strain

Using Eq. 2.1 and the known crosshead and actuator position data in Figures 4.2 and 4.3, we estimated the effective length L_{eff} . Note that, for estimating the least squares fit, we only considered the straight portions of the curves in Figures 4.4 and 4.5, i.e., the data beyond the yield strain. The estimated L_{eff} and the physical gage length are given in Table 4.1 for the two strain rates. The corresponding estimated strain-versus-stress curves with respect to crosshead and actuator position measurements are shown in Figures 4.6 and 4.7, respectively. In addition, the scalar material properties estimated from the above-mentioned tensile test data can be found in Table 4.2.

Table 4.1 Estimated effective gage length (or calibration factor) and specimen nominal gage length for 316 SS base metal specimen under room temperature tensile testing

Strain rate	L_{eff} estimate in mm (in.)		Specimen gage length in mm (in.)	
	Based on crosshead displacement (stroke)	Based on actuator displacement (position)	Nominal length	Measured length
0.0001/s	16.507 (0.649)	17.272 (0.68)	14.25 (0.561)	13.233 (0.521)
0.001/s	16.842 (0.663)	17.628 (0.694)	14.25 (0.561)	14.224 (0.560)

Figure 4.6 Strain-versus-stress curves estimated from crosshead displacement measurements

Figure 4.7 Strain-versus-stress curve estimated from actuator position measurements

Table 4.2 Estimated tensile material properties for 316 SS base metal specimen under room temperature tensile testing

Test number	Elastic modulus in GPa (ksi)	0.2% yield		Ultimate		Fracture		Reduction in gauge area (%)
		Stress in MPa (ksi)	Strain in %	Stress in MPa (ksi)	Strain in %	Stress in MPa (ksi)	Strain in %	
T01 (strain rate = 0.0001/s)	178.92 (25950.1)	244.8 (35.5)	0.338	568.88 (82.51)	57.087	369.6 (53.61)	71.88	84.424
T02 (strain rate = 0.001/s)	180.15 (26128.5)	249.43 (36.2)	0.337	574.59 (83.34)	59.498	333.9 (48.43)	71.57	83.149

4.3 Summary

Room-temperature tensile tests of 316 SS base metal have been conducted under two strain rates: 0.0001/s and 0.001/s. Based on these data, material properties and stress-strain curves were estimated. These test results are being or will be used in finite-element-based mechanistic modeling and for selection of test parameters for related fatigue testing in the LWRS program.

5 Room Temperature Fatigue Test of 316 SS Base Metal Specimens

5.1 Introduction

Mechanistic modeling of environmental fatigue requires developing cyclic plasticity modeling. The cyclic plasticity modeling requires cyclic plasticity constitutive relations and related material properties. For the purpose first four room temperature fatigue tests using 316 SS base metal specimens were conducted at the ANL's in-air fatigue test frame. In the present sections, efforts were made to analyze these fatigue test data. Based on these data analysis, different types of cyclic plasticity models can be developed. The data analysis results and a proposed cyclic plasticity model are discussed below.

5.2 Fatigue testing of 316 SS base metal specimens and resulting data analysis

Four 316 SS hourglass specimens were fatigue tested in room-temperature air, one with 0.25% (F04), two with 0.5% (F01 and F02) and one with 0.75% (F03) strain amplitude, using one of the ANL's fatigue test frames. The numbers within brackets denote the test sequence number or specimen numbers. All these tests were performed under strain control cycling with a strain rate of 0.001/s (0.1%/s). Figure 5.1 shows the test frame, specimen (F02 after fatigue tested) and applied strain waveform. Note that, except specimen F01, all the other specimens were cycled until 25% peak load drop from the initial load. In contrast, specimen F01 was cycled until complete rupture. Also, since some discrepancies were observed during F01 specimen testing, the related data are not discussed in this report. The details of the test data obtained from other three specimens fatigue testing are discussed below.

Figure 5.1 a) ANL's in-air fatigue test set-up with capability to test both under room temperature and elevated temperature, b) typical hourglass specimen after fatigue tested, and c) the applied strain wave form used for the mentioned test.

5.2.1 Hysteresis behavior of 316SS base metal

The evolution of the cyclic room-temperature hysteresis loops with time were recorded for the above mentioned tests ($\epsilon_a^t = 0.25\%$, $\epsilon_a^t = 0.5\%$, and $\epsilon_a^t = 0.75\%$). Figures 5.2, 5.4 and 5.6 show the overlapped cyclic stress-strain curves for $\epsilon_a^t = 0.25\%$, $\epsilon_a^t = 0.5\%$, and $\epsilon_a^t = 0.75\%$, respectively. From these figure it can be seen that after some initial hardening, the material softens. Also, the respective magnified hysteresis curves (refer to Figs. 5.3, 5.5, and 5.7) show that there are significant oscillations in the stress (e.g., 60 MPa for $\epsilon_a^t = 0.25\%$ test) possibly due to dynamic strain aging. For accurate cyclic plasticity and hence fatigue life estimation it may be necessary to consider these oscillations in the stress.

Figure 5.2 Overlapped hysteresis plot for $\epsilon_a^t = 0.25\%$

Figure 5.3 Magnified image of Figure 5.2

Figure 5.4 Overlapped hysteresis plot for $\epsilon'_a = 0.5\%$

Figure 5.5 Magnified image of Figure 5.4

Figure 5.6 Overlapped hysteresis plot for $\epsilon'_a = 0.75\%$

Figure 5.7 Magnified image of Figure 5.6

5.2.2 Evolution of cyclic elastic modulus

To model cyclic plasticity, it is also necessary to estimate cyclic elastic modulus and its evolution over time. For the current tests, we have also estimated both the upward and downward elastic modulus for individual cycles. Figures 5.8, 5.9, and 5.10 show the evolution of upward (E_{up}) and downward (E_{down}) moduli for the tests with $\epsilon_a^t = 0.25\%$, $\epsilon_a^t = 0.5\%$, and $\epsilon_a^t = 0.75\%$, respectively. From Fig. 5.8 it can be seen that for the test with $\epsilon_a^t = 0.25\%$, except during the end of the test, both elastic moduli vary between 180 to 190 GPa, with maximum variation within the range of 5-6%. It is to be noted that the monotonic elastic modulus for 316 SS estimated under similar conditions (e.g., at room temperature and with a strain rate of 0.1%/Sec.) was 180.15 GPa (see Table 4.2). Similarly, from Fig. 5.9 it can be seen that for the test with $\epsilon_a^t = 0.5\%$, the elastic moduli vary in the range of 165-185MPa (10-12%). However, for $\epsilon_a^t = 0.75\%$, (see Fig. 5.10) the elastic moduli varied in the range of 160-220 MPa, which is more than 25-35%.

Figure 5.8 Estimated upward and downward elastic modulus for $\varepsilon_a^t = 0.25\%$

Figure 5.9 Estimated upward and downward elastic modulus for $\varepsilon_a^t = 0.5\%$

Figure 5.10 Estimated upward and downward elastic modulus for $\varepsilon_a^t = 0.75\%$

5.2.3 Evolution of cyclic maximum ($\sigma_n^{\max}$) and minimum ($\sigma_n^{\min}$) peak stress

Different materials can harden/soften differently depending on the applied stress/strain. Knowing the hardening and softening behavior of the material will aid in the development of suitable constitutive relations for cyclic plasticity model. The evolution of hardening/softening behavior can be observed from the peak cyclic stress versus time or the peak cyclic stress versus number of cycles curves. Figure 5.11 shows the evolution of peak maximum and minimum stresses for the tests with $\varepsilon_a^t = 0.25\%$, $\varepsilon_a^t = 0.5\%$, and $\varepsilon_a^t = 0.75\%$, respectively. From the figure it can be observed that, in all cases, the material initially hardens and then softens. Also, it can be seen that the magnitude of hardening/softening increases as the applied strain amplitude or strain range increases. It can be observed that for the test with $\varepsilon_a^t = 0.75\%$, there is an abrupt drop in the peak stress during the 10th and 11th cycle, possibly indicating buckling of the specimen or slippage of the extensometer.

Figure 5.11 Evolution of maximum and minimum peak stress with respect to number of fatigue cycles for test cases $\varepsilon_a^t = 0.25\%$, $\varepsilon_a^t = 0.5\%$, and $\varepsilon_a^t = 0.75\%$

5.2.4 Evolution of cyclic elastic strain range ($\Delta\varepsilon_n^e$) and plastic strain range ($\Delta\varepsilon_n^p$)

The variations of the elastic strain range ($\Delta\varepsilon_n^e$) and plastic strain range ($\Delta\varepsilon_n^p$) with number of fatigue cycle ($n=1, \dots, N$) are needed for development of the cyclic plasticity model. These trends can be linked to the cycle-dependent hardening and softening behavior of material, and will enable to develop a suitable constitutive relation that can be incorporated in to the mechanics-based evolutionary cyclic plasticity model. Figures 5.12 and 5.13 show the variations of the elastic and plastic strain ranges with cycles for the test with $\varepsilon_a^t = 0.25\%$, respectively. Figures 5.14 and 5.15 show the corresponding evolutions for the test with $\varepsilon_a^t = 0.5\%$ and Figs. 5.16 and 5.17 show the evolutions for the test with $\varepsilon_a^t = 0.75\%$, respectively. From these figures it can be observed that, except for the test with $\varepsilon_a^t = 0.75\%$ (Figs. 5.16 and 5.17), similar trends in elastic and plastic strain range evolution are observed for tests with $\varepsilon_a^t = 0.25\%$ and $\varepsilon_a^t = 0.5\%$. For example, Fig. 5.12 shows that the elastic strain range ($\Delta\varepsilon_n^e$) initially increases and then decreases, which indicates initial hardening followed by softening. Figure 5.13 shows that the plastic strain range ($\Delta\varepsilon_n^p$) initially decreases and then increases, which also indicates initial hardening and then softening. The continuous hardening with cycle in the case of

$\varepsilon_a^t = 0.75\%$ is possibly due to the large applied strain amplitude indicating a trend towards stress saturation and a stable hysteresis loop. However, the possibility of buckling makes this test questionable.

Figure 5.12 Evolution of elastic strain range for test case $\varepsilon_a^t = 0.25\%$

Figure 5.13 Evolution of plastic strain range for test case $\varepsilon_a^t = 0.25\%$

Figure 5.14 Evolution of elastic strain range for test case $\varepsilon_a^t = 0.5\%$

Figure 5.15 Evolution of plastic strain range for test case $\varepsilon_a^t = 0.5\%$

Figure 5.16 Evolution of elastic strain range for test case $\varepsilon_a^t = 0.75\%$

Figure 5.17 Evolution of plastic strain range for test case $\varepsilon_a^t = 0.75\%$

5.2.5 Evolution of cyclic back stress (α_t)

When there is a stress reversal, the hysteresis loop moves up or down from its original position depending on the material and applied stress/strain. This is due to strain hardening/softening and associated Bauschinger effect. This shift in stress space can be represented by a back stress (α_t). The estimation of evolution of back stress with respect to time is necessary for modeling cyclic plasticity. The evolution of back stress α_t at any given instant of time can be expressed as:

$$\alpha_t = \alpha_n + \alpha_\varepsilon \quad (5.1)$$

Where, α_n is the mean shift of hysteresis loop in stress space and can be expressed as

$$\alpha_n = \frac{1}{2}(\sigma_{n;\varepsilon=\varepsilon_{\max}}^t + \sigma_{n;\varepsilon=\varepsilon_{\min}}^c) \quad (5.2)$$

and α_ε is the back stress at strain ε within an individual hysteresis loop and can be expressed as

$$\alpha_\varepsilon = \frac{1}{2}(\sigma_{n;\varepsilon}^{ten} + \sigma_{n;\varepsilon}^{com}) \quad (5.3)$$

In Eq. 5.2 $\sigma_{n;\varepsilon=\varepsilon_{\max}}^{ten}$ and $\sigma_{n;\varepsilon=\varepsilon_{\min}}^{com}$ denote the n^{th} fatigue cycle maximum tensile stress at $\varepsilon = \varepsilon_{\max}$ and maximum compressive stress at $\varepsilon = \varepsilon_{\min}$, respectively. For the tests with $\varepsilon_a^t = 0.25\%$, $\varepsilon_a^t = 0.5\%$, and $\varepsilon_a^t = 0.75\%$, the mean back stresses α_n were estimated and their evolutions are plotted in Figs 5.18, 5.19, and 5.20, respectively.

Figure 5.18 Evolution of mean back stress for individual cycle (n) for test case $\varepsilon_a^t = 0.25\%$

Figure 5.19 Evolution of mean back stress for individual cycle (n) for test case $\varepsilon_a^t = 0.5\%$

Figure 5.20 Evolution of mean back stress for individual cycle (n) for test case $\varepsilon_a^t = 0.75\%$

5.2.6 Evolution of damage state (d_t)

It may be necessary to associate the evolutionary stress-strain properties to a physical damage quantity say d_t . If so, in the constitutive model for cyclic plasticity, the stress-strain relation properties can be input as a function of this time independent variable rather than explicitly expressing the material properties with respect to time. Though in a stress controlled fatigue test it may be easier to express d_t as function of accumulated plastic strain, it may not be straight forward in the case of strain controlled fatigue tests. For example in strain controlled fatigue tests the damage state at any given instant of time d_t can be expressed in terms of accumulated plastic path length and is as given below:

$$d_t = P_p = \int d\varepsilon^p = 2 \sum_{n=1}^N \Delta \varepsilon_n^p \quad (5.4)$$

Figure 5.21 shows the corresponding estimated damage states for test cases $\varepsilon_a^t = 0.25\%$, $\varepsilon_a^t = 0.5\%$, and $\varepsilon_a^t = 0.75\%$, respectively. However, from the figure it can be seen that the estimated damage states are not truly cycle independent rather depends on the number of fatigue

cycles the specimen experienced. This drawback may necessitate the need of performing few stress controlled fatigue tests at least for developing evolutionary plasticity and hence the evolutionary fatigue model.

Figure 5.21 Evolution of plastic path travel based damage states (d_t) for test cases $\varepsilon_a^t = 0.25\%$, $\varepsilon_a^t = 0.5\%$, and $\varepsilon_a^t = 0.75\%$

5.3 Constitutive model for cyclic plasticity

The constitutive relation for the cyclic plasticity model can be developed based on the above discussed test results. The constitutive relation can be of two types such as evolutionary cyclic plasticity model and stabilized or half-life based cyclic plasticity model. Both these models are discussed briefly below.

5.3.1 Detailed evolutionary cyclic plasticity model

The evolutionary plasticity model captures the evolution of material parameters over the entire fatigue life. To apply this model, the material parameters such as elastic modulus, yield stress, elastic strain range, plastic strain range, back stress, hardening constants, etc. have to be described as functions of time independent parameter(s) describing the physical damage state of the structure. For example, the stress state at time $t + \Delta t$ can be expressed as

$$\begin{aligned}\sigma_{t+\Delta t} &= \sigma_t + \Delta\sigma \\ \Delta\sigma &= C : \Delta\varepsilon^{el} = C : (\Delta\varepsilon^{to} - \Delta\varepsilon^{pl})\end{aligned}\tag{5.5}$$

Where C is the elasticity matrix, σ_t is the stress up to time t and can be expressed in terms of stress up to n^{th} fatigue cycle using the following relation;

$$\sigma_t = \sigma_n + \sigma_{\bar{t}} \quad (5.6)$$

Where the time equivalent of n^{th} fatigue cycle is equal to nT where T is the time period of an individual fatigue cycle and $\bar{t}$ is elapsed time in an individual cycle. The relationship between t and $t + \Delta t$ is schematically shown in Figure 5.22.

Figure 5.22 Schematic showing relation between t and $t + \Delta t$ with respect to applied strain cycle

In Eq. 5.5, $\sigma_{t+\Delta t}$ is the true stress estimated from a two-step process, first by estimating a trial stress $^{trial}\sigma_{t+\Delta t}$ in solving Eq. 5.4 with the assumption of $\Delta \epsilon^{pl} = 0$ (elastic predictor step) and then correcting the trial stress by satisfying the von-Mises yield criteria (plastic corrector step) given as

$$f = ({}^{trial}\sigma'_{t+\Delta t} - \alpha_{t+\Delta t}) : ({}^{trial}\sigma'_{t+\Delta t} - \alpha_{t+\Delta t}) - \sigma_{t+\Delta t}^y = 0 \quad (5.7)$$

Where ${}^{trial}\sigma'_{t+\Delta t}$ is the trial deviatoric stress tensor, $\alpha_{t+\Delta t}$ is the back stress tensor at time $t + \Delta t$ and can be expressed as

$$\alpha_{t+\Delta t} = \alpha_n + \alpha_{\bar{t}+\Delta t} \quad (5.8)$$

Where, α_n is the evolutionary contribution expressed as a function of cycles and $\alpha_{\bar{t}+\Delta t}$ is the contribution within a particular cycle expressed as a function of time. The evolutionary contribution corrects the $\alpha_{\bar{t}+\Delta t}$ by considering the evolutionary effect of stress hardening and softening. Similarly in Eq.5.7, the yield stress $\sigma_{t+\Delta t}^y$ can also be expressed in terms of a correction part (σ_n^y) which is a function of cycles and a yield stress ($\sigma_{\bar{t}+\Delta t}^y$) within an individual cycle which is a function of time as follows:

$$\sigma_{t+\Delta t}^y = \sigma_n^y + \sigma_{\bar{t}+\Delta t}^y \quad (5.9)$$

The cycle-dependent correction α_n for back stress and σ_n^y for yield stress have to be related to a time or cycle independent physical parameters (e.g., accumulated plastic strain in a stress controlled test).

5.3.2 Stabilized or half-life based approximate cyclic plasticity model

Typically estimating the evolutionary correction terms described through Eq. 5.8 and 5.9 are complex to model with other independent variables unless these independent variables are not linked to a time or cycle independent physical parameters (e.g accumulated plastic strain in a stress controlled test). Hence for simplicity in many available cyclic plasticity models the stabilized or half-cycle stress-strain behavior is assumed constant over the entire fatigue life. This subsection describes the estimation of some of the stabilized cycle material properties that can be used in a simplified cyclic plasticity model.

For example, to estimate the hardening and yield parameter only the half-life hysteresis curves of all the three tests with $\epsilon_a^t = 0.25\%$, $\epsilon_a^t = 0.5\%$, and $\epsilon_a^t = 0.75\%$ are considered. An equivalent monotonic stress-strain curve is estimated by connecting the peak maximum stress point of the individual plastic strain versus stress hysteresis curves. Figure 5.23 shows the half-life hysteresis curve of the individual test cases and the associated tensile half of the cyclic stress-strain curve. In a similar fashion, the compressive half of the cyclic stress-strain curve can be estimated by connecting the peak minimum stress point of the individual plastic strain versus stress hysteresis curves. From Fig. 5.23 it can be seen that both the tensile and compressive halves of the cyclic stress-strain curve follow a linear pattern and the corresponding hardening parameter are estimated by directly estimating the slopes of these linear stress-strain curves and the yield stress as the corresponding y-intercepts. Because of lack of data, the usual 0.2% offset strain is not determined in this case. A stabilized or half-life cyclic stress-strain curve can be constructed by performing a number of strain-controlled fatigue tests at different strain amplitudes. The same information can be obtained by performing a single strain controlled fatigue tests with a sequence of different strain amplitudes, which are repeated at certain regular intervals. The hardening parameters can be estimated from the estimated cyclic stress-strain curve. For example, the tensile hardening constant is estimated as 24.426 GPa and the corresponding yield

stress as 194 MPa. The respective compressive hardening constant and yield stress are found to be 24.183 GPa and 194 MPa, respectively. It is noted that the monotonic yield stress estimated from monotonic tensile tests under similar test conditions (e.g room temperature, strain rate = 0.1%/Sec.) was 249.43 MPa (see Table 4.2).

Figure 5.23 Overlapping half-life hysteresis curves for test cases $\varepsilon_a^t = 0.25\%$, $\varepsilon_a^t = 0.5\%$, and $\varepsilon_a^t = 0.75\%$ and associated monotonic stress-strain curve

5.4 Summary

The room temperature fatigue test data of 316SS base metal specimen are analyzed to derive the hardening and softening behavior of 316SS base metal under in-air and room temperature conditions. This analysis will help develop a suitable constitutive relation for a mechanistically based cyclic plasticity or fatigue model. In addition, the information obtained through this test data will help us plan the next series of tests, such as, elevated temperature and water environment fatigue testing of base and weld specimens. Both evolutionary plasticity model and half-life hysteresis loop based approximate cyclic plasticity models are discussed. Based on half-life cycle hysteresis curves, a cyclic stress-strain curve was estimated.

6 Room temperature tensile test of 316 SS -316 SS similar metal weld specimen

6.1 Introduction

One room temperature tensile test was conducted for 316 SS-316 SS similar weld specimen. The specimen was cut from the fusion zone of the 316 SS-316 SS similar metal weld plate discussed in Section 2. The schematic of the specimen with respect to weld fusion zone can be seen from Figure 2.6. The geometry of the specimen maintain similar as the base metal specimen geometry as shown in Figure 2.2. The particular specimen was cut from the welded plates in which the weld direction is parallel to the rolling direction of the 316 SS base metal. The tensile test was conducted at a strain rate of 0.001 /S. The details of the results are described in the following subsections.

6.2 Estimated stress-strain curve and associated tensile test material properties

Similar procedure described in section 4 (for room temperature base metal tensile tests) was followed to estimate the required stress-strain curve and tensile material properties. As before a two stage tensile tests were followed due to the limitations of the extensometer output. The first stage was carried out up to 2 % strain with extensometer output as control parameter and the second stage was carried out from 2% strain to complete tensile rupture with frame position sensor output as control parameter.

6.2.1 Stage-1 (up to 2%) stress-strain curve of 316 SS -316 SS weld specimen

The stage-1 (up to 2 % strain) stress-strain curves estimated using the extensometer and load cell measurements are shown in Figure 6.1. The figure also shows the estimated elastic modulus

Figure 6.1 Up to 2 % strain room temperature strain-versus-stress curve for 316 SS-316 SS weld specimen.

and yield stress. Comparing the room temperature elastic modulus of pure 316 base (refer test T02 of Table 4.2) and weld metals it can be seen that weld metal has much lesser elastic modulus (138.02 GPa) compared to the weld metal elastic modulus (180.15 GPa). However, in contrary it is found that the yield stress of weld metal (430 MPa) is much higher compared to the yield stress of base metal (249.43 MPa).

6.2.2 Full stress-strain curve of 316 SS -316 SS weld specimen and associated tensile test material properties

Similar as the procedure described section 4.2 the room temperature full stress-strain curve of the 316 SS-316 SS weld specimen was estimated using both the stage-1 and stage-2 sensor (extensometer, stroke, actuator position, and load cell) measurements. For example Figure 6.2 and 6.3 show stroke versus stress and actuator position versus stress curves for both the stage-1

Figure 6.2 Crosshead displacement (stroke) versus stress for 316 SS-316 SS weld specimen under room temperature tensile testing

and stage-2 tensile tests. Figures 6.4 and 6.5 show the stage-1 stroke versus strain and actuator position versus stress curves, respectively. It is to be noted that for stage 2 (i.e., beyond 2 %) strain the extensometer reading was not available and hence mapped from the stroke or position sensor data as described in Section 4.2. Figures 6.6 and 6.7 show the corresponding mapped stress-strain curve using stroke and position sensor measurements, respectively. Figure 6.7 shows the full stress-strain curve whereas the Fig. 6.5 show only up to the 3.5 % strain, which is the equivalent maximum limit of 3.5 % strain. From the Figs. 6.6 and 6.7 it can be seen that the weld metal harden faster compared to the base metal and with reaching fracture strength much before the base metal fracture strength. However, it can be seen that the ultimate stress of weld metal (596.4 MPa) is slightly larger compared to the ultimate stress of base metal, which is 574.59 MPa (refer Table 4.2 for test T02). The corresponding effective length used for

converting the stroke and position measurements to equivalent strain can be found from Table 6.1. Also all the estimated room temperature tensile properties of 316 SS-316 SS weld specimen are summarized in Table 6.2.

Figure 6.3 Actuator positions versus stress for 316 SS-316 SS weld specimen under room temperature **tensile testing**

Figure 6.4 Crosshead displacement (stroke) with respect to stage-1 (known extensometer measurements) strain for 316 SS-316 SS weld specimen under room temperature tensile testing

Figure 6.5 Actuator positions with respect to stage-1 (known extensometer measurements) for 316 SS-316 SS weld specimen under room temperature tensile testing

Figure 6.6 Combined stage-1 and 2 stress-strain curves estimated using crosshead displacement (stroke) measurements for 316 SS base metal and 316 SS-316 SS weld specimens under room temperature tensile testing

Figure 6.7 Combined stage-1 and 2 (full) stress-strain curves estimated using actuator displacement (position) measurements for 316 SS base metal and 316 SS-316 SS weld specimens under room temperature tensile testing

Table 6.1 Estimated effective gage length (or calibration factor) and specimen nominal gage length for 316 SS-316 SS weld specimen under room temperature tensile testing

Test number (Strain rate)	L_{eff} estimate in mm (in.)		Specimen gage length in mm (in.)	
	Based on crosshead displacement (stroke)	Based on actuator displacement (position)	Nominal length	Measured length
T03 (0.001/s)	14.977 (0.589)	15.488 (0.609)	14.25 (0.561)	14.072 (0.554)

Table 6.2 Estimated tensile material properties for 316 SS-316 SS weld specimen under room temperature tensile testing

Test number (Strain rate)	Elastic modulus in GPa (ksi)	0.2% yield		Ultimate		Fracture		Reduction in gauge area (%)
		Stress in MPa (ksi)	Strain in %	Stress in MPa (ksi)	Strain in %	Stress in MPa (ksi)	Strain in %	
T03 (strain rate = 0.001/s)	138.02 (20018.1)	430 (62.36)	0.513	596.4 (86.50)	36.305	476.2 (69.07)	55.94	62.207

6.3 Summary

One room-temperature tensile test of 316 SS-316 SS weld metal was conducted under strain rates 0.001/s. Based on the test data, tensile material properties and stress-strain curves were estimated. The estimated results show that the weld metal has higher yield strength and lower elastic modulus compared to the corresponding base metal. The results also show that the weld metal fracture well before the base metal although both have the similar ultimate strength.

7 Elevated temperature tensile test of 316 SS base metal specimens

7.1 Introduction

In addition to room temperature tensile test of 316 SS base metal specimens an elevated temperature tensile test was conducted for a 316 SS base metal specimen. The specimen geometry kept same as the base metal specimen as described in Section 2.2. The tensile test was conducted at a strain rate of 0.001/s while trying to maintain the specimen gage area temperature approximately equal to the typical LWR coolant temperature of 300°C. The test frame discussed in Section 3, is used for the elevated temperature tensile testing. The test results are summarized below.

7.2 Pretest heating up the specimen

First before stating the tensile test the specimen was heated up using the induction heating system described in Section 3. The temperature at different locations of the specimen and pull rod are measured using 15 thermocouples (see Fig. 7.1). The pretest heating up the specimen was continued up to duration at which the temperature in the gage area reaches the approximate required temperature of 300°C. It is found that this duration is around 4 to 4.5 hour, by which the temperature in the gage and other location stabilizes. Figure 7.1 shows the pretest temperature history from various thermocouples. Figure 7.2 shows the corresponding spatial temperature distribution at various location of test specimen and pull rod before the starting of the tensile test.

Figure 7.1 a) Locations of thermocouples on specimen and pull rod b) Temperature history at different locations of the specimen and pull rod before the starting of tensile test for 316 SS base metal specimen

Figure 7.2 Temperature distributions at different locations of the specimen and pull rod before the starting of tensile test for 316 SS base metal specimen

7.3 Summary of tensile test results and estimated stress-strain curve

Except the heating up the specimen, an ssimilar procedure described in section 4 (for room temperature base metal tensile tests) was followed to conduct the mentioned tensile test and to estimate the required stress-strain curve and tensile material properties from the test data. As before mentioned due to the limitation in extensometer measurement range a two phase tensile test procedure was followed with first phase was carried out up to 2 % strain with extensometer output as control parameter and the second phase was carried out from 2% strain to complete tensile rupture with frame position sensor output as control parameter.

7.3.1 Estimated phase-1 (up to 2%) stress-strain curve

The estimated stress-strain curve during the first phase of the tensile test is shown in Fig. 7.3. The figure also shows the estimated elastic modulus of 157.21 GPa and yield stress of 156.07 MPa, respectively. Comparing the room temperature test results of base metal tensile test (refer Table 4.2 test TO2) it can be found that the elevated temperature elastic modulus and yield stress are much lesser compared to the corresponding room temperature elastic modulus (of 180.15 GPa) and yield stress (of 249.43 MPa), respectively. Also from the temperature history data it is found that the temperature in the gage area relatively remains constant over the phase-1 tensile test durations. Figure 7.4 shows the comparison of temperature profile at different locations of specimen at start and end of the phase-1 tensile test.

Figure 7.3 Up to 2 % strain elevated temperature (300 oC) strain-versus-stress curve for 316 SS base metal specimen

Figure 7.4 Temperature profile at start and end of the phase-1 tensile test

7.3.2 *Estimated full (both phase-1 and 2) stress-strain curve*

Similar as the procedure described Section 4.2 the full stress-strain curve of the 316 SS base metal specimen under elevated temperature was estimated using both the phase-1 and phase-2 sensor (extensometer, stroke, actuator position, and load cell) measurements. For example Figures 7.5 and 7.6 show stroke versus stress and actuator position versus stress curves estimated from both the phase-1 and 2 tensile test data, respectively. Whereas Figs. 7.7 and 7.8 show the phase-1 stroke and actuator displacement with respect to stress generated in the gage area. Using the above-mentioned data and the procedure described in section 4.2 the combined phase-1 and 2 stress-strain curves are estimated. Figures 7.9 and 7.10 show the corresponding estimated stress-strain curves with respect to stroke and actuator position measurements, respectively. Also the Figures 7.9 and 7.10 show the comparison of stress-strain curve with respect to the corresponding room temperature stress-strain curve (refer section 4 for details). From these figures it can be found the elevated temperature curve harden slower compared to the room temperature tensile curves. Also it can be found that the ultimate stress and fracture strain of elevated temperature case are much lesser compared to the corresponding room temperature case. However, care should be taken while using the above mentioned full stress-strain curve particularly using the stress-strain data during the end of the test. This is because as the strain (hence the pull rod displacement) increases, the specimen gage area slowly goes out of the induction heater coil boundary. This leads to the change in temperature profile in gage area compared to the original intended temperature profile.

Figure 7.11 show the snap shot of the temperature recorder after the end of phase-2 test. It is to be noted that the phase-2 test duration was approximately from 4.5-4.65 h and at 4.65 h the temperature at the center of the gage was approximately 285°C. As can be seen from Fig. 7.11 during the end of the tests the temperature in the lower half the specimen drops, whereas the temperature of the upper half of the specimen rises. This is due to the specimen pulled downward from the original position based on the location of the test frame actuator. This can also be evident from Fig. 7.12, which shows the specimen location with respect to coil location at the end of test. The estimated effective gage lengths used for converting the stroke and actuator position sensor measurements to strain are given in Table 7.1. Whereas Table 7.2 summarizes the relevant tensile test properties estimated through above-mentioned data.

Figure 7.5 Crosshead displacement (stroke) versus stress for 316 SS base metal specimen under elevated temperature tensile testing

Figure 7.6 Actuator positions versus stress for 316 SS base metal specimen under elevated temperature tensile testing

Figure 7.7 Crosshead displacement (stroke) with respect to phase-1 (known extensometer measurements) strain for 316 SS base metal specimen under elevated temperature tensile testing

Figure 7.8 Actuator positions with respect to stage-1 (known extensometer measurements) for 316 SS base metal specimen under elevated temperature tensile testing

Figure 7.9 Combined phase-1 and 2 stress-strain curves estimated using crosshead displacement (stroke) measurements for 316 SS base metal specimen under elevated temperature tensile testing

Figure 7.10 Combined stage-1 and 2 (full) stress-strain curves estimated using actuator displacement (position) measurements for 316 SS base metal specimen under elevated temperature tensile testing

Figure 7.11 Snap shot of the temperature history recorder after the end of phase-2 tensile test. The phase-2 duration was approximately from 4.5-4.65 hour with gage center temperature of 285 oC at the end 4.65 hour.

Figure 7.12 The specimen location with respect to the fixed coil location after the end of phase-2 test

Table 7.1 Estimated effective gage length (or calibration factor) and specimen nominal gage length for 316 SS base metal specimen under elevated temperature tensile testing

Test number (Strain rate)	L_{eff} estimate in mm (in.)		Specimen gage length in mm (in.)	
	Based on crosshead displacement (stroke)	Based on actuator displacement (position)	Nominal length	Measured length
T04 (0.001/s)	17.748 (0.699)	18.672 (0.735)	14.25 (0.561)	13.487 (0.531)

Table 7.2 Estimated tensile material properties for 316 SS base metal specimen under elevated temperature tensile testing

Test number (Strain rate)	Elastic modulus in GPa (ksi)	0.2% yield		Ultimate		Fracture		Reduction in gauge area (%)
		Stress in MPa (ksi)	Strain in %	Stress in MPa (ksi)	Strain in %	Stress in MPa (ksi)	Strain in %	
T04 (strain rate = 0.001/s)	157.212 (22801.67)	156.067 (22.64)	0.2996	418.717 (60.73)	31.301	275.7 (39.99)	41.24	69.474

7.4 Summary

One elevated-temperature tensile test of 316 SS base metal specimen was conducted under strain rates 0.001/s and approximately at the typical LWR coolant temperature of 300°C. Based on the test data, tensile material properties and stress-strain curves were estimated. The estimated results show that the elevated temperature shows lower yield stress, ultimate stress and fracture strain compared to the corresponding room temperature tensile test data. This shows that at 300°C the 316 SS base metal has softer tensile properties compared to its room temperature conditions.

8 Elevated temperature tensile test of 316 SS -316 SS similar metal weld specimen

8.1 Introduction

To obtain the elevated temperature tensile properties and stress-strain curve of 316 SS-316 SS similar metal weld specimen one elevated temperature tensile test was performed. The tensile test was conducted at a strain rate of 0.001/s while trying to maintain the specimen gage area temperature approximately equal to the typical LWR coolant temperature of 300°C. The geometry of the specimen maintained same as the corresponding room temperature test specimen. Similar procedure, discussed in Sections 4, 6, and 7, was used to conduct the test and estimating the relevant material properties and stress-strain curve. The procedure and results are highlighted below.

8.2 Pretest heating up the specimen

Similar procedure discussed in Section 7.2 was used to initial heating up the specimen until achieving a stable temperature of 300°C at specimen gage area. Figure 8.1 shows the pretest temperature history from various thermocouples. Figure 8.2 shows the corresponding spatial temperature distribution at various location of test specimen and pull rod before the starting of the tensile test.

Figure 8.1 Temperature history at different locations of the specimen and pull rod before the starting of tensile test for 316 SS-316 SS weld metal specimen

Figure 8.2 Temperature distributions at different locations of the specimen and pull rod before the starting of tensile test for 316 SS-316 SS weld metal specimen

8.3 Summary of tensile test results and estimated stress-strain curve

Similar as before a two phase tensile test was performed due to the limitation in extensometer measurement range with first phase was carried out up to 2 % strain with extensometer output as control parameter and the second phase was carried out from 2% strain to complete tensile rupture with frame position sensor output as control parameter.

8.3.1 Estimated phase-1 (up to 2%) stress-strain curve

The estimated stress-strain curve during the first phase of the tensile test is shown in Fig. 8.3. The figure also shows the estimated elastic modulus of 136.42 GPa and yield stress of 354.25 MPa, respectively. The corresponding room temperature test values are 138.02 GPa and 430 MPa (see Table 6.2) respectively. This says under the mentioned elevated temperature the material harden lesser compared to its room temperature condition. During the phase 1 test it is found that the temperature at the gage area had not change much due to the actuator displacement. Figure 8.4 shows the comparison of temperature distribution on specimen at start and end of phase-1 test.

Figure 8.3 Up to 2 % strain elevated temperature (300°C) strain-versus-stress curve for 316 SS-316 SS weld metal specimen

Figure 8.4 Temperature profile at start and end of the phase-1 tensile test

8.3.2 Estimated full (both phase-1 and 2) stress-strain curve

The full stress-strain curve and all related tensile properties for the 316 SS-316 SS weld metal specimen under elevated temperature are estimated using both the phase-1 and phase-2 sensor (extensometer, stroke, actuator position, and load cell) measurements. For example Figures 8.5 and 8.6 show stroke versus stress and actuator position versus stress curves estimated from both the phase-1 and 2 tensile test data, respectively. Whereas Figs. 8.7 and 8.8 show the phase-1 stroke and actuator displacement with respect to stress generated in the gage area. Using the above-mentioned data and the procedure described in section 4.2 the combined phase-1 and 2 stress-strain curves are estimated. Figures 8.9 and 8.10 show the corresponding estimated stress-strain curves with respect to stroke and actuator position measurements, respectively. Also, the Figures 8.9 and 8.10 show the comparison of stress-strain curve with respect to the corresponding room temperature stress-strain curve (refer section 6 for details). From Fig. 8.10 it can be seen that under elevated temperature condition the weld metal harden lesser compared to the room temperature condition with much lower yield and ultimate strength. Also it can be seen under elevated temperature the weld metal fracture much earlier compared to the corresponding room temperature conditions. The estimated effective gage length used for converting the stroke and actuator position measurements to equivalent strain is given in Table 8.1. Also the estimated scalar tensile material properties are summarized in Table 8.2. Also to note that similar to the elevated temperature tensile test for base metal (see Section 7.3.2), the weld specimen also slightly goes out of induction heating coil boundary during the end of phase-2 test. This leads to a slight drop in temperature in gage area. For example, Fig. 8.11 shows the temperature history snap shot at the end of phase-2 test, which was approximately during 4.55 to 4.7 h. At the end of 4.7 h it can found that the temperature at the gage section drops to 290°C approximately. Similarly other location on the specimen either the temperature drops or rises. Hence, with this information care should be taken if intended to use the full stress-strain curve. However, for fatigue modeling purpose it is not necessary to use the full stress-strain curve.

Figure 8.5 Crosshead displacement (stroke) versus stress for 316 SS-316 SS weld metal specimen under elevated temperature tensile testing

Figure 8.6 Actuator positions versus stress for 316 SS-316 SS weld metal specimen under elevated temperature tensile testing

Figure 8.7 Crosshead displacement (stroke) with respect to phase-1 (known extensometer measurements) strain for 316 SS-316 SS weld metal specimen

Figure 8.8 Actuator positions with respect to stage-1 (known extensometer measurements) for 316 SS-316 SS weld metal specimen under elevated temperature tensile testing

Figure 8.9 Combined phase-1 and 2 stress-strain curves estimated using crosshead displacement (stroke) measurements for 316 SS-316 SS weld metal specimen

Figure 8.10 Combined stage-1 and 2 (full) stress-strain curves estimated using actuator displacement (position) measurements for 316 SS-316 SS weld metal specimen

Figure 8.11 Snap shot of the temperature history recorder after the end of phase-2 tensile test for 316 SS-316 SS weld metal specimen. The phase-2 duration was approximately from 4.55-4.7 h with gage center temperature of 290°C at the end 4.7 h.

Table 8.1 Estimated effective gage length (or calibration factor) and specimen nominal gage length for 316 SS-316 SS weld metal specimen under elevated temperature tensile testing

Test number (Strain rate)	L_{eff} estimate in mm (in.)		Specimen gage length in mm (in.)	
	Based on crosshead displacement (stroke)	Based on actuator displacement (position)	Nominal length	Measured length
T05 (0.001/s)	15.446 (0.608)	16.234(0.639)	14.25 (0.561)	14.021(0.552)

Table 8.2 Estimated tensile material properties for 316 SS-316 SS weld metal specimen under elevated temperature tensile testing

Test number (Strain rate)	Elastic modulus in GPa (ksi)	0.2% yield		Ultimate		Fracture		Reduction in gauge area (%)
		Stress in MPa (ksi)	Strain in %	Stress in MPa (ksi)	Strain in %	Stress in MPa (ksi)	Strain in %	
T05 (strain rate = 0.001/s)	136.42(19786.04)	354.252 (51.38)	0.458	476.752 (69.15)	18.784	404.4 (58.65)	27.18	44.693

8.4 Summary

One elevated-temperature tensile test of 316 SS-316 SS weld metal specimen was conducted under strain rates 0.001/s and approximately at the typical LWR coolant temperature of 300°C. Based on the test data, tensile material properties and stress-strain curves were estimated. The estimated results show that the elevated temperature shows lower yield stress, ultimate stress and fracture strain compared to the corresponding room temperature tensile test data. This shows that at 300°C the 316 SS-316SS weld metal has softer tensile properties compared to its room temperature conditions.

9 Estimation of effect of thermal strain and coefficient of thermal expansion in 316 SS base and 316 SS-316 SS similar metal weld material

9.1 Introduction

LWR components may experience significant thermal strains during the heat up and cool down of the reactor. Since thermal strain by itself does not produce any stress, it is necessary to assess the contribution of thermal strain to the total strain. The previously discussed elevated temperature tensile test data for 316 SS base (see Section 7) and 316 SS-316 SS weld (see Section 8) metal specimens are further analyzed to estimate the magnitude of the purely thermal strain which has to be deducted from the total strain measured in order to obtain the mechanical strain that produces stress and to estimate the values of coefficient of thermal expansion (CTE) of base and weld metal. The results are briefly described below.

9.2 Effect of free thermal straining

The pretest data discussed in Sections 7.2 and 8.2 are used to assess the effect of thermal strain. Figure 9.1 shows the overall temperature history during the elevated temperature tensile tests conducted for base and weld metals. The pre-tensile test specimen heat up data are used to estimate the thermal expansion strain. Particularly, data up to temperature stabilization is considered. It is to be noted that during pretest heat up period the specimen was kept under load control with zero load set point and with no other applied mechanical strain. However, the specimen was allowed to expand freely due to thermal transient strain. Figure 9.2 shows the measured stress history for both the test cases. Figure 9.3 shows stabilized temperature profile in the gage area of base and weld specimens. Figure 9.4 shows the gage area temperature build up versus change in gage area length that was measured by the extensometer. Figure 9.5 shows the estimated thermal strain for base and weld metal. From the figure it can be seen that weld metal experienced lower thermal strain compared to the base metal.

Figures 9.4 and 9.5 show that the extensometer recorded a spike in the weld strain during initial heat up. This is a test related anomaly (may be due to instantaneous slippage of extensometer) which should be ignored. Also, from Fig. 9.5 it can be seen that the strain increases to 0.5 % during the temperature build up from room temperature to typical LWR temperature of 300°C. This thermally-induced strain could lead to significant thermal stress had the specimen been constrained. In addition, from Fig. 9.1 it can be seen that during start of the pretest heating up, there is a 50°C jump in the temperature beyond which the temperature increase steadily up to the stabilized temperature. This is a test related anomaly, which is most likely caused by large electromagnetic interference when the induction heater was switched on. For the purpose of estimating the CTE, only the thermal strain data above 50°C was considered. The estimated variations of the CTEs of both materials with temperature are shown in Fig. 9.6. From the figure it can be seen that the CTE for both base and weld metals increase with temperature, although the magnitude is smaller for weld metal. This smaller CTE magnitude for weld metal compared to base metal is due to smaller thermal strain as can be seen from Fig. 9.4. This preliminary CTE test data can be used for the mechanistic modeling while considering thermal transient and cyclic loading. However, the above-discussed approach to estimate the CTE does not follow the

guidelines of ASTM standard and appropriate guidelines to be followed for accurate estimation of CTE.

Figure 9.1 Comparison of base and weld metal tensile test temperature profile at specimen gage area

Figure 9.2 Comparison of base and weld metal tensile test stress profile at specimen gage area

Figure 9.3 a) Schematic showing the location of thermocouples b) Comparison of base and weld metal pre tensile test, stabilized temperature profile at specimen gage area

Figure 9.4 Specimen gage area temperature versus extensometer measurements for base and weld metals

Figure 9.5 Specimen gage area temperature versus estimated thermal strain for base and weld metals

Figure 9.6 Specimen gage area temperature versus estimated coefficient of thermal expansion for base and weld metals

9.3 Summary

The effects of thermal transient in 316 SS base and 316 SS-316 SS weld metals are estimated. This is evaluated using the pretest data during the heat up of the corresponding tensile specimens. From the results it is found that the thermal strain can reach 0.5 % during the temperature build up from room temperature to typical LWR temperature of 300°C. This strain, if constrained, could lead to significant thermal stress if the specimen were constrained.. These types of data suggest the need for pure thermal cycling test to model the effect of pure thermal cyclic stress during reactor heat up and cool down. Also using the above-mentioned pre tensile test data, approximate CTEs for both the base and weld metals were estimated, which can be used for the future mechanistic model development.

10 Mechanistic modeling: Evaluation of dynamic crack modeling, with application to steam generator tube rupture simulation

10.1 Introduction

Mechanistic modeling of environmental damage, such as stress corrosion cracking (SCC) and corrosion fatigue (CF), requires crack initiation and/or propagation modeling. Finite element techniques can be used for this purpose. However, modeling crack propagation using the conventional finite element method (FEM) is highly cumbersome, particularly for moving crack tips, because it requires remeshing of the finite element domain after each crack propagation increment. In addition, the crack path must be known beforehand, as well as which remeshing has to be performed. However, in reality, the crack may follow an arbitrary path, and efficient crack propagation modeling requires the crack path to be solution dependent or automatic.

In conventional FEM, at each time interval during which the crack grows, the element boundary has to be aligned along the crack path, which may not be the case in practice. Also, conventional FEM often fails to converge while modeling discontinuities, such as cracks. All of the above-mentioned limitations restricted the use of conventional FEM for modeling the moving crack tip. However, with the recent advancement of the extended finite element method (XFEM), modeling moving cracks has become possible. The development of XFEM was first linked to the work of Babuska, et al. [3] and Melenk and Babuska [4]. They proposed the partition of unity method (PUM), which allows the use of local enrichment functions to model cracks. This helps avoid singularity problems associated with discontinuities in conventional FEM. Also, the development of the level set method (LSM) [5,6] has made it easier to model cracks, particularly for modeling moving interfaces or shapes, such as cracks. The LSM has made it possible to perform numerical computations involving curves and surfaces on a fixed Cartesian grid without having to parameterize the bulk material or object.

Belytschko and Black [7] first extended the concept of PUM and LSM to conventional FEM for solving linear elastic fracture mechanics problems. The resulting method is popularly known as the extended finite element method or XFEM. The XFEM method was further improved by many other researchers [8-16] and has recently been implemented in commercially available software, such as ABAQUS [17]. In the present work, the use of XFEM through ABAQUS is evaluated by modeling crack initiation and propagation in steam generator tubes. Currently, the model does not consider the environmental effects of SCC/CF, only the transient crack initiation and propagation at room temperature. However, in the future SCC/CS will be modeled using XFEM as one of the computational tools. The current results have been validated against the experimental results available under ANL's steam regenerator tube integrity program sponsored by NRC [18]. The details of the model and results are discussed in the following subsections.

10.2 Theoretical background

10.2.1 Extended finite element method: Generic theoretical background

In the generic XFEM framework [7-15], the displacement field in a finite element crack domain can be expressed as

$$\begin{aligned}
 u &= \sum_i^N N_i(x) q_i + \text{enriched function} \\
 &= \sum_{i=1}^N N_i(x) q_i + \sum_{i=1}^N N_i(x) H(x) a_i + \sum_{i=1}^N N_i(x) \sum_{\alpha=1}^4 F_{\alpha} b_i^{\alpha} \\
 &= \sum_{i=1}^N N_i(x) \left[q_i + H(x) a_i + \sum_{\alpha=1}^4 F_{\alpha} b_i^{\alpha} \right]
 \end{aligned} \tag{10.1}$$

where $N_i(x)$ and q_i are, respectively, the usual nodal shape functions and nodal degree-of-freedom (DOF) vector used in conventional FEM and associated with the continuous part of the finite element model; $H(x)$ and a_i are, respectively, the Heaviside function and nodal-enriched DOFs associated with the cracked geometry; and F_{α} and b_i^{α} are, respectively, the additional asymptotic crack tip functions and the associated enriched-nodal DOFs. The Heaviside function $H(x)$ can be given as

$$H(x) = \begin{cases} +1 & x \text{ above the crack surface} \\ -1 & x \text{ below the crack surface} \end{cases} \tag{10.2}$$

The asymptotic crack tip functions F_{α} can be given as

$$F_{\alpha}(x) = \left\{ \sqrt{r} \sin\left(\frac{\theta}{2}\right), \quad \sqrt{r} \cos\left(\frac{\theta}{2}\right), \quad \sqrt{r} \sin(\theta) \sin\left(\frac{\theta}{2}\right), \quad \sqrt{r} \sin(\theta) \cos\left(\frac{\theta}{2}\right) \right\} \tag{10.3}$$

where (r, θ) is the polar coordinate system with its origin at the crack tip. The finite element global equilibrium equation associated with the displacement field in Eq. (10.1) can be given as

$$\begin{bmatrix} \mathbf{K}_{qq} & \mathbf{K}_{qa} & \mathbf{K}_{qb} \\ \mathbf{K}_{aq} & \mathbf{K}_{aa} & \mathbf{K}_{ab} \\ \mathbf{K}_{bq} & \mathbf{K}_{ba} & \mathbf{K}_{bb} \end{bmatrix} \begin{Bmatrix} \mathbf{q} \\ \mathbf{a} \\ \mathbf{b} \end{Bmatrix} = \begin{Bmatrix} \mathbf{f}_q \\ \mathbf{f}_a \\ \mathbf{f}_b \end{Bmatrix} \tag{10.4}$$

The element stiffness matrix associated with the global equilibrium in Eq. (10.4) can be given as

$$\begin{bmatrix} \mathbf{K}_{qq}^e & \mathbf{K}_{qa}^e & \mathbf{K}_{qb}^e \\ \mathbf{K}_{aq}^e & \mathbf{K}_{aa}^e & \mathbf{K}_{ab}^e \\ \mathbf{K}_{bq}^e & \mathbf{K}_{ba}^e & \mathbf{K}_{bb}^e \end{bmatrix} = \int_{A_e} \begin{bmatrix} \mathbf{B}_q^T \mathbf{D} \mathbf{B}_q & \mathbf{B}_q^T \mathbf{D} \mathbf{B}_a & \mathbf{B}_q^T \mathbf{D} \mathbf{B}_b \\ \mathbf{B}_a^T \mathbf{D} \mathbf{B}_q & \mathbf{B}_a^T \mathbf{D} \mathbf{B}_a & \mathbf{B}_a^T \mathbf{D} \mathbf{B}_b \\ \mathbf{B}_b^T \mathbf{D} \mathbf{B}_q & \mathbf{B}_b^T \mathbf{D} \mathbf{B}_a & \mathbf{B}_b^T \mathbf{D} \mathbf{B}_a \end{bmatrix} dA_e \quad (10.5)$$

In Eq. (10.5), $\mathbf{D}$ is the material property matrix, and $\mathbf{B}$ stands for the respective strain-displacement matrix associated with the conventional finite element nodal DOFs (subscript $\mathbf{q}$), crack domain enriched-nodal DOFs (subscript $\mathbf{a}$), and crack tip-enriched nodal DOFs (subscript $\mathbf{b}$).

10.2.2 XFEM modeling through ABAQUS

In the present work, commercially available ABAQUS based extended finite element technique is used to model the initiation and propagation of moving cracks in steam generator tubes. To note that for simplicity the current version of ABAQUS does not allow considering the displacement field associated with the asymptotic crack tip functions given in Eq. (10.1), particularly for modeling moving cracks. The total displacement field considered for modeling a moving crack in the present work can then be as follows:

$$\begin{aligned} u &= \sum_i^N N_i(x) q_i + \text{enriched function} \\ &= \sum_{i=1}^N N_i(x) q_i + \sum_{i=1}^N N_i(x) H(x) a_i = \sum_{i=1}^N N_i(x) [q_i + H(x) a_i] \end{aligned} \quad (10.6)$$

In addition to the techniques common to conventional FEM, the XFEM procedure in ABAQUS has additional techniques, such as phantom node modeling, level set methods, and cohesive zone modeling. A brief discussion of these techniques is given below. The details of these techniques can be found in the ABAQUS user manual [17] and elsewhere in the literature [5-16] related to these topics.

Phantom node modeling approach

In the phantom node approach, additional nodes are created surrounding the crack. These nodes remain even after the crack has passed through that element. These nodes are introduced to represent the discontinuity associated with the cracked elements. These phantom nodes are associated with the Heaviside function $H(x)$ in Eq. (10.6). The location of the phantom nodes with respect to real nodes and the cracked element is schematically shown in Fig. 10.1. Phantom nodes are automatically created in the crack tip element of the uncracked mesh. These nodes are superimposed on the real nodes of the element, and when the element is intact these phantom nodes are fully constrained to the real nodes. When the crack passes through the element, the element gets separated into two superimposed elements, consisting of a combination of real nodes and phantom nodes, as shown in Fig. 10.1b. In the new elements, the cracked surfaces are separated according to traction separation techniques, which are described in the following subsection. Unlike the uncracked finite elements, the time-dependent stiffness matrix of the new cracked element is computed by integrating over the area from the side of the real nodes up to

the crack surface only. For example, the stiffness matrix of the cracked element shown in Figure 10.1 can be expressed as below:

$$\int_{A_e} (B^T DB) dA_e = \int_{A_t} (B^T DB) dA_t + \int_{A_b} (B^T DB) dA_b \quad (10.7)$$

where A_t and A_b are the real area of the top and bottom portion of the cracked element, respectively.

Figure 10.1 Schematic of (a) cracked and uncracked mesh showing real and phantom nodes and (b) cracked element as sum of two virtual or phantom elements

Level set method

In the XFEM framework it is essential to automatically track the crack surface and crack front. This tracking is made possible by the level set representation, in which the crack plane surface and crack tip surface are represented by two level set functions or fields. Figure 10.2 schematically shows the level set fields for the crack plane surface and crack tip surface represented by $\Phi = 0$ and $\psi = 0$, respectively [15]. These surfaces are assumed orthogonal, such that $\nabla\Phi \cdot \nabla\psi = 0$. The values of these fields are time or solution dependent. These functions are computed on a narrow band of grid points surrounding the crack surface and tip. These field values can be used not only to obtain the geometric information regarding the location of the crack but also the local coordinate system that can be used to generate the enrichment function $H(x)$ in Eq. (10.6). The LSM does not require explicit representations of the crack boundary/interface because they are defined entirely by the solution-dependent surfaces, $\Phi = 0$ and $\psi = 0$.

Figure 10.2 Schematic showing the orthogonal level set fields that describe the crack tip

Crack initiation criteria

A crack, to be initiated in any element, has to satisfy certain criteria. Different initiation criteria can be defined in terms of solution-dependent stress, strain, etc., and respective critical values. In the present work, the maximum principal stress criterion is used for the initiation of the crack. The maximum principal stress initiation criterion is given below:

$$f = \frac{\langle \sigma_{\max}^p \rangle}{\sigma_{cr}^p} = \begin{cases} \geq 1 + f_{tol} & \text{crack will initiate} \\ < 1 + f_{tol} & \text{crack will not initiate} \end{cases} \quad (10.8)$$

where $\sigma_{\max}^p$ is the solution-dependent maximum principal stress, and σ_{cr}^p is the critical principal stress that has to be provided as an input material property. In Eq. (10.8), the symbol $\langle \rangle$ represents Macaulay brackets with $\langle \sigma_{\max}^p \rangle = 0$ if $\sigma_{\max}^p < 0$, i.e., when the maximum principal stress is purely compressive.

Crack evolution through traction separation criteria

Crack evolution criteria describe the rate at which traction is applied to the cracked surface of the cracked element following initiation. The traction in a cracked element is shown schematically in Fig. 10.1b. The three-dimensional traction in a cracked element can be found by using the following expression:

$$\mathbf{t} = \begin{Bmatrix} t_n \\ t_s \\ t_t \end{Bmatrix} = \begin{bmatrix} K_{nn} & 0 & 0 \\ 0 & K_{ss} & 0 \\ 0 & 0 & K_{tt} \end{bmatrix} \begin{Bmatrix} \delta_n \\ \delta_s \\ \delta_t \end{Bmatrix} \quad (10.9)$$

where $K_{ii, i=n,s,t}$ are calculated based on the elastic properties of the cracked element; t_n , t_s , and t_t are the traction along normal, first shear, and second shear directions; and δ_n , δ_s , and δ_t are the respective separation displacements. The separation displacements can be calculated using the traction separation curve [19, 20], shown in Fig. 10.3.

Figure 10.3 Schematic of traction separation curve

The area under the curve in Fig. 10.3 can be assumed to represent the fracture energy. For simplicity, assuming linear traction separation behavior, and hence a linear traction separation curve, the separation displacement δ can be calculated from

$$G_{cr} = \frac{1}{2} t_0 \delta_f \quad (10.10)$$

where G_{cr} is the critical fracture energy or fracture toughness, and t_0 is the solution-dependent traction at crack initiation and can be related to the crack initiation principal stress $\sigma_0 \geq \sigma_{cr}^p$, where σ_{cr}^p is the critical principal stress given by Eq. (10.8).

10.3 Results and analysis

The above XFEM technique was used to model crack initiation and propagation in steam generator (SG) tubes under simulated severe accident conditions. The results are compared with

the experimental data obtained through the NRC-sponsored SG tube integrity experiments conducted at ANL. Several Alloy 600 SG tubes were tested under different pressure conditions, and the details of these tests can be found in NUREG/CR-6804 [18]. In the present report, only a few prototypical cases are considered to verify the capability of the XFEM modeling techniques. The details of the model and results are discussed below.

10.3.1 SG tube model with single initial crack

In the first case, SG tube models were developed with a single preexisting part-through-wall axial crack from the outer diameter (OD) surface. The tubes had an OD of 22.2 mm (7/8 in.), and a thickness of 1.27 mm and were made from Alloy 600 material. The material properties considered for the present FEM are given in Table 10.1, and the stress-strain curve is shown in Fig. 10.4. Three-dimensional brick elements were used to model the tube. The initial crack was modeled as a shell or planar geometry and assembled to the tube geometry. A typical FEM model of an SG tube is shown in Fig. 10.5. The model also included a part-through-wall OD axial crack with length of 6.35 mm and a ratio for the crack depth to tube wall thickness (a/h) of 75%. In the FEM model the geometric and force boundary conditions are applied such that it can equivalently represent the experimental boundary conditions. In the NRC-sponsored SG tube integrity experiments, one end of the SG tube was fixed to the compressed air flow path, whereas the other end was plugged to help build up the pressure inside the tube. As in the experiment conditions, the FEM model inner surface was subjected to an increasing pressure. In addition, an equivalent longitudinal pressure applied to the end plug was used to simulate the far-field biaxial stress field. Crack initiation and propagation were simulated for an increasing applied internal pressure. Note that the propagation of the initial crack does not occur immediately after the pressurization starts. The crack may start growing only after a critical pressure is reached.

Table 10.1 Room-temperature material properties for Alloy 600

Elastic modulus (GPa)	200
Poisson's ratio	0.3
Yield strength σ_y (MPa)	296
Ultimate strength σ_u (MPa)	684
Critical principal stress in Eq. (10.8) $\sigma_{cr}^p = 0.5(\sigma_y + \sigma_u)$	490
Critical fracture energy G_{cr} (kJ/m ²) in Eq. (10.10)	415

Figure 10.4 Room-temperature stress-strain curves for Alloy 600

Figure 10.5 Typical FEM model of a 22.2-mm (7/8-in.) OD tube with an initial crack length of 6.35 mm and crack depth to wall thickness ratio of 75%: (a) OD surface and (b) cut section of the cross section

In real nuclear plants, SG tubes containing preexisting SCC cracks may start to grow due to a pressure transient when the internal pressure reaches a critical value. Such case may occur during a design-basis accident. In the XFEM model, this critical pressure can be estimated when the solution-dependent maximum principal stress equals or exceeds the limiting critical principal stress σ_{cr}^p , as given by Eq. (10.8). In all the XFEM models discussed in this work, σ_{cr}^p is assumed to be represented mathematically by $\sigma_{cr}^p = 0.5(\sigma_y + \sigma_u)$, which is approximately the

flow stress of the material. The predicted ligament rupture and final burst pressures based on this assumption match the corresponding experimental values reasonably well. Note that in the experiment, the crack after initiation did not arrest but continued to propagate unstably until full ligament rupture (but before burst). The details of the findings are discussed further below.

For the FEM model shown in Fig. 10.5, the maximum principal stress distribution at an applied pressure of 24.66 MPa is shown in Fig. 10.6. The simulation results indicate that the crack starts and begins to propagate along the radial or wall thickness direction at this pressure. also indicate that the maximum principal stress at the crack tip element exceeds the limiting principal stress, σ_{cr}^p , of 490 MPa (Table 10.1). After initiation, the crack grows further in the radial direction (along the thickness) and ruptures the last ligament in the inner diameter (ID) surface. The corresponding applied pressure is referred to here as “ligament rupture pressure.” After the ID ligament ruptures, the crack grows further in the axial direction unstably with increasing pressure until the FEM calculation fails to converge because of issues associated with large plastic deformation. The corresponding applied internal pressure is referred to as the “burst pressure.” For the FEM model shown in Fig. 10.5, the estimated ID ligament rupture and burst pressure were found to be 37.73 MPa and 40.01 MPa, respectively. The corresponding experimental values were reported as 36.5 and 41.2 MPa, respectively, showing a good correlation between the XFEM model and experimental results. Figures 10.7a and 10.7b show the corresponding OD surface shape at the ligament rupture pressure and burst pressure, respectively. The experimental specimen after bursting, shown in Fig. 10.8, has a remarkably similar geometry to the FEM-predicted shape (Fig. 10.7b).

Figure 10.9 shows the time-dependent (or with respect to applied pressure) crack opening displacement (COD) at the OD and ID surface. It shows that, although the OD COD is larger than the ID COD, both increase unstably after the ID ligament rupture. Figure 10.10 shows the estimated equivalent plastic strain with respect to applied pressure at a radial crack-tip element (in front of the initial crack) and at a central ID ligament element. Figures 10.9 and 10.10 both show that the COD and the maximum equivalent plastic strain behave in a similar manner with increasing pressure. In addition to the above-mentioned model, additional tube models were developed with different initial crack lengths and crack depth to wall thickness ratios (a/h). In a parametric study, some of these results are depicted in Figures 10.11 and 12. For example, Fig. 10.11 shows the radial crack initiation pressure and ID ligament rupture pressure as functions of a/h . As a/h increases, the corresponding radial crack initiation and ID ligament rupture pressures decrease non-linearly. Similar trends can also be seen with increasing initial crack length, as shown in Fig. 10.12.

Figure 10.6 Maximum principal stress distribution upon exceeding the critical principal stress σ_{cr}^p just before the crack initiation or cracking of the crack-tip element in front of initial crack in radial direction

Figure 10.7 Shape of the OD surface and maximum principal stress distribution for the 22.2-mm OD tube at (a) ID ligament rupture pressure (37.5 MPa) and (b) final burst pressure (40.01 MPa)

Figure 10.8 After burst shape of a typical 22.2-mm diameter tube with 6.35 mm initial notch: (a) top view and (b) side view

Figure 10.9 Estimated COD with respect to applied pressure at the OD and ID surface of the 22.2-mm OD tube

Figure 10.10 Estimated equivalent plastic strain with respect to applied pressure at radial crack-tip element (in front of the initial crack) and central ID ligament element of the 22.2-mm OD tube

Figure 10.11 Radial crack initiation pressure and ID ligament rupture pressure with respect to different ratios of initial crack depth to wall thickness

Figure 10.12 Radial or wall thickness crack initiation pressure and ID ligament rupture pressure with respect to different initial crack length

10.3.2 SG tube model with two initial cracks for crack coalescence simulation

In the second case, Alloy 600 SG tubes were modeled with two initial cracks to simulate crack coalescence. For this purpose, a 22.2-mm (7/8-in.) OD tube with different initial crack configurations was modeled. For example, Figure 10.13 shows the FEM model of the tube with two 72% part-through OD axial cracks, each with a length equal to 6.2 mm. The two cracks are separated by an uncracked axial ligament of length 0.25 mm. Figure 10.13 also shows an uncracked ligament of length 0.36 mm in the radial direction. Due to continuous pressurization, a sequence of events would occur that can easily be modeled through a single XFEM simulation.

This model involves several consecutive events: the axial crack initiates at the crack-tip elements in the axial ligament; then, the uncracked axial ligament is completely ruptured, creating a single partial through-wall crack; then, the radial ligament crack initiate, propagate and completely ruptures, creating a single 100% through-wall crack; then, the tube ruptures unstably. The predicted applied internal pressure corresponding to some of the above-mentioned events can be found in Table 10.2. For example, the tube in case 1 has an initial crack length of 12.7 mm ($2c + b$), $b = 0.25$ mm, and a/h ratio of 72%; after pressurization the model predicts that the axial ligament crack initiates at applied internal pressure of 15.86 MPa, the corresponding uncracked axial ligament of length $b=0.25$ mm completely ruptures at 16.81 MPa, and the uncracked radial ligament completely ruptures at 30.97 MPa. The latter is well correlated with the experimentally measured radial ligament rupture pressure of 33.8 MPa. The FEM program stopped at 31.22 MPa possibly due to a convergence problem associated with large accumulated plastic strain and

unstable crack growth. The shapes of the OD surface at ID ligament rupture and final burst can be seen from Figs. 10.14a and 10.14b, respectively. The corresponding variation in COD at OD and ID with respect to the applied pressure can be seen from Fig. 10.15. As evident from the figures, after the ID ligament rupture, the COD grows unstably. Similar trends can also be observed from Fig. 10.16, showing equivalent plastic strain at two OD and ID elements with respect to applied pressure. Figure 10.17 shows the equivalent plastic strain distribution near the ID crack region after the ID ligament ruptures at 30.97 MPa applied pressure. This figure indicates substantial plastic strains, on the order of 20-25%, at the time of ID ligament rupture.

Two additional cases with different initial cracks were also simulated, and the results are summarized in Table 10.2. In case 2, the two initial cracks were modeled with $2c + b = 12.7$ mm, $a/h = 70$, and $b = 0.13$ mm. The FEM calculations estimated an ID ligament rupture pressure of 31.51 MPa, which is well correlated with the experimental value of 33.8 MPa. In case 3, two through-wall initial cracks were modeled with $2c + b = 12.7$ mm, $a/h = 100$, and $b = 0.25$ mm. In this case, the crack initiation in the axial ligament is predicted to start at 3.8 MPa, which is well below the corresponding applied pressure of 15.86 MPa for case 1. Note that case 1 has the same initial crack geometry as case 3, but they differ in that a/h is 72% for case 1 and 100% for case 3. It is also predicted that the axial ligament rupture pressure for case 3 is 4.1 MPa, which is also well below the axial ligament rupture pressure for case 1.

Figure 10.13 FEM model of 22.2-mm (7/8-in.) OD tube with two interacting initial cracks

Figure 10.14 Shape of the OD surface and maximum principal stress distribution for case 1 at (a) ID ligament rupture pressure (30.97 MPa) and (b) final burst pressure (31.22 MPa)

Figure 10.15 Estimated COD with respect to applied pressure at the OD and ID surface for case-1 tube (see Table 10.2) with $2c + b = 12.7$ mm, $a/h = 72$, and $b = 0.25$ mm

Figure 10.16 Estimated equivalent plastic strain with respect to applied pressure at the OD and ID surface for case-1 tube (see Table 10.2) with $2c + b = 12.7$ mm, $a/h = 72$, and $b = 0.25$ mm

Figure 10.17 Distribution of equivalent plastic strain at 30.97 MPa (radial ligament rupture pressure) for case-1 tube (see Table 10.2) with $2c + b = 12.7$ mm, $a/h = 72$, and $b = 0.25$ mm

Table 10.1 XFEM and experimental results for three cases of crack coalescence model

Case No.	Initial crack description	XFEM model results (MPa)				Experiment (MPa)
		Axial ligament crack initiation pressure	Axial ligament rupture pressure	Radial ligament rupture pressure	Burst pressure	Radial ligament rupture pressure
1	a/h=72% 2c+b=12.7 b=0.25	15.86	16.81	30.97	31.2 2	33.8
2	a/h=70% 2c+b=12.7 b=0.13	14.98	15.14	31.51	31.7 1	33.8
3	a/h=100% 2c+b=12.7 b=0.25	3.8	4.1	NA	22.3 02	NA

10.4 Summary

Multiple XFEM models were developed to predict crack initiation and propagation in Alloy 600 SG tubes with persisting crack(s). The results are compared with the experimental results available from the NRC-supported tube integrity program and conducted at ANL. The XFEM predicted rupture and burst pressure results agreed well with available experiment results at room temperature. This exercise shows that the XFEM technique can effectively be used to model propagating cracks until SG tube rupture under design-basis accident conditions. A similar technique may be useful to model stress corrosion cracking and or fatigue cracks, which is one of our future tasks.

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