Failure Behavior of Internally Pressurized Flawed and Unflawed Steam Generator Tubing at High Temperatures - Experiments and Comparison With Model Predictions

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Abstract

This report summarizes experimental work performed at Argonne National Laboratory on the failure of internally pressurized steam generator tubing at high temperatures ($\geq 700^{\circ}\text{C}$). A model was developed for predicting failure of flawed and unflawed steam generator tubes under internal pressure and temperature histories postulated to occur during severe accidents. The model was validated by failure tests on specimens with part-through-wall axial and circumferential flaws of various lengths and depths, conducted under various constant and ramped internal pressure and temperature conditions. The failure temperatures predicted by the model for two temperature and pressure histories, calculated for severe accidents initiated by a station blackout, agree very well with tests performed on both flawed and unflawed specimens.

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Executive Summary

This report summarizes experimental work performed at Argonne National Laboratory (ANL) on the failure of internally pressurized steam generator tubing at high temperatures ($\geq 700^{\circ}\text{C}$). A high-temperature facility was constructed. It consisted of a length of steam generator tubing placed in a furnace, closed off at one end, and connected to a high-pressure nitrogen gas bottle through suitable valving at the other end. During a test, the specimen was stabilized at a nominal steam generator operating temperature of 300°C and the internal pressure and/or temperature were then ramped up at a predetermined rate until specimen failure occurred.

Analyses of the failure pressure and temperature of tubes under severe accident conditions (i.e., $T > 350^{\circ}\text{C}$) were conducted. The severe-accident scenario under investigation involved a total station blackout, with a stuck-open steam generator secondary-side atmospheric dump valve, resulting in loss of feedwater and secondary-side depressurization. Two time/temperature histories were considered, based upon severe-accident analyses by Idaho National Engineering and Environmental Laboratory (INEEL) and Electric Power Research Institute (EPRI). At these high temperatures, plastic deformation is likely to be much more extensive than at normal reactor operating temperatures, and creep effects may no longer be negligible. To predict failure times and temperatures under these two hypothetical accident scenarios, two types of predictive models, namely the flow stress model and the creep rupture model, were evaluated.

The flow stress model assumes that failure of unflawed tubes occurs when the applied hoop stress in the tube becomes equal to the flow stress of the material, which is typically taken to be the average of the yield and ultimate tensile strengths. For a tube that contains a preexisting part-through axial flaw, the hoop stress in the ligament of the flaw is equal to the nominal hoop stress multiplied by a stress multiplication factor. Three existing formulations (developed by Battelle Columbus Laboratory [BCL], Pacific Northwest National Laboratory [PNNL], INEEL, and Electricité de France [EDF]) for this multiplication factor were evaluated on the basis of their prediction of a series of low-temperature failure tests conducted by PNNL on axially flawed Alloy 600 steam generator tubes. At ANL, we developed a new multiplication factor that can be used to predict the failure pressures of the PNNL tests more accurately than all three existing formulations of the flow stress model, particularly so for shorter and deeper cracks. The ANL multiplication factor was used to evaluate the flow stress model at high temperatures.

The creep rupture model assumes a linear summation of creep damage in terms of time under stress divided by the time to fail by creep rupture at that stress. These incremental damage fractions are integrated over the stress-vs.-time/temperature history of the tube, and failure is predicted when this integrated damage fraction is equal to unity. The stress magnification factors used for the flow stress analyses of part-through cracks are also assumed to be applicable to the case of creep. The time to creep rupture used for evaluating the damage fraction was calculated with Larson-Miller parameter correlations which were developed for Alloy 600 from creep rupture data that were reported in the literature. The creep rupture model was validated by failure tests on specimens with part-through axial flaws of various lengths and depths, conducted under various constant and ramped internal pressure and temperature conditions. In all cases, predictions of the creep rupture model were closer to experimental results than those of the flow stress

model. Also, the failure temperatures predicted by the creep rupture model for the two temperature and pressure histories calculated for severe accidents initiated by a station blackout agreed better with tests performed on both flawed and unflawed specimens than those predicted by the flow stress model.

The creep rupture model was further extended to predict failure of tubes with part-through circumferential cracks. A new multiplication factor for axial stress in the ligament was developed on the basis of room-temperature tests reported in the literature. The same multiplication factor was used to predict failure at high temperature by the creep rupture model. Several temperature ramp tests were conducted under a constant pressure on tubes with a single or two symmetrical part-through circumferential flaws. The symmetrically flawed specimens were used to simulate bending constraints that are known to have an important effect on the failure of circumferentially cracked tubes. The test failure temperatures agreed well with predicted values.

List of Acronyms

ANL	Argonne National Laboratory
BCL	Battelle Columbus Laboratories
COA	Crack opening angle
COD	Crack opening displacement
EDF	Electricité de France
EPRI	Electric Power Research Institute
FEA	Finite-element analysis
ID	Inner diameter
INEEL	Idaho National Engineering and Environmental Laboratory
LMP	Larson-Miller parameter
MSLB	Main steam line break
NRC	U.S. Nuclear Regulatory Commission
OD	Outer diameter
PNNL	Pacific Northwest National Laboratory
RMS	Root mean square
PWR	Pressurized water reactor
SBO	Station blackout
SCDAP	Severe Core Damage Analysis Program
SG	Steam generator
SS	Stainless steel

List of Symbols

a	crack depth in tube
A	coefficient for uniaxial creep rate law
b	
c	semiaxial crack length in tube ($2c$ = axial crack length)
C^* and J	path-independent integrals
dt	time increment
EI	elastic beam bending stiffness
h	wall thickness of tube
h_2	function for calculating crack opening displacement (COD)
k	flow stress factor
K	stress index for circumferential cracks
m	bulging factor for axial through-wall cracks, parameter for circumferential cracks, and stress exponent for uniaxial stress-strain law
m_p	magnification factor for part-through cracks
M_{fail}	bending moment at failure
n	number of circumferential cracks and stress exponent for uniaxial creep rate law
N	empirical parameter for circumferential cracks
P	load
P_o	reference load
P_{lm}	Larson-Miller parameter for creep rupture
p	pressure
p_b	failure pressure for unflawed tube
p_{fail}	failure pressure for tubes with circumferential cracks
p_{cr}	failure pressure for tube with through-wall crack
p_{sc}	ligament failure pressure for tube with part-through crack
R_m	mean radius of tube
s	standard deviation
S	elastic rotational stiffness
t	time
t_f	time to tube failure
t_R	time to creep rupture of a uniaxial specimen at a given stress and temperature
T	temperature
T_f	failure temperature of tube
y'	beam rotation due to bending
α	parameter dependent on a/h used in ANL improved m_p correlation; also parameter for uniaxial stress-strain law

β	angular location of neutral axis
$\delta, \dot{\delta}$	crack opening displacement, displacement rate
$\Delta p, \Delta a$	errors in predicted failure pressure and measured crack depth, respectively
$\epsilon, \dot{\epsilon}$	strain, strain rate
ϵ_0	parameter for uniaxial stress-strain law
γ	ratio of outer and inner radii
θ	semiangular length of circumferential crack
λ	normalized crack length = $\left[12(1 - \nu^2)\right]^{\frac{1}{4}} \frac{c}{\sqrt{R_m h}} = \frac{1.82c}{\sqrt{R_m h}}$
ν	Poisson's ratio
σ	stress, hoop stress, axial stress
σ^*	maximum flow stress
σ_{lig}	ligament stress
σ_m	standard error of mean estimate of fitted variable
σ_0	parameter for uniaxial stress-strain law
$\bar{\sigma}$	flow stress
σ_y	yield strength
σ_u	ultimate tensile strength

1 Introduction

Operating experience with PWR steam generators in both the U.S. and abroad has shown that cracks of various morphologies can and do occur in steam generator tubes, starting early in life. These may be single cracks that are axial or circumferential, inside diameter (ID)- or outside diameter (OD)-initiated, part-through-wall or through-wall, or they may be multiple cracks that are parallel or form a network. Tests have shown that, depending on the location and morphology of these cracks, the steam generator tubes can be weakened to various extents.

Under normal operating conditions, the temperature in a PWR steam generator is $\approx 300^{\circ}\text{C}$ and the pressure across the tube wall, Δp_{no} , is ≈ 9 MPa (1300 psi); under design basis accidents, such as a main steamline break (MSLB) in which the secondary side has dropped to atmospheric pressure, the pressure across the tube wall, Δp_{MSLB} , is 18 MPa (2560 psi), and the temperature of the steam generator tubing is $< 350^{\circ}\text{C}$. In this temperature range, creep effects are negligible in Alloy 600. Degraded tubes must actually be capable of withstanding $3 \cdot \Delta p_{\text{no}} \approx 27$ MPa (3900 psi) and $1.4 \cdot \Delta p_{\text{MSLB}} \approx 25$ MPa (3660 psi) to meet requirements for continued operation. For typical unflawed steam generator tubes made of Alloy 600, the failure pressure, p_b , at these temperatures is ≈ 65 MPa (9400 psi).

Severe accidents that involve significant core damage are unlikely events in nuclear reactors. Even in the unlikely event that such an accident should occur, in most cases, any potential risk to the public is mitigated by the presence of a robust containment. The behavior of steam generator tubing during such severe accidents is of particular interest, because failure of the steam generator tubes could lead to bypass of the containment. As a part of its effort to develop guidance for accident analysis, the Nuclear Regulatory Commission (NRC) and its contractors and Electric Power Research Institute (EPRI) have been investigating the potential for failure of steam generator tubes during severe accidents. The accident sequences that appear to produce the greatest risk of steam generator tube failure are those in which the reactor pressure vessel fails to depressurize, but depressurization does occur on the secondary side. Even in these cases, preliminary investigations^{*,†} suggest that failures are likely to occur in the hot-leg nozzle or the inlet surge-line nozzle, leading to depressurization of the reactor system, which prevents failure of steam generator tubes. However, such calculations are subject to large uncertainties, and the NRC is pursuing additional studies to better understand the progression of such sequences, the temperature of the steam generator tubes during such accidents, and the behavior of steam generator tubes at the high temperatures associated with such accidents. At these high temperatures, plastic deformation is likely to be much more extensive than at normal reactor operating temperatures, and creep effects

* P. G. Ellison, L. W. Ward, C. Dobbe, S. A. Chavez, C. L. Atwood, C. L. Smith, L. M. Wolfram, J. L. Jones, L. N. Haney, and W. J. Reece, The Risk Significance of Induced Steam Generator Tube Rupture, INEL-95/0641, Rev. 1 (Draft), Lockheed Martin Idaho Technologies, Inc., Idaho National Engineering and Environmental Laboratory, December 15, 1995.

† E. L. Fuller, M. A. Kenton, M. Epstein, R. E. Henry, and N. G. Cofie, Risks from Severe Accidents Involving Steam Generator Tube Leaks or Ruptures, EPRI TR-106194, Electric Power Research Institute, Palo Alto, CA (to be published).

may no longer be negligible. The development and validation of models to describe the failure of flawed steam generator tubes at high temperatures was a major objective of the activity described in this report. The tests and analyses presented in this report do not attempt to accurately simulate any particular severe-accident scenario; rather, they are intended to provide tools that can be used to determine failure under a broad range of pressure and temperature histories.

2 Analytical Failure Models

There is extensive literature¹⁻⁶ on the development and validation of analytical models to describe the behavior of flawed tubes at normal reactor operating temperatures (288–320°C). These models and data can be used to analyze the potential for failure during design-basis accidents, during which the temperature of the steam generator tubing is <350°C. In this temperature range, creep effects are negligible in Alloy 600. However, in postulated severe accidents, much higher temperatures are possible. Plastic deformation is likely to be much more extensive at these higher temperatures than at normal reactor operating temperatures, and creep effects can no longer be neglected. Until recently, no test data or validated models were available to predict the failure of flawed tubes at temperatures associated with postulated severe accidents. Therefore, the NRC initiated a program at Argonne National Laboratory (ANL) to generate failure data on flawed and unflawed steam generator tubes at high temperatures. Although these tests were carried out to failure, such failure may not occur in a real steam generator because the surge-line nozzle and depressurization of the system may fail. The purpose of these tests was to help develop models that would predict failure of steam generator tubes under given severe-accident conditions. The tests were therefore carried out to failure under various pressure and temperature conditions (not necessarily simulating any particular severe-accident scenario) to provide a broad data base for developing such models. A creep rupture model for predicting failure of the remaining ligament of a part-through crack has been developed and validated by tests on tubes that contain both axial and circumferential flaws.

All of the analytical models discussed in this report are applicable to steam generator tubes that contain a single dominant crack. Although some limited analytical and experimental studies of failure of tubes that contain multiple cracks have been performed, current practice generally applies the existing models to a single enveloping crack.

2.1 Low-Temperature Failure of Tubes

2.1.1 Unflawed Tubes

The failure pressure for defect-free straight tubing of inner radius R and wall thickness h is given by

$$P_b = \bar{\sigma} \text{Log} \left(1 + \frac{h}{R} \right), \quad (1a)$$

which for small values of h/R reduces to

$$P_b = \frac{\bar{\sigma}h}{R_m}, \quad (1b)$$

where R_m is the mean radius and $\bar{\sigma}$ is the flow stress, generally defined in terms of the yield (σ_y) and ultimate (σ_u) strengths as

$$\bar{\sigma} = k(\sigma_y + \sigma_u). \quad (1c)$$

Experimentally determined values of k usually vary between 0.5 and 0.6.

Room-Temperature Failure Properties of Unflawed Alloy 600 Tubing

Based on 100 failure tests on Alloy 600 and Alloy 690 steam generator tubing, a value of $k = 0.58$ was recommended by Flesch and Cochet.⁷ Values of k (Table 1) calculated from failure test results on 19.1-mm (3/4-in.)-diam and 22.2-mm (7/8-in.)-diam Alloy 600 tubes conducted at ANL and Pacific Northwest National Laboratory (PNNL)⁶ are slightly smaller. However, inasmuch as our primary interest in this report is the prediction of failure of Alloy 600 tubes at high temperatures ($>700^\circ\text{C}$), a regime where this material shows very little strain hardening in tensile tests, selecting a value of $k > 0.5$ leads to unrealistic values for the flow stress. Therefore, to define flow stress at high temperatures, k was chosen to be 0.5.

2.1.2 Tubes with Axial Cracks

Through-Wall Axial Cracks

The most widely used model for predicting the pressure that is necessary to cause unstable ductile (plastic collapse) failure of tubing with a through-wall axial crack, P_{cr} , was originally proposed by Hahn et al.¹ and later modified by Erdogan.²

$$P_{cr} = \frac{\bar{\sigma}h}{mR_m} = \frac{P_b}{m}, \quad (2a)$$

where

$$m = 0.614 + 0.481\lambda + 0.386 \exp(-1.25\lambda), \quad (2b)$$

$$\lambda = \left[12(1 - \nu^2) \right]^{1/4} \frac{c}{\sqrt{R_m h}} = \frac{1.82 c}{\sqrt{R_m h}}, \quad (2c)$$

p_b is the failure pressure of an unflawed tube (Eq. 1b), and $2c$ is the axial crack length.

Part-Through Axial Cracks

For part-through axial cracks, the pressure required to fail the remaining ligament can be calculated from an empirical equation (referred to as the Battelle Columbus Laboratories [BCL] equation) reported by Kiefner et al.⁴

$$p_{sc} = \frac{\bar{\sigma}h}{m_p R_m} = \frac{p_b}{m_p}, \quad (3a)$$

where

$$m_p = \frac{1 - \frac{a}{h}}{1 - \frac{mh}{a}}, \quad (3b)$$

and a is the crack depth. We emphasize that Eq. 3a gives only the pressure required to fail the remaining ligament. The stability of the resulting through-wall crack can be analyzed by solving Eq. 2a. If $p_{cr} > p_{sc}$, the through-wall crack is stable. Although the crack will leak, it will not increase in length without a further increase in pressure. If $p_{cr} < p_{sc}$, the resulting crack will be unstable and will rapidly increase in length without any additional increase in pressure.

Under the auspices of an NRC-sponsored steam generator integrity program, PNNL⁶ conducted a series of tests on tubes that contained part-through axial slots. Based on these tests, PNNL⁶ developed an empirical equation (referred to as the PNNL equation) for the failure pressure of a tube that contains a part-through axial crack. The equation is of the same form as Eq. 3a but Eq. 3b is replaced by

$$m_p = \left[1 - \frac{a}{h} + \frac{a}{h} \exp(-0.41\lambda) \right]^{-1}. \quad (4a)$$

Ellison et al.⁸ reanalyzed the PNNL tube test data and proposed that the value of k should be taken as 0.5973 and that Eq. 3a should be modified to give

$$m_p = \left[1 - \frac{a}{h} + \frac{a}{h} \exp(-0.51\lambda) \right]^{-1}. \quad (4b)$$

(referred to as the Idaho National Engineering and Environmental Laboratory [INEEL] equation).

Based on tube failure tests, Flesch and Cochet⁵, at Electricité de France (EDF) recommended the use of Eqs. 3a and 3b for flaw depths $>85\%$ of the wall thickness. However, to reduce the degree of conservatism, they used σ_u instead of $\bar{\sigma}$. To predict failure of the remaining ligament by plastic instability of tubes with flaw depths between 20 and 85% of wall thickness, they recommended replacing Eq. 3b by the following empirical equation (referred to as the EDF equation):

$$m_p = \left(1 - \frac{\frac{c}{h} \frac{a}{h}}{1 + \frac{c}{h}} \right)^{-1} \text{ for } 0.2 < a/h < 0.85, \quad (5a)$$

and

$$m_p = \frac{1 - \frac{a}{mh} \frac{\bar{\sigma}}{\sigma_u}}{1 - \frac{a}{h}} \text{ for } a/h > 0.85. \quad (5b)$$

Improved Correlation for m_p . As the crack depth approaches 100% of the wall thickness (i.e., $a/h = 1$), Eqs. 3a and 3b and 5b predict that p_{sc} approaches 0, whereas Eqs. 4a and 4b do not predict that p_{sc} approaches 0, i.e., for short, deep cracks, they predict a higher pressure for ligament failure than do Eqs. 3a-3b. Eventually, Eqs. 4a and 4b should become unconservative for very deep cracks. On the other hand, Eq. 3b tends to be overly conservative for short and deep cracks. Therefore, the PNNL tube test data were re-analyzed (including re-measurement of the flaws by posttest fractography) by ANL and it was proposed that Eq. 3b be modified as follows (referred to as the ANL equation):

$$m_p = \frac{1 - \alpha \frac{a}{mh}}{1 - \frac{a}{h}}, \quad (5c)$$

where

$$\alpha = 1 + 0.9 \left(\frac{a}{h} \right)^2 \left(1 - \frac{1}{m} \right).$$

Except for short and deep cracks, Eq. 5c predicts failure pressures similar to those predicted by Eq. 3b.

The relative errors of the values of m_p predicted by the various models and the PNNL failure test results⁶ are summarized in Table 2. The errors are expressed in terms of the RMS errors for the entire test series and in terms of the RMS errors associated with a range of crack depths. Roughly equivalent numbers of tests were performed in each crack depth range. The EDF correlation is also included for comparison, but because it involves a material characteristic (the ultimate tensile stress) as well as geometric factors, it would be an unsatisfactory choice to use for high temperatures, where creep damage is significant.

The errors were smaller for unflawed tubes than for flawed tubes, even when the cracks were relatively shallow. The errors of the PNNL and BCL models seem to be significantly larger for deep ($a/h > 0.75$) cracks than for shallower cracks. The INEEL and the ANL models exhibit a more uniform distribution of errors with flaw depth. Although the errors associated with the predictions from Eq. 5c (with $k = 0.5$) for the PNNL data set are slightly smaller than those associated with the other correlations, the INEEL correlation describes the PNNL tests almost as well.

The values of the stress magnification factor m_p computed by the various equations are shown in Figs. 1a and 1b. Note that, although the values of m_p as computed by the various equations are within 20-30% of each other for shallow cracks ($a/h = 0.5$), they can differ by as much as a factor of 2 for short, deep cracks (≈ 0.25 in., $a/h = 0.9$). Failure tests on tubes that contain deep cracks (to be discussed later) have shown that the m_p values are more in agreement with the ANL equation (Eq. 5c) than the BCL equation (Eq. 3b). To verify this analytically, detailed elastoplastic finite-element analyses were conducted for a 22.2-mm (7/8-in.)-diam tube with a 25-mm (1-in.)-long, 50% deep axial crack, and for a 6-mm (0.25-in.)-long, 90% deep axial crack subjected to rapidly increasing internal pressure at 300 and 750°C. The stress-strain curves at these temperatures are shown in Fig. 2a. The results presented in Fig. 2b show that the maximum hoop stress magnification in the ligament for the shallower crack is independent of the stress-strain curve of the material. Furthermore, the hoop stress magnification factor (defined as the ratio of the average hoop stress in the ligament and the average hoop stress in an unflawed tube) changes very little with internal pressure and its variation with crack depth is more in agreement with the ANL equation than with the BCL equation.

2.1.3 Circumferential Cracks

Through-Wall Circumferential Cracks

Failure loads of tubes with a single circumferential crack critically depend on the bending constraint imposed externally on the tubes. The two extreme cases are the free-bending case and the fully constrained case. In reality, steam generator tubes are partially constrained against bending by tube support plates.

Free Bending Case. Failure loads for tubing with through-wall circumferential cracks can also be calculated by plastic limit load (collapse) analyses, which were based on earlier work by Kanninen et al.⁹ and are described by Ranganath and Mehta.¹⁰ For an unconstrained (free-to-bend) tube with a through-wall crack of angular length 2θ and no applied primary bending stress, the critical failure pressure is

$$p_{cr} = \frac{2\bar{\sigma}h}{R_m} \left(1 - \frac{\theta}{\pi} - \frac{2\beta}{\pi} \right), \quad (6a)$$

where the angular location of the neutral axis is given by

$$\beta = \sin^{-1} \left(\frac{\sin \theta}{2} \right). \quad (6b)$$

Fully Constrained Case. Equation 6a is applicable to one extreme case, where the tube is completely free to bend. In the opposite extreme case of total constraint against bending, a criterion based on maximum shear stress in the net section, as proposed by Cochet et al.,¹¹ can be used to calculate the instability limit pressure:

$$p_{cr} = \frac{2(\gamma^2 - 1)(\pi - \theta)\bar{\sigma}}{2\pi + (\pi - \theta)(\gamma^2 - 1)}, \quad (7a)$$

where

$$\gamma = \frac{R_o}{R_i}. \quad (7b)$$

The following thin-shell, uniaxial approximation to Eq. 7a is often used to predict failure of steam generator tubes that are fully constrained against bending:

$$p_{cr} = \frac{2\bar{\sigma}h}{R_m} \left(1 - \frac{\theta}{\pi}\right). \quad (7c)$$

In reality, the tube support plates offer significant but not total restraint against bending, a circumstance that tends to increase the failure pressure to somewhere between the pressure those predicted by Eqs. 6a and 7a (or 7c).

Partially Constrained Case. Hernalsteen¹² has developed a semi-empirical approach to taking into account the stiffening effects of the tube support plate on the failure pressure of a steam generator tube that contains a circumferential crack at the top of the tube sheet. By conducting several pressure tests of circumferentially flawed steam generator tube/tube sheet/tube support plate assemblies and measuring the failure pressures p_{fail} and the restraining loads (thus the bending moments M_{fail} at the tube sheet) at the tube support plate at failure, he developed an empirical correlation between the stress index K and the elastic rotational stiffness of the tube S , where

$$K = \frac{\left(\frac{M_{fail}}{\pi R_m^2 h} + \frac{p_{fail} R_m}{2h} \right)}{\bar{\sigma}} \quad (8a)$$

and

$$S = \frac{M}{EI y'}, \quad (8b)$$

where EI is the flexural stiffness of the tube and M and y' are the bending moment and rotation at the tube sheet, respectively. Based on this correlation, Hernalsteen determined the value of K from the value of S that corresponds to the steam generator tube configuration under consideration and computed the failure pressure from Eq. 8a. Typical values of K ranged from 0.6 for a 2-3-m span to 0.9 for a 3-4-cm span between the tube sheet and the tube support plate.

Finite Element Analysis. A series of elastoplastic finite-element analyses were conducted at ANL to determine the effects of a lateral constraint on the failure pressure of a tube with a single through-wall circumferential crack. Initially, two tubes with 180° and 240° through-wall circumferential cracks were analyzed. Typical finite-element models and displacements for the cases of free-bending and bending with a lateral support are shown in Figs. 3a and 3b, respectively. Note that in the free-bending case, the beam bends almost like a rigid body about a hinge at

the cracked section. Figure 4a shows the variations of the maximum rotation with the normalized failure pressures for 180° and 240° circumferential through-wall cracks. Both cracked tubes reach maximum pressures at a rotation of $\approx 10^\circ$, which is in agreement with the experimental observation of Hernalsteen,¹² that tubes appear to fail at a fixed rotation of the cracked section. Figure 4b shows available failure pressure data reported by Cochet and Flesch¹³ and predictions obtained from the simple-beam free-bending and fully-constrained models (Eqs. 6a and 7c), together with results from finite-element analysis. The simple free-bending model underpredicts both the test failure pressures and the finite-element results slightly. Analysis of a transversely supported tube (to simulate the tube support plate) with a 240° crack (indicated by the open circle with an arrow) showed that the maximum pressure capability is increased significantly over that of a free-bending tube (Fig. 4b). The critical pressure for a span of 0.67 m (26 in.) appears to be much closer to the fully constrained case than to the free-bending case.

Part-Through Circumferential Cracks

Consider a tube with mean radius R_m and wall thickness h , that contains either two symmetrical part-through circumferential cracks (Fig. 5a) or a single part-through circumferential crack (Fig. 5b) of angular length 2θ and depth a . At low temperatures, where creep effects are negligible, the ligament failure pressure (p_{sc}) is generally expressed in terms of a stress magnification factor (m_p) by equating the magnified axial stress in the ligament to the flow stress,

$$p_{sc} = \frac{2\bar{\sigma}h}{R_m m_p}. \quad (9)$$

Failure pressure for circumferentially cracked tubes, i.e., the value of the magnification factor m_p , depends strongly on the degree of restraint the tubes are subjected to against bending. The two extreme cases, i.e., the free-bending case and the completely constrained case are relatively easy to analyze. Generally, steam generator tubes are sufficiently constrained laterally that the failure loads are expected to be much closer to the completely constrained case than the free-bending case. The discussion here assumes that the tubes are either completely constrained or are completely free to bend.

Fully Constrained Case. The fully constrained case would also include the case for an unrestrained tube that contains two symmetrical cracks (Fig. 5a). In this case, the whole section that contains the crack (or cracks) is subjected to axial tensile stress, with the ligament (or ligaments) being subjected to stress intensification. If the average stress in the ligament (or ligaments) is expressed as $1/m$ times the average stress in the rest of the section that contains the crack (or cracks), the average ligament axial stress (σ_{lig}) can be calculated from a simple equilibrium of axial forces,

$$\sigma_{lig} = \frac{pR_m}{2h} \frac{1}{\left[m + \left(\frac{n\theta}{\pi} \right) \left(1 - \frac{a}{h} - m \right) \right]}. \quad (10a)$$

where

$$n = \begin{cases} 1 & \text{for a single crack} \\ 2 & \text{for two symmetrical cracks.} \end{cases}$$

If we define m_p as the ratio of the average ligament axial stress and the average axial stress in the unflawed tube, m_p is given by

$$m_p = \frac{1}{\left[m + \left(\frac{n\theta}{\pi} \right) \left(1 - \frac{a}{h} - m \right) \right]}. \quad (10b)$$

Originally, Kurihara et al.¹⁴ used the empirically obtained expression

$$m = 1 - \left(\frac{a}{h} \right)^\kappa \left(\frac{n\theta}{\pi} \right)^\mu, \quad (11)$$

(with $n = 1$).

Although Kurihara et al.¹⁴ recommended values of $\kappa = 2$ and $\mu = 0.2$ for the exponents, the results are almost indistinguishable from those obtained by using $\kappa = 3$ and $\mu = 0.3$. Because the behavior of Eq. 11 is not correct (i.e., m does not tend to 0) when a/h tends to 1 for all θ , it was modified to have the same form as in the case of axial cracks, i.e.,

$$m = \frac{1 - \frac{a}{h}}{1 - \frac{a}{Nh}}, \quad (12a)$$

where

$$N = 1 + \lambda \left(\frac{n\theta}{\pi} \right)^\gamma \quad (12b)$$

and λ and γ are fitting parameters.

Both the failure modes and moments of the original set of test data from four-point bending failure tests on pressurized part-through circumferentially cracked Type 304 stainless steel pipes at room temperature (used by Kurihara et al.¹⁴) can be predicted somewhat better by the current model with $\lambda = 0.2$ and $\gamma = 0.2$ and by defining the flow stress as $0.55[\sigma_y + \sigma_u]$, see Eq. 1c than by the Kurihara model (see Figs. 6a and b).

Free-Bending Case. Figure 5b shows that, in the free-bending case, part of the section that contains the crack will, in general, be subjected to compressive stress. As a result, Eq. 10a must be replaced by

$$\sigma_{lig} = \frac{pR_m}{2h} \frac{1}{\left[m \left(1 - \frac{2\beta}{\pi} \right) + \left(\frac{n\theta}{\pi} \right) \left(1 - \frac{a}{h} - m \right) \right]}, \quad (13a)$$

where the angle β that defines the location of the neutral axis is given by

$$\beta = \sin^{-1} \left\{ \frac{\sin \theta}{2} \left[1 - \frac{1}{m} \left(1 - \frac{a}{h} \right) \right] \right\} \text{ for } \beta \leq \pi - \theta, \quad (13b)$$

and Eq. (10b) has to be replaced by

$$m_p = \frac{1}{\left[m \left(1 - \frac{2\beta}{\pi} \right) + \left(\frac{n\theta}{\pi} \right) \left(1 - \frac{a}{h} - m \right) \right]}, \quad (13c)$$

with m and N defined by Eqs. 12a and 12b, respectively.

2.2 High-Temperature Failure of Tubes

The behavior of flawed steam generator tubing during severe accidents has recently been considered in reports by INEEL* and EPRI.† In these reports, the failure of unflawed tubing and other components, such as the surge-line nozzle, is described in terms of creep damage failure. Both analyses assume that failure of flawed steam generator tubing in severe accidents can be described in terms of the models described by Eqs. 2a and 3a by taking the flow stress to be a function of temperature. With this assumption, the failure pressure of a flawed tube depends only on the flaw geometry and temperature and is independent of the loading rate or the detailed time/temperature history.

Although the flow stress model is a straightforward extension of a model that has been well verified at normal operating temperatures, the use of such a model at high temperatures is subject to conceptual difficulties. For example, although Alloy 600 shows little strain hardening at high temperatures, its flow stress is a function of not only temperature but also strain rate, as shown schematically in Fig. 7. Intuitively, failure pressure or temperature would be expected to be truly rate-independent if the pressure or temperature ramps are sufficiently rapid so that there is insufficient time for creep to influence the deformation or damage of the ligament and the crack tip ligament fails by plastic instability when the ligament stress exceeds the maximum load-carrying capacity of the material ($\sigma_{\text{ligament}} = \sigma^*$ in Fig. 7). At the other extreme, if the pressure or temperature ramps are sufficiently slow (in the limit, a constant-pressure or constant-temperature hold), failure should be controlled by creep processes and a failure model based on creep rupture would be more appropriate.

* P. G. Ellison, L. W. Ward, C. Dobbe, S. A. Chavez, C. L. Atwood, C. L. Smith, L. M. Wolfram, J. L. Jones, L. N. Haney, and W. J. Reece, The Risk Significance of Induced Steam Generator Tube Rupture, INEL-95/0641, Rev. 1 (Draft), Lockheed Martin Idaho Technologies, Inc., Idaho National Engineering and Environmental Laboratory, December 15, 1995.

† E. L. Fuller, M. A. Kenton, M. Epstein, R. E. Henry, and N. G. Cofie, Risks from Severe Accidents Involving Steam Generator Tube Leaks or Ruptures, EPRI TR-106194, Electric Power Research Institute, Palo Alto, CA (to be published).

In loading histories at intermediate rates, the damage processes are more complex and difficult to analyze. Conceptually, it is possible to include rate effects within the framework of a flow stress model by developing a constitutive equation that expresses the flow stress as a function of both strain rate and temperature. Several such constitutive relationships based on the so-called equation-of-state theory, are available (for example see Ref. 15). However, in addition to being quite complex, these relationships are not easily amenable to the problem of predicting failure of steam generator tubes, particularly tubes that contain flaws and are subjected to typical temperature and pressure histories expected during severe accidents. In this report, the term flow stress models is used exclusively to denote simple rate-independent flow stress models that have been used successfully at low temperatures.

Recent tests conducted at ANL have shown that pressure and temperature ramp rates significantly influence failure pressure (Fig. 8a) and failure temperature (Fig. 8b). Therefore, a creep rupture model for predicting failure of flawed and unflawed tubes was developed. For the high-temperature tests conducted at ANL under various loading histories with temperature ramp rates of interest in severe-accident analyses, predictions based on this model are in much better agreement with the test results than predictions based on flow stress models. However, for completeness, the flow stress models are also discussed in this report.

2.2.1 Assumptions in High-Temperature Models

Consider a tube with a part-through flaw subjected to a temperature history $T(t)$ and nominal hoop stress history $\sigma(t)$. To analyze the ligament failure behavior of a tube under such a general loading history, both the flow stress and the creep rupture models make the following assumptions.

(a) The failure (ligament) time and temperature of a flawed tube are the same as those of an unflawed tube subjected to a nominal hoop stress history $m_p\sigma(t)$ and the same temperature history $T(t)$.

(b) The values of the magnification factor m_p , determined semiempirically (see Eqs. 3b, 4a, 4b, 5a, 5b and 5c, for axial cracks and Eqs. 10b and 13c for circumferential cracks) and validated by failure tests of flawed tubes at low temperatures are also applicable at high temperatures.

These assumptions can be shown to be valid for certain classes of creep and plasticity problems.¹⁶ They are not strictly valid for the problem considered here, but the test program at ANL has shown that they can provide a reasonable approximation.

2.2.2 Flow Stress Models

In the flow stress models, it is assumed that for any arbitrary history of hoop stress $\sigma(t)$ and temperature $T(t)$, failure occurs at a temperature T and hoop stress σ

whenever the following failure equation is satisfied, independent of stress/temperature history:

$$\sigma = \frac{\overline{\sigma(T)}}{m_p}, \quad (14)$$

where $\overline{\sigma(T)}$ is the flow stress at temperature T and m_p is a stress magnification factor that accounts for the crack.

Flow Stress Data for Alloy 600

To apply the flow stress models rigorously to predict the failure temperatures of steam generator tubes, we need the tensile properties of Alloy 600 tubes in the hoop direction as functions of temperature. Although fairly extensive tensile-property data for tubing are available at room temperature,⁶ only limited data are currently available at higher temperatures. Tensile properties of Alloy 600 rods as a function of temperature were reported by Rempe et al.¹⁷ Ellison et al.⁸ expressed the sum of the yield and ultimate tensile strengths of Alloy 600 bars (in MPa) as a function of temperature as follows:

$$\sigma_y + \sigma_u = \begin{cases} 564.4 - 0.4546T + 1.5055 \times 10^{-3}T^2 - 1.9907 \times 10^{-6}T^3 & \text{for } 20^\circ\text{C} \leq T \leq 727^\circ\text{C} \\ 4308.9 - 11.381T + 1.030 \times 10^{-2}T^2 - 3.1734 \times 10^{-6}T^3 & \text{for } 727^\circ\text{C} < T \leq 1100^\circ\text{C} \end{cases} \quad (15a)$$

To estimate the variation of the flow stress of Alloy 600 tubes with temperature, they multiplied Eq. 15a by a factor of 0.9, which is the factor by which the sum of the yield and ultimate tensile strengths of Alloy 600 bars¹⁷ at room temperature must be multiplied to obtain the sum of the yield and ultimate tensile strengths for Alloy 600 tubes at room temperature.^{6,8}

Tensile data on hot-rolled Alloy 600 bars as a function of temperature are also reported in Ref. [18]. In this case, the sum of the yield and ultimate tensile strengths (in MPa) of hot-rolled Alloy 600 bars was given as a function of temperature by the equation.

$$\sigma_y + \sigma_u = 1780 - 9.84T + 0.0352T^2 - 5.08 \times 10^{-5}T^3 + 2.4 \times 10^{-8}T^4 \quad \text{for } 200^\circ\text{C} \leq T \leq 870^\circ\text{C}. \quad (15b)$$

Flow stresses for Alloy 600 computed from the above data (with $k = 0.5$), together with flow stresses from various other sources, are plotted in Fig. 9. Most of the tests in Fig. 9 were conducted under stroke control at a nominal strain rate of 34%/min. Data from room-temperature tensile tests on the tubing being tested at ANL are also shown in Fig. 9. Above 600°C, the flow stress decreases markedly with temperature. Note from Fig. 9 that although there may be a wide variation in flow stress at low temperatures, the heat-to-heat and product form variations in the flow stress diminish rapidly with increasing temperature. Therefore, the INEEL flow stress curve, which covers the widest range of temperature, is used to predict failure.

2.2.3 Creep Rupture Model

Creep failure of an unflawed tube under a varying stress and temperature history can be predicted by a relatively straightforward analysis,¹⁶ based on a linear time/fraction damage rule, such as that used in the ASME Code, Section III, as follows:

$$\int_0^{t_f} \frac{dt}{t_R(T, \sigma)} = 1, \quad (16a)$$

where t_R is the time to creep rupture for a uniaxial specimen under a stress σ and temperature T , both of which may be functions of time; and t_f is the time to failure of the tube.

A rigorous analysis of flawed tubes under a similar loading would be very complex. Therefore, we extended the creep failure model to flawed tubing by using the assumptions referred to earlier, i.e., we assumed that failure can be predicted from the following equation:

$$\int_0^{t_f} \frac{dt}{t_R(T, m_p \sigma)} = 1. \quad (16b)$$

Creep Rupture Properties of Alloy 600

To predict failure under severe-accident conditions with the creep rupture model, creep rupture properties (particularly at short lives) of Alloy 600 tubes in the hoop direction are needed. The available literature data on the creep rupture properties of Alloy 600 were reviewed. A compilation of the available data is shown in Fig. 10 in terms of the Larson-Miller parameter (LMP) and the stress. These data include both cold-worked and annealed materials. However, an examination of the data revealed no apparent clustering of the data due to differing thermomechanical treatment, and data corresponding to both types of materials were included in the fits. A least-squares bilinear best fit is shown in Fig. 10, along with estimated 95% confidence limits. The equations for best-fit curves for the LMP P_{lm} are

$$\begin{aligned} \log_{10} \sigma &= 4.31 \pm 0.10 - 1.43 \times 10^{-4} P_{lm} & \sigma \leq 39.3 \text{ MPa} \\ \log_{10} \sigma &= 5.03 \pm 0.13 - 1.81 \times 10^{-4} P_{lm} & \sigma > 39.3 \text{ MPa}, \end{aligned} \quad (17a)$$

where the time to rupture t_R is then given by

$$t_R = 10^{\frac{P_{lm}}{T} - 15}, \quad (17b)$$

with t_R in h and T in K.

For actual calculations, the inverse of the expressions is more useful. In English units,

$$\begin{aligned} P_{lm} &= (24.3 \pm 0.7 - 3.0 \ln \sigma) \times 10^3 & \sigma \leq 5.7 \text{ ksi} \\ P_{lm} &= (23.2 \pm 0.7 - 2.4 \ln \sigma) \times 10^3 & \sigma > 5.7 \text{ ksi}, \end{aligned} \quad (17c)$$

where $\ln \sigma$ is the natural log. The uncertainty in the LMP reflected in Eqs. 17a and 17c is due primarily to differences in heat-to-heat behavior of materials.

The "breakpoint" in the bilinear fit occurs at a stress that is relatively low when compared with breakpoints of interest in steam generator tube failures. Even the nominal stress in a steam generator tube with the secondary depressurized is >130 MPa (19 ksi), i.e., $\log_{10} \sigma \approx 2.1$. In the crack ligament, the stress is even higher. Hence, for these applications, only Eq. 17c is actually used. The variance has been assumed to be the same for all stress levels. This is unlikely to be true. We expect that the variance will be less at the higher stresses of actual interest, but, as noted, very few data are available at higher stresses and it is difficult to quantify this judgment. The bounds predicted by Eq. 17c are expected to be conservative at the stress levels typical of flawed tubes.

In Fig. 11, the Larson-Miller correlation currently used in the Severe Core Damage Analysis Program (SCDAP) is shown along with existing creep data; it is a reasonable fit to the data, but there clearly is some change in slope that is not reflected in the correlation. In particular, the slope departs from the data at the high stress levels (low LMP values) of interest in the present analyses.

To establish the applicability of Eqs. 17b and 17c to our tubing material, constant-pressure creep-rupture tests were conducted on unflawed 22.2-mm (7/8-in.)-diam tubes under both isothermal and constant-temperature ramp loading. The experimental results are plotted in Fig. 12 against predicted times to rupture, obtained with Eqs. 17b and 17c. In all cases, the predicted rupture lives are well within a factor of 2 of the experimental lives, indicating that the above Larson-Miller representation of the data is adequate for our material.

3 Failure Tests on Flawed and Unflawed Alloy 600 Tubes

3.1 Description of High-Temperature Test Facility

A high-temperature facility to determine the failure temperatures and pressures of unflawed and flawed tubes at very high temperatures was constructed as part of the present program. A schematic drawing of the test facility is shown in Fig. 13. In simplest terms, it consists of a furnace into which is placed a length of steam generator tubing that is closed off at one end and connected to a high-pressure nitrogen gas bottle through suitable valving at the other end. During a test, the specimen is first stabilized at a nominal steam generator operating temperature of 300°C and a fixed internal pressure. Then, the temperature is ramped up at a predetermined rate until specimen failure occurs. Because of the flow limitation of the system, the specimens are depressurized immediately upon ligament failure either by rupture or by pinhole leak.

Constant pressure is maintained during the test by a back-pressure regulator, and the pressure is monitored by a pressure transducer. A constant-leak valve is installed in a position behind the back-pressure regulator as an alternate means of gas pressure control in combination with the pressure regulator on the gas cylinder.

A high-flow cutoff valve is also installed in the gas line to automatically stop the flow of nitrogen under high-flow tube-failure conditions. This prevents emptying of the gas bottle when specimen failure occurs during unattended long-term tests. The temperature of the furnace and specimen is controlled with a programmable temperature controller, and both the pressure and temperature outputs are recorded.

The test specimen is ≈ 0.45 m (18 in.) long, and the furnace is ≈ 0.33 m (13 in.) long, with a uniform temperature zone ≈ 80 mm (3 in.) long near the center. Welded end plugs close each end of the specimen, and these end plugs are outside of the furnace during testing to avoid failure at the welds. A tapped hole in one end plug permits connection to the gas pressurization system, and a solid mandrel inside the specimen displaces most of the pressurized gas volume to reduce stored energy. The furnace is fitted with heavy metal-plate blast shields at each end to contain any projectiles that may be produced during a tube failure, and the furnace is also fitted with a heavy-wall SS tube to prevent projectile damage to the furnace elements.

3.1.1 Facility Checkout

Temperature measurements were conducted on an instrumented dummy specimen to determine the temperature profiles along the length and through the thickness of the specimen. Under isothermal conditions at 700°C , the temperature along the length of the specimen was found to vary $\approx 6^{\circ}\text{C}$ over a 50-mm (2-in.)-long central portion that corresponds to the maximum flaw length to be tested. During a 400°C/h constant-temperature ramp, the temperature gradient through the thickness was $\approx 8^{\circ}\text{C}$, with the temperature of the inner surface being lower, as expected.

Temperature profile tests were subsequently conducted on a dummy specimen with the simulated INEEL temperature ramp shown in Fig. 14a. The programming of the temperature controller was adjusted to optimize the temperature response of the specimen, and minor modifications were made to thermocouple placement and specimen positioning in the furnace. Because of these changes, the temperature of the specimen followed the desired temperature ramp very closely, and the temperature gradient over the central 51 mm (2 in.) of the specimen was reduced to $\approx 4^{\circ}\text{C}$. The temperature gradient through the specimen thickness was $\approx 6^{\circ}\text{C}$ during the 7.50°C/min ramp rate, and less for the slower rates.

Finally, a temperature profile test was conducted with the EPRI temperature ramp shown in Fig. 14b. The temperature of the specimen followed the desired temperature history very closely during the initial ramp rate of 5.37°C/min . However, because of thermal-inertia effects, the specimen could not precisely follow the subsequent higher rate ramps of 10.60, 28.24, and $14.80^{\circ}\text{C/min}$ indicated in Fig. 14b. Instead, the specimen heated at an approximately uniform rate of ≈ 16 - 18°C/min over this temperature interval. This rate compares favorably with the average desired heating rate of 17.3°C/min for these three ramp steps. The temperature overshoot at the end of the last ramp step (at 667°C) was $\approx 8^{\circ}\text{C}$, and the

temperature gradient through the thickness of the specimen did not exceed 10°C during the fastest ramp.

3.2 Tests on Specimens with Axial Cracks

Using three types of loading histories, a total of 73 tests designed to validate the ANL creep rupture model, in the high-temperature test facility were conducted on 19.1-mm (3/4-in.)- and 22.2-mm (7/8-in.)-diam Alloy 600 tubes that contained a variety of electro-discharge machined (EDM) flaws. Such flaws are typically 0.008 in. wide and are not as sharp as real cracks, but previous tests at lower temperatures have shown that the failure pressures of specimens with corrosion cracks are at most $\approx 10\%$ less than those predicted by failure correlations developed from specimens with machined flaws.⁶ At a higher temperature, the effect of the crack tip geometry would be expected to be even less significant.

3.2.1 Measurement of Axial Flaw Depth

The flaw depth and length are critical parameters in calculating expected failure pressures of the tubes, and these dimensions must be determined as precisely as possible. The accurate determination of the flaw depths in particular poses some difficulties. Four methods were developed to measure the depths of the machined flaws. Two of these methods are applicable to specimens prior to testing, one is performed after testing, and the fourth method is destructive and thereby precludes subsequent pressure testing of the specimen.

The first technique used to measure flaw depth was posttest fractography. In this method, the fracture surfaces of the failed specimen are photographed at a known magnification after the test, and the contrast between the machined portion of the fracture surface and the region of subsequent ductile fracture in the photograph permits a reasonably accurate determination of flaw depth. This method assumes that the premachined portion of the tube wall does not thin appreciably during the test. This appears to be a reasonable assumption, inasmuch as this portion of the wall cannot support a hoop stress, and the axial deformations observed during pressure testing are on the order of a few percent or less.

The second technique used to measure flaw depth was replication of the pre-machined flaws. In this technique, a plastic replica was made of the flawed region of the specimen prior to testing, and the height of this replica, which corresponds to the depth of the flaw, was then determined by optical microscopy. This technique offers the advantage that the flaw geometry is preserved in time, and reexamination of the flaw is possible even after the test is completed. Accurate measurements require that the replicas do not stretch or tear during extraction from the sample.

A third technique, developed later in the present test series, is to directly measure the flaw depth prior to testing with a traveling optical microscope that gives a digital readout of the x, y, and z positions of the objective lens. The flaw depth can be measured by focusing first on the outer surface of the specimen and then on the bottom of the machined flaw. The flaw depth corresponds to the movement of the

microscope objective between these two steps; readings accurate to $\approx \pm 2,500 \text{ } \mu\text{m}$ (± 0.0001) are possible. This technique can also be used to obtain highly accurate measurements of flaw length. The principal disadvantage of the technique is the fact that the measurements cannot be rechecked after the specimen has been tested.

A fourth technique for determining flaw depth is destructive metallography of the flawed tube. The tube is simply sectioned through the flaw, and flaw depth at that position is determined from a photomicrograph. The technique is also highly accurate, but it suffers from the obvious disadvantage that the sectioned tube cannot be subsequently pressure-tested. In addition, the depth is determined at only one position per section along the flaw length.

All four of these techniques have been employed in the present program, and the results are listed in Table 3. Posttest fractography was the first technique used, and data from this procedure are available for all of the tubes. The flaw replication and pretest direct optical measurement techniques were developed later in the program, and values from the early tests are not available. Because both of these techniques are applied prior to the test, it is not possible to make these measurements on failed specimens. Only the pretest measurement by optical microscopy (Technique A in Table 3) permitted accurate determination of flaw length. For those flaws to which this technique was not applied, the nominal flaw length is given. The EDM process is such that the length is not expected to vary significantly from the nominal value, and the observations from Technique A, i.e., pretest measurement by optical microscopy, bear this out.

The last four entries at the bottom of Table 3 represent flaw depth values obtained by three of the four techniques on the same set of flaws. The purpose of these measurements was to compare the flaw depth values determined by the various techniques. Four flaws of various depths were machined into a single 1.27-mm (0.050-in.)-thick tube by the usual procedure. The depth of the flaws along their length was then determined by pretest direct optical observation and by replication. The tube was then sectioned through each of the four flaws, and the flaw depth at the point of sectioning was measured metallographically. Because this sectioning made subsequent pressure testing impossible, posttest fractography values for the flaw depths could not be obtained. In addition, the depth measurement obtained by replication from the deepest flaw is not valid because the replica tore, apparently during extraction. This fact was not discovered until after the tube was sectioned, and so a second replica could not be made. As indicated in Table 3, aside from this invalid measurement, the agreement among the three techniques for these flaws was quite good. The largest variation in measured flaw depth was 0.02 mm (0.0009 in.), or $\approx 6\%$ variation between the values measured by pretest optical microscopy and posttest metallography.

Table 3 also provides information that can be used to compare the various techniques for tubes that were actually tested to failure. In general, the flaw depth values obtained by replication are slightly greater than those obtained by posttest fractography; the pretest microscopy determinations agree reasonably well with the values obtained by replication. Because the posttest fractography values represent

the only consistent set of values for all of the specimens, these are the flaw depth values that have been used in the analysis and modeling of the tests performed to date.

Data on the variation in depth along the length of the flaw are provided in Table 4 for the nine specimens that were characterized by pretest optical microscopy (Technique A). Observed depth values are reported at intervals of ≈ 6.4 mm (0.25 in.) as well as within ≈ 0.4 mm (0.015 in.) of each end of the flaw. For all of the measured flaws, the variation in depth is $25\text{ }\mu\text{m}$ (0.001 in.) or less over the central portion of the flaw. This variation represents an improvement over the $\approx 50\text{ }\mu\text{m}$ (0.002 in.) variability observed in posttest fractography measurements on specimens produced before improvements in flaw fabrication procedures were instituted. However, several flaws still show substantial variation in depth near the ends, with rounding of the corners indicated. The average values for flaw depth reported in Tables 3 and 4 for these specimens were obtained by ignoring the measurements at each end, because the depths at these locations were nonrepresentative in many cases.

3.2.2 Isothermal Failure Tests

Tests were conducted on flawed tubing by subjecting it to isothermal constant pressure loading. A summary of the tests is given in Table 5. The failure times predicted by using four values of m_p are plotted against experimental failure times in Fig. 15a for tubes that contained an $\approx 60\%$ -deep, 25-mm (1 in.)-long crack. Except for a test at 667°C , the predicted failure times predicted by the ANL m_p (Eq. 5c) are within a factor of 2 of the experimental lives of the specimens. The times predicted by using m_p values determined by the BCL (Eq. 3b) and EDF (Eq. 5a) equations are also reasonable, but those by the INEEL equation (Eq. 4b) are significantly off in most cases. It should be noted that the flow stress model is incapable of predicting time to failure for tests of this type and, in fact, would predict that none of the tubes should have failed.

Failure Tests on Specimens with Deep Flaws

As discussed earlier, some experimental evidence⁵ indicates that the numerical values of m_p computed by the BCL equation for short and deep cracks are too high for tests conducted at low temperatures, where a flow stress model is valid. Results from tests, summarized in Table 6 and shown in Fig. 15b, confirm that this is also true at high temperatures. All of the test specimens referred to in this figure contained cracks that were $\geq 90\%$ deep. The BCL equation grossly overestimates the damaging influence (i.e., underestimates the time to failure) of these cracks. The predictions obtained by using the INEEL and ANL equations are in better agreement with experimental results than those obtained from the BCL model. Results predicted by the EDF equation are not shown in Fig. 15b because, at high temperatures, they are essentially the same as those predicted by the BCL equation. At low temperatures the EDF model⁵ predicts a smaller m_p for cracks that are $> 85\%$ deep than the BCL model, because the flow stress in the BCL equation is replaced by the ultimate tensile strength (see Eq. 5b). However, there is little difference between the flow stress and the ultimate tensile strength at high temperatures

because strain hardening is greatly reduced. Overall, as in the low-temperature case, the ANL equation for m_p gave the best predictions for failure times of specimens with shallow as well as deep cracks and was used for the analysis of the high-temperature tests.

3.2.3 Pressure and Temperature Ramp Tests

To evaluate the effect of loading rates on failure conditions and to compare the predictive capabilities of the creep rupture and flow stress models, two additional types of tests were conducted. In the first type, the specimens were heated to a specified temperature and then pressurized isothermally at a constant pressure ramp until failure. In the second type, the specimens were first pressurized at low temperature and then, with the pressure held constant, they were subjected to a constant temperature ramp until failure.

A summary of all of these tests conducted to date is given in Tables 7 and 8. Results from both types of tests, plotted in Figs 16a and 16b, show that the loading rate (pressure or temperature), which is ignored in a simple flow stress model, significantly influences failure, even for unflawed tubes. Figure 17, a plot of the INEEL flow stress curve as well as "effective" flow stresses derived from the pressure ramp tests on flawed and unflawed tubes, clearly indicates that the flow stress model would grossly underpredict the failure pressures for the higher ramp rate tests if the INEEL flow stress curve were used. The failure pressures are close to those predicted by the creep rupture model; see Fig. 16a.

The pressures in the temperature ramp tests on flawed specimens were selected such that the product of m_p and the nominal hoop stresses were approximately equal for two crack geometries. Thus, the predicted failure temperatures for both geometries fall approximately on a single line for either the creep rupture or flow stress models, as shown in Fig. 16b. The experimental results are in much better agreement with the predictions of the creep rupture model and confirm that the effect of flaws on failure can be characterized by the m_p approach. Therefore, the creep rupture model can be expected to predict failure more reliably under varying temperature and pressure histories during severe accidents than a simple rate-independent flow stress model.

3.2.4 Tests under Simulated Severe-Accident Time/Temperature Histories

Tests were performed at ANL to determine the behavior of flawed tubes under time/temperature histories similar to those projected to occur under severe-accident conditions. The purpose of the tests was to further validate the creep rupture model to support its use to determine the time to failure of flawed tubes under projected time/temperature histories that could reach temperatures as high as 850°C.

In all of the tests, the internal pressure was held constant at 16.2 MPa (2350 psi). Tests were conducted on both 19.1-mm (3/4-in.)- and 22.2-mm (7/8-in.)-diam tubes with wall thicknesses of 1 mm (0.043 in.) and 1.3 mm (0.050 in.), respectively.

Four nominal flaw geometries, with axial lengths of 6 mm (0.25 in.), 25 mm (1 in.), and 50 mm (2 in.) and depths varying from 20% to 65% of wall thickness, were tested. Duplicate tests were run for all of the 22.2-mm (7/8-in.)-diam tube tests.

For the tests to date, two time/temperature histories have been considered. Both are based on preliminary analyses of an accident sequence that involves total station blackout (SBO) with a stuck-open steam generator secondary-side atmospheric dump valve, resulting in secondary-side depressurization and loss of feedwater. One time/temperature history, which is referred to as the "INEEL ramp," is based on a preliminary analysis by INEEL.* The other, referred to as the "EPRI ramp," is based on a preliminary analysis reported by EPRI.† The time/temperature scenarios calculated by INEEL* and EPRI† for some postulated severe accident sequences are shown in Figs. 14a and 14b, respectively, which also show the time/temperature histories used in the ANL tests. In both series of tests, the specimens were first heated rapidly to 300°C, equilibrated at 300°C, and then subjected to the test ramps. Both analyses also predict depressurization of the system due to the failure of the surge line. Because the primary purpose of our tests was to help develop a failure model, the tests have ignored the predicted depressurization. The EPRI analysis also predicts a reduction in temperature following a short 5-min hold at 667°C. To increase the contribution of creep damage in the tests, the EPRI temperature history was arbitrarily modified to include a 2-h hold time at 667°C and ignored the predicted reduction of temperature after the hold. If the specimen did not fail in 2 h of constant temperature hold, it was subjected to a temperature ramp of 2°C/min until failure.

Neither ramp chosen for the tests was intended to be an accurate representation of a particular sequence, but together they can represent a range of histories for which a failure model would be needed. Thus, although the INEEL and EPRI analyses predict that failure of the surge-line nozzle and consequent depressurization of the system will occur prior to the failure of the steam generator tubes with or without flaws, the tests at ANL were continued with full pressure until failure occurred.

Thirty-two ramp tests have been performed using the INEEL and EPRI time/temperature histories. Results from these tests are summarized in Tables 9 and 10 and Figs. 18a and 18b. In addition to failure data, Tables 9 and 10 also include crack depths measured by posttest fractography. The predicted failure temperatures and times in Figs. 18a and 18b were calculated with the creep rupture model and the ANL correlation for m_p , Eq 5c. The test results are replotted in Figs. 19a and 19b to show the dependence of the time to failure on m_p . The numbers on the abscissa of Fig. 19a and 19b are the stress magnification factors m_p as calculated

* P. G. Ellison, L. W. Ward, C. Dobbe, S. A. Chavez, C. L. Atwood, C. L. Smith, L. M. Wolfram, J. L. Jones, L. N. Haney, and W. J. Reece, The Risk Significance of Induced Steam Generator Tube Rupture, INEL-95/0641, Rev. 1 (Draft), Lockheed Martin Idaho Technologies, Inc., Idaho National Engineering and Environmental Laboratory, December 15, 1995.

† E. L. Fuller, M. A. Kenton, M. Epstein, R. E. Henry, and N. G. Cofie, Risks from Severe Accidents Involving Steam Generator Tube Leaks or Ruptures, EPRI TR-106194, Electric Power Research Institute, Palo Alto, CA (to be published).

by Eq. 5c; m_p can be considered a measure of crack severity that takes into account both the length and depth of the crack. The reduction in failure temperature of a flawed tube from that of an unflawed tube increases with increasing m_p . The failure temperature depends on the time/temperature history. The EPRI ramp, which includes a 2-h hold time, leads to lower failure temperatures than the INEEL ramp, which does not include such a constant-temperature hold. The deleterious effect of the 2-h constant-temperature hold of the EPRI ramp increases with increasing severity of the crack. For the unflawed tubes, there is little difference between the failure temperatures for the two histories, but for the most severe flaw geometries, there are significant reductions in the temperatures at which failure occurs for the EPRI ramp. This observation confirms that, for time/temperature histories that are characteristic of severe accident sequences, the particular history can significantly influence the failure temperature.

Evaluation of Stress Magnification Factor m_p in Flawed Tubes for High-Temperature Tests

The INEEL and EPRI ramp tests were also analyzed with other correlations for m_p . A comparison of the test results with the predictions of creep rupture models with the various m_p correlations is given in Table 11 in terms of the temperature at which failure occurred. Although the m_p models were based on the low-temperature tests, they appear capable of accurately predicting high-temperature situations, where creep damage is predominant, i.e., m_p seems to depend primarily on geometric factors rather than on material characteristics. The ANL and BCL models give significantly better results than the PNNL and INEEL models in these tests. Considering the relatively small differences in the predicted values of m_p for most crack depths and sizes, the differences are somewhat surprising. However, the matrix for these tests was heavily weighted toward cracks with $a/h \approx 0.6$, where the differences between the BCL-type models and the PNNL and INEEL-type models are relatively large. The test results suggest that the ANL and BCL models are more accurate in this range. Additional isothermal creep failure tests (discussed in an earlier section on validation tests), performed on specimens with deep cracks ($a/h \approx 0.9$) showed that either the ANL or the INEEL correlation for m_p was appropriate for this geometry. The results from these tests, which confirmed that the original BCL model overestimates m_p for such deep cracks, are consistent with the behavior observed in the low-temperature tests at EDF and PNNL and that either the ANL or the INEEL model gives much better predictions for such deep cracks. However, if both shallow and deep cracks are included, the ANL model gives the best overall predictions.

Predictions by Flow Stress Models

The times to failure for the INEEL and EPRI ramp tests were also predicted with the flow stress models. The predictions of the flow stress models are shown with experimental results in Figs. 20a and 20b. The predicted values were calculated with the INEEL flow stress curve (Fig. 9, Eq. 15a) and the ANL stress

magnification factor (Eq. 5c). Although the predictions for the tests with the EPRI ramp are reasonably good, the failure temperatures and times for the tests that used the INEEL ramp were significantly underpredicted. The accurate prediction of the EPRI ramp tests is probably a fortuitous consequence of the fact that the average strain rates in the ligament were close to the strain rate used in the tensile tests from which the flow stress curve was derived. Overall, the predictions agree much better with those of the creep rupture model.

3.2.5 Uncertainty in Predictions by Creep Rupture Model

As mentioned earlier, the uncertainty in the LMP reflected in Eq. 17c is due primarily to differences in the heat-to-heat behavior of materials. For the tubes used in the validation tests at ANL, the best fit to the experimental data is obtained when the constant in Eq. 17c is taken as 23.1, i.e., they seem to be fairly "average" heats of material. In a sampling from the population represented by Eq. 17c when computing damage, a single member of the population represented by Eq. 17c is selected and used for the entire history; the sampling is performed once for each history not for each time step. The variability observed in replicate tests on the same heat of material can be taken into account by assuming that the accumulated damage at failure is distributed, i.e., at failure, $D = 1 + \beta$, where β is a random variable that can be estimated from the variability observed in replicate tests. This variability, however, is small when compared with that observed in heat-to-heat variations. For tubes with flaws, failure is assumed to be controlled by Eq. 16b, where the effective stress is given by the nominal hoop stress multiplied by the magnification factor m_p , which is a function of crack geometry. For a given time/temperature history, the time to failure is a function of only m_p and the creep properties.

Uncertainty in m_p

For the low-temperature tests, the error in the predicted failure pressure Δp due to an error in crack depth measurement Δa is

$$\frac{\Delta p}{p} = m_p \frac{d\left(\frac{1}{m_p}\right)}{d\left(\frac{a}{h}\right)} \frac{\Delta a}{h}. \quad (18)$$

Assuming that the variance in a is independent of crack size, the total variance for the test series due to errors in the measurement of a can be estimated from

$$\sigma_p^2 = \left\{ \sum \left[m_p \frac{d\left(\frac{1}{m_p}\right)}{d\left(\frac{a}{h}\right)} \frac{1}{h} \right]^2 \right\} \sigma_a^2. \quad (19)$$

For estimates of σ_a based on the uncertainty of the depth measurements originally reported by PNNL (± 1.5 mils or $\sigma_a = 0.75$ mils), the estimate of σ_p obtained

from Eq. 19 represents only a small portion of the observed error. However, in the 20 samples reexamined by ANL to date, the standard deviation of the differences between the ANL measurement and the previous PNNL measurement is >2.2 mils. This suggests that the uncertainty in the depth measurements is much larger than previously assumed, and that most of the observed scatter in the values observed for m_p in the tests can be attributed to uncertainties in the measurement of crack size. In many cases, it is desirable to account for crack size uncertainties explicitly, hence, it is of interest to try to estimate the "model error," i.e., the error in the predicted m_p , given that the crack size is known exactly. The mean value of the observed m_p is a much better measure of the "true" m_p , and uncertainty estimates on the mean should provide a good estimate of the model error. Such estimates can be made by standard statistical techniques.¹⁹ The standard error of the mean estimate of the fitted variable is given by

$$\sigma_m = s \left[\frac{1}{n} + \frac{(x - \bar{x})^2}{\sum_k (x_k - \bar{x})^2} \right]^{1/2},$$

where n is the total number of data points, $\bar{x}$ is the mean value of the x_k values at which observations are available, s is the estimated variance about the regression,

$$s^2 = \frac{\sum_k (y_k - \hat{y}_k)^2}{n - 2},$$

y_k represents the observed values of the variable, and $\hat{y}_k$ represents the values predicted by the regression equation. The standard error of the predicted values is then

$$\sigma_m = s \left[1 + \frac{1}{n} + \frac{(x - \bar{x})^2}{\sum_k (x_k - \bar{x})^2} \right]^{1/2}$$

These limits are, of course, larger than those for the value of the mean value of y for a given x .

Although the actual uncertainty estimates are nonlinear, the simple linear expression

$$m_p = \frac{1 - \alpha \frac{a}{mh}}{1 - \frac{a}{h}} (1 \pm 0.06) \quad (20)$$

can be shown to bound the estimates of the 95% confidence interval on the mean.

3.2.6 Failure Modes of Specimens Tested at High Temperature

Depending on the absence or presence of flaws in the specimens and on the pressure and temperature at failure (independent of the details of the loading history), various failure modes were observed.

Unflawed Specimens

Typically, most of the unflawed specimens failed in an unstable manner, for example, see Figs. 21a and 21b. The specimens in these figures were subjected to a constant-temperature/constant-pressure loading and failed in ≈ 30 min. The specimen in Fig. 21a was subjected to 31 MPa (4500 psi) at 700°C whereas that in Fig. 21b was subjected to 16 MPa (2350 psi) at 800°C. However, a single unflawed specimen subjected to 12.4 MPa (1800 psi) at 800°C showed a completely different failure mode which is more typical of creep failure of internally pressurized tubes (Fig. 21c). The specimen in Fig. 21c failed in 4 h by developing a pinhole leak and after accumulating a significant amount of creep deformation (ballooning). This was the only specimen that was tested at a pressure < 14 MPa (2000 psi). All of the other unflawed specimens were tested at greater pressures and failed in an unstable manner, independent of the temperature history.

Flawed specimens

None of the flawed specimens failed in an unstable manner, because all of the test specimens were depressurized immediately upon ligament failure. Typical failure of a specimen that contained a 50-mm (2-in.)-long, 20%-deep initial flaw is shown in Figs. 22a and 22b; Figs. 23a and 23b show the same for a 25-mm (1-in.)-long, 60%-deep initial flaw, and Figs. 24a and 24b show the same for a 6-mm (0.25-in.)-long, 90%-deep initial flaw. At a given failure pressure and temperature, the longer and shallower the initial flaw, the greater was the crack opening at failure. Note that the 50- and 25-mm (2- and 1-in.)-long cracks show evidence of a slight tear at the crack tips.

Measurement of Crack Opening Displacements. The crack opening displacements (CODs) in all of the failed specimens with axial cracks were measured and the results are reported in Table 12. Flaws that had a measurable crack opening are classified as "fishmouth," and flaws that had no measurable COD are classified as leakers. The CODs and crack opening angles (COAs) are plotted against the initial flaw depths (a/h) in Figs. 25a and 25b, respectively, where a trend of increased COD and COA with decreasing initial flaw depth is clearly evident. Intuitively, this is to be expected, because the shallower flaws require proportionately larger pressures or higher temperatures or longer times to cause failure of the ligament than the deeper flaws of same length. The specimens with the 25- and 51-mm (1- and 2-in.) cracks, which showed the largest COA at failure, also had slight tears at the crack tips.

It is interesting to check whether a criterion for stability that works at low temperatures can distinguish the specimens that formed fishmouths from those that

leaked at high temperatures. At low temperatures, stability of a part-through crack after ligament failure can be determined from the following conditions:

If $p_{cr} > p_{sc}$, the crack is stable

If $p_{cr} < p_{sc}$, the crack is unstable,

where p_{cr} and p_{sc} are, respectively the failure pressures of through-wall and part-through cracks, as given by Eqs. 2a and 3a. In other words, the stability boundary on a plot with crack length $2c$ and crack depth a/h as axes is given by

$$m = m_p, \quad (21)$$

which is independent of the flow stress or loading and depends only on the length and depth of the crack. The curve corresponding to Eq. 21, together with all of the high-temperature test data, is plotted in Fig. 26. Although the curve $m = m_p$ appears to separate remarkably well the specimens that leaked from those that formed fishmouths, closer examination of the data for the 25-mm (1-in.) crack shows that the correlation does not work for all constant-pressure tests. In Fig. 27, constant-pressure data (both isothermal and T-ramp tests) for specimens with 25-mm (1-in.) crack are plotted. Contrary to what would be expected from the correlation, not all of the specimens below the $m = m_p$ line fishmouthed. A better correlation that distinguishes specimens that formed fishmouths from those that leaked is shown in Fig. 28 for specimens with a 25-mm (1-in.) flaw.

3.3 Stability of Axial Flaws after Ligament Failure

To address the question of stability of part-through cracks after ligament failure, we fabricated two specimens with 12.7-mm (0.5-in.) through-wall cracks and with inside metallic liners to act as patches to prevent leakage of the pressurizing gas. It was hoped that the metallic liner would transmit the pressure to the tube wall by creep. In the first specimen, a 0.25-mm (0.01 in.)-thick pure nickel liner was used. It was first heated to 850°C and then pressurized at a rate of 7 MPa/min. (1000 psi/min). The nickel liner developed a pinhole under the crack at a pressure of 10.3 MPa (1500 psi), just as the cracked section of the tube started to bulge. The predicted instability pressure for the tube was 16 MPa (2300 psi). A second specimen, also with a 12.7-mm (0.5-in.) through-wall crack, but with a 0.2-mm (0.008-in.)-thick Type 304 SS liner was heated to 750°C, pressurized to 16.2 MPa (2350 psi), and then held. This specimen also developed a leak due to failure of the stainless steel liner after ≈ 1 min of temperature and pressure hold. The measured crack opening after the test was 1.1 mm (0.043 in.). Subtracting the initial flaw width of 0.2 mm (0.008 in.), the crack opening rate in this specimen due to creep was 0.89 mm/min (0.035 in./min). No additional tests with through-wall cracks were performed.

Although none of the part-through flawed specimens failed in an unstable manner after ligament failure, some of the specimens with shallower initial flaws and higher failure pressure showed tearing at the crack tip. The tearing may indicate that these specimens were probably close to instability when the ligaments failed. At failure, the COA of these specimens was 40°-50°. To get an estimate of the time it

would take for a through-wall crack to open to a COA of 45°, the following calculations based on C* analysis were performed, although admittedly a more rigorous analysis would require that effects of finite deformation at the crack tip be taken into account.

3.3.1 Calculation of Crack-Opening Displacement by C* Analysis

If the material creep strain rate is taken as a simple power-law function of stress:

$$\dot{\epsilon} = A\sigma^n, \quad (22a)$$

the crack-opening rate may be obtained from the equation for COD as a function of the path-independent integral J (Ref. 20) by replacing J with the path-independent integral C* (Ref. 21) and the strain hardening exponent with the stress exponent for creep rate.

For a material with the uniaxial stress-strain law

$$\frac{\epsilon}{\epsilon_0} = \alpha \left(\frac{\sigma}{\sigma_0} \right)^m, \quad (22b)$$

the COD in a plate with a center crack of width 2b and a crack of length 2c due to applied tensile load P, is given by²²

$$\delta = \alpha \epsilon_0 h_2(c/b, m) \left(\frac{P}{P_0} \right)^m, \quad (23a)$$

where the function h_2 is defined in Ref. 22 and P_0 is a reference stress.

For an infinitely wide plate subjected to remote tensile stress σ , Eq. 23a reduces to

$$\delta = \alpha \epsilon_0 h_2(0, m) \left(\frac{\sigma}{\sigma_0} \right)^m. \quad (23b)$$

By analogy, the crack-opening rate in a pressurized tube at high temperature is then given by

$$\dot{\delta} = A h_2(0, n) \sigma^n. \quad (24)$$

In Eq. 24, σ represents the hoop stress multiplied by the usual bulging factor (Eq. 2b), which is a function of tube geometry and crack length.

Because creep tests conducted at INEEL¹⁷ have shown that Alloy 600 does not display primary creep at temperatures $\geq 732^\circ\text{C}$, but goes into steady-state creep almost immediately after the start of the tests, the C* formulation²² should give reasonable estimates of COD. The steady-state creep rates¹⁷ plotted in Fig. 29a shows that the creep rate (1/s, with stress in MPa) can be approximated by Eq. 22a with

$$A = 1.36 \times 10^{-18} \text{ and } n = 6.1 \text{ at } 732^\circ\text{C} \quad (25a)$$

and

$$A = 6.14 \times 10^{-12} \text{ and } n = 3.7 \text{ at } 871^\circ\text{C} \quad (25b)$$

The h_2 function²² is plotted in Fig. 29b as a function of c/b for plane stress and for $n = 6$ and 3.7 . For an infinitely wide plate, by extrapolation, $h_2 = 7$ for $n = 6$ and $h_2 = 6$ for $n = 3.7$. Substituting these values in Eq. 24, the crack opening rate for a pressure of 16.2 MPa (2350 psi) can be calculated and plotted as shown in Fig. 30. For a 6.4-mm (0.25-in.) crack, the times to open to 0.25 mm (0.010 in.) are ≈ 2 min at 732°C and a few seconds at 871°C . If we consider the measured COD result reported in Section 3.3 for the case of a 12.7-mm (0.5-in.)-long through-wall crack with stainless steel liner, the calculated COD due to creep at 732°C , after correcting for hoop stress due to the stainless steel liner, is 1.0 mm/min (0.04 in./min), which agrees well with the measured value of 0.89 mm/min (0.035 in./min).

The times to open an initially closed through-wall crack of various lengths to a COA of 45° at 732 and 871°C under an internal pressure of 16.2 MPa (2350 psi) are shown in Fig. 31. Note that the time varies from >40 min for a 6.4-mm (0.250-in.)-long crack, to 3 min for a 12.7-mm (0.5-in.)-long crack, and to 10 s for a 25.4-mm (1-in.)-long crack at 732°C . The corresponding times at 871°C are 4 min, 50 s, and 5 s, respectively. Inasmuch as most of the failure temperatures for our tests were in the range of 750 - 850°C , the times to open the cracks to a COA of 45° are relatively short, unless the cracks are <5 mm (0.2 in.).

3.3.2 Lower-Bound Flow Stress for Computing Unstable Failure

A lower bound to the flow stress for computing unstable failure of tubes with through-wall cracks can be obtained by ignoring the time it takes for a crack to open to a critical COA after ligament failure and calculating effective flow stresses from the solid line in Fig. 28 by using Eq. 2a. Such calculated flow stresses are plotted in Fig. 32, together with flow stresses obtained from tensile tests. Note that the calculated flow stress curve lies considerably above curves obtained from tensile tests at high temperatures, but approaches the latter at lower temperatures. This is to be expected, because the tensile tests are normally conducted at $\approx 10^{-3}/\text{s}$, whereas the maximum flow stresses at instability correspond to much higher strain rates. Although more data would be desirable, the instability pressures calculated from the flow stress curve in Fig. 32 and indicated by solid lines in Figs 33a and 33b are consistent with the failure modes of the test specimens with 6.4-mm (0.25-in.) and 50 mm (2-in.) cracks. It is proposed that the higher flow stress curve of Fig. 32 can be used to determine the stability of a through-wall crack conservatively, by using only the pressure and temperature at the moment of ligament failure and ignoring the pressure and temperature histories prior to ligament failure.

The critical crack length for a 22.2-mm (7/8-in.)-diam. tube as a function of temperature at a pressure of 16.2 MPa (2350 psi) is shown in Fig. 34. For typical severe-accident temperatures (600 - 900°C) at a pressure of 16.2 MPa (2350 psi), the critical crack length varies from 23 mm (0.9 in.) at 600°C to 10 mm (0.4 in.) at 900°C . These values represent minimum lengths of part-through cracks that will

become unstable immediately after ligament failure. However, at high temperatures, shorter cracks can grow in a stable manner by creep mechanisms prior to instability.

3.4 Tests on Specimens with Circumferential Cracks

In contrast to axial cracks, only 15 failure tests with part through circumferential cracks have been conducted. These tests had a single loading history that consisted of a constant internal pressure of 16.2 MPa (2350 psi) and a temperature ramp of 10°C/min from 300 to 600°C, followed by a temperature ramp of 2°C/min to ligament failure. As in the case of axial cracks, all specimens were depressurized immediately after ligament failure.

Constraint to bending was simulated by testing specimens with two symmetrically located cracks (Fig. 5a). Tests were also conducted on unconstrained tubes with a single circumferential crack. In all but one case, the crack opening at failure was significantly smaller than the openings observed for axial cracks. The only exception was a single tube with a 360° crack; this tube broke into two pieces. Also, except for this test, most tests failed by developing pin hole leaks in the ligament. A single tube with a 240° crack failed by ligament failure across the whole front of the flaw.

As expected, all of the tubes with symmetrical flaws failed without significant bending, see Figs. 35a and 35b. Tubes with deep flaws showed little or no bulging of the section that contained the flaws (Fig. 35a); those with shallower flaws showed some bulging (Fig. 35b). In contrast, all of the specimens with a single crack showed significant bending at failure (Fig. 35c).

3.4.1 Tubes with Two Symmetrical Circumferential Flaws

The results of 12 failure tests that were conducted on symmetrical circumferentially cracked specimens are listed in Table 13. Specimens with cracks of angular length $2\theta = 90^\circ$, 120° , 150° , and 180° (which is a full 360° crack) were tested. To keep the effects of crack length separate from the effects of crack depth, the depth to thickness ratio a/h was kept approximately constant at 0.77 for of the test specimens.

Initially, specimens were fabricated with cracks that had parallel sides; later, on they were changed to beveled sides (Fig. 36). Changing the cracks from parallel-sided to beveled-sided did not affect the failure temperatures (compare tests T-91 and T-92 in Table 13).

The failure temperatures are plotted against the flaw length 2θ in Fig. 37. Because of the variability of the crack depth around the circumference, the specimens with 360° ($2\theta = 180^\circ$) cracks showed the largest scatter. However, note from Table 13 that the specimens with the highest failure temperature (T-99 and T107) contained the shallowest cracks. The failure temperatures increased significantly when the crack depth was reduced at any angular crack length.

The failure temperatures for the tests were predicted by Eqs. 16b and 10b for m_p . The m_p values were calculated with both the Kurihara model (Eq. 11) and the ANL model (Eqs. 12a and 12b). The predicted failure temperatures are presented with the experimentally observed failure temperatures in Fig. 38. On average, the ANL model gives a closer prediction of the failure temperatures than the Kurihara model. The maximum error in predicted failure temperature for the ANL model is 43°C.

3.4.2 Tubes With a Single Circumferential Flaw

Three failure tests were conducted on free-to-bend specimens with a single circumferential flaw; the results are listed in Table 14. Specimens with cracks of angular length $2\theta = 90^\circ$, 180° , and 240° were tested.

The failure temperatures for the tests were predicted by Eq. 16b, with m_p given by Eq. 13c. The m_p values were calculated with the ANL model (Eqs. 12a and 12b). The predicted failure temperatures are close to the experimentally observed failure temperatures in all cases (Table 14).

3.4.3 Comparison of Failure of Tubes with a Single Flaw and with Two Flaws

The failure of tubes with two symmetrical cracks was usually initiated on the crack with the greater depth. However, very little overall bending of the specimen occurred. On the other hand, significant bending occurred in the specimens with a single crack. The longer the crack the more bending the specimen sustained. Typical failure of both types of specimens is shown in Figs. 35a-c. Specimen T-96 contained two symmetrical 150° flaws (81-87% deep), T-99 had two symmetrical 150° flaws (54-55% deep), whereas specimen T-111 contained a single 240° flaw (66-68% deep). Specimens T-96 and T-99 failed by developing a pinhole leak in the ligament on the side with the deeper crack. Specimen T-111 failed by full ligament failure.

All but one specimen with a 360° crack failed by ligament failure. A single specimen out of three with 360° cracks broke into two pieces. In most cases, ligament failure occurred locally, leading to a pinhole leak. One specimen with a single 240° crack failed by full ligament failure (accompanied by a loud noise). However, because of rapid depressurization of the specimen, the resulting through-wall crack did not propagate unstably to give rise to a guillotine break.

To demonstrate the influence of bending on failure pressure, the test results from both types of specimens are plotted against the total angular crack length ($n = 2\theta$) in Fig. 39. Note that the specimens with 360° cracks may be considered either as specimens with a single 360° crack ($n = 1$) or two 180° cracks ($n = 2$). Thus the failure temperatures for both types of cracked specimens coincide at $n = 2\theta = 360^\circ$. However, at smaller angles, the free-to-bend specimens failed at lower temperatures than the specimens with two symmetrical cracks, as expected.

4 Discussion of Models and Conclusions

4.1 Axial Cracks

Flow stress models can predict failure of flawed or unflawed steam generator tubes at low temperatures ($<350^{\circ}\text{C}$). The classical BCL model is adequate for relatively shallow axial cracks. However, it significantly overestimates the damaging influence of short, deep cracks ($\geq 90\%$). The new correlation developed at ANL can be more successfully applied to cracks of all sizes. Also, finite-element analyses have shown that computed m_p factors are more in agreement with those calculated by the ANL correlation than with those calculated by the classical BCL correlation.

At high temperatures ($>700^{\circ}\text{C}$), the simple rate-independent flow stress model cannot account for the rate effects observed in isothermal pressure ramp tests or constant-pressure temperature ramp tests. It predicts constant failure pressure and constant failure temperature, respectively, for these two types of tests, independent of the ramp rates, contrary to the test results. Furthermore, the flow stress model incorrectly predicts no failure for isothermal constant-pressure failure tests that experienced time-delayed failure. Developing a flow stress model that can account for the effects of rate on failure of flawed steam generator tubes would require some major theoretical developments. The semi-empirical creep rupture model developed at ANL can account for the rate effects reasonably well in all of the tests conducted to date by relatively simple calculations. It can also predict the time to failure for the isothermal constant-pressure failure tests reasonably well.

In general, the failure temperatures and times predicted by the flow stress models are closer to the experimentally measured values for the EPRI ramp than the INEEL ramp. The flow stress models tend to underestimate the failure temperatures of the most severe cracks by $\approx 60^{\circ}\text{C}$ for the INEEL ramp tests. On the other hand, they appear to predict the failure temperatures for the EPRI ramp tests, which include a 2-h constant-temperature hold, much better.

Independent of any model, the EPRI ramp produced more damage than the INEEL ramp (see Fig. 19). For a given crack geometry, the flow stress prediction for the failure temperature is nominally independent of the temperature ramp history. In reality, the values obtained for the flow stress at high temperatures are quite sensitive to the strain rates at which the flow stress is measured, and the flow stress models would not be expected to give good results for histories that produced strain rates that differed greatly from those in the test used to determine the flow stress. Thus, the flow stress models might be expected to be more applicable to the loading history without a constant-temperature hold. That the agreement is better for the EPRI ramp is probably fortuitous. Conservatism in the flow stress model that may be associated with the stress analysis or the strain rates used in the tensile tests to determine the flow stress are balanced by the additional damage associated with the 2-h hold of the EPRI ramp. It is likely that predictions by the flow stress models would not be as favorable if the hold time for the EPRI ramp were at a lower temperature ($\leq 500^{\circ}\text{C}$), where creep is negligible, or if the hold time were at a higher temperature, where creep damage would be more extensive.

The creep rupture model correctly predicts that the EPRI ramp is more damaging than the INEEL ramp. Also, it predicts the failure times and temperatures for both ramps more accurately than the flow stress model. It has been validated by 73 tests with various pressure and temperature histories for which the predictions by the flow stress model are either significantly off or incorrect. Therefore, we recommend that the creep rupture model be used to evaluate ligament failure of part-through axial cracks in steam generator tubes during severe accidents.

Although the stability of through wall cracks was not investigated experimentally under the current program, calculations were carried out to estimate the times taken by initially closed through-wall cracks of various sizes to open to a COA of 45° at pressures and temperatures expected at the time of ligament failure during severe accidents. Because in many cases these times are relatively short, they were ignored and a conservative procedure was developed to evaluate the stability of through-wall cracks by the flow stress model using a lower-bound flow stress curve that was derived from the present series of tests.

4.2 Circumferential Cracks

Fifteen high-temperature failure tests were conducted on 22.2-mm (7/8-in.)-diam tubes that contained a single or two symmetrical circumferential cracks under a constant internal pressure (16.2 MPa) and a linear temperature ramp of 2°C/min. The tubes with symmetrical cracks show little bending whereas those with a single crack show significant bending at failure. A creep rupture model that accounts for this bending effect has been developed. The magnification factor developed at ANL for the part-through flaws can predict the failure temperatures of all of the tests better than the magnification factor reported in the literature.

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Table 1. Room temperature failure and axial tensile properties of unflawed Alloy 600 tubing tested by ANL and PNNL.

Heat No	Tested by	Diam. (mm)	Wall thickness (mm)	Average Failure Pressure (MPa)	Average Yield Strength (MPa)	Average Ultimate Tensile Strength (MPa)	k
Valinco NX 8524	ANL	19.1	1.09	68.5	308	681	0.57
heat C	PNNL	19.1	1.09	67.1	361	674	0.53
PNNL ID EX-82-1/2675	ANL	22.2	1.27	72.4	357	683	0.57
heat B	PNNL	22.2	1.27	65.1	328	641	0.55
heat E	PNNL	22.2	1.27	65.8	300	681	0.55
heat F	PNNL	22.2	1.27	64.1	274	665	0.56

Table 2. Relative error between failure pressures predicted by various m_p models and those observed in PNNL tests^a

Error	Model				
	PNNL	INEEL	EDF	BCL	ANL
Mean	-0.01	0.05	0.08	0.04	-0.02
RMS all	0.13	0.11	0.12	0.13	0.09
RMS $a/t=0$	0.03	0.03	0.03	0.03	0.03
RMS $0.25 < a/t \leq 0.45$	0.09	0.09	0.09	0.09	0.09
RMS $0.45 < a/t \leq 0.75$	0.14	0.14	0.14	0.11	0.12
RMS $0.75 < a/t$	0.21	0.11	0.17	0.22	0.08

^aErrors may differ from estimates in original reports because they are based on new measurements of a/t made at ANL. Three outlier tests (C-03-5, C-15-5, and C-44-3) were not included in the calculation of the errors.

Table 3. Flaw depth and flaw depth/wall thickness ratio obtained by various techniques^a

Spec. No.	Tube Diam (mm)	Wall Thick. (mm)	Flaw Length (mm) ^b	Flaw Depth (mm)				Flaw Depth/Wall Thick. (a/h)			
				A	B	C	D	A	B	C	D
T-1	19.1	1.07	50.8	-	-	0.43	-	-	-	0.40	-
T-2	19.1	1.09				unflawed					
T-3	19.1	1.08	50.8	-	-	0.48	-	-	-	0.45	-
T-4	19.1	1.08	50.8	-	-	0.22	-	-	-	0.20	-
T-5	22.2	1.26	50.8	-	-	0.20	-	-	-	0.16	-
T-6	22.2	1.26	50.8	-	-	0.46	-	-	-	0.36	-
T-7	22.2	1.25	6.35	-	-	0.81	-	-	-	0.65	-
T-9	22.2	1.26	25.4	-	-	0.69	-	-	-	0.54	-
T-10	22.2	1.29				unflawed					
T-11	19.1	1.08	25.4	-	-	0.66	-	-	-	0.61	-
T-12	19.1	1.08	6.35	-	-	0.64	-	-	-	0.59	-
T-13	19.1	1.08				unflawed					
T-14	19.1	1.08	50.8	-	-	0.38	-	-	-	0.35	-
T-15	22.2	1.26	50.8	-	-	0.38	-	-	-	0.30	-
T-16	22.2	1.25	25.4	-	0.73	0.71	-	-	0.59	0.57	-
T-17	22.2	1.25	50.8	-	0.30	0.25	-	-	0.24	0.20	-
T-18	22.2	1.31	6.35	-	0.88	0.86	-	-	0.67	0.66	-
T-19	22.2	1.29				unflawed					
T-20	19.1	1.08	50.8	-	0.19	0.13	-	-	0.18	0.12	-
T-21	19.1	1.08	50.8	-	0.47	0.43	-	-	0.44	0.40	-
T-22	19.1	1.08				unflawed					
T-23	19.1	1.06	6.35	-	0.60	0.61	-	-	0.57	0.57	-
T-24	22.2	1.31	6.35	-	0.85	0.86	-	-	0.65	0.66	-
T-25	22.2	1.31	50.8	-	0.30	0.28	-	-	0.23	0.21	-
T-26	22.2	1.29	50.8	-	0.54	0.53	-	-	0.42	0.41	-
T-27	22.2	1.28	6.35	-	0.72	0.71	-	-	0.56	0.55	-
T-28	22.2	1.31				unflawed					
T-29	19.1	1.08	25.4	-	0.63	0.58	-	-	0.58	0.54	-
T-30	22.2	1.32				unflawed					
T-31	22.2	1.27	50.8	-	0.33	0.28	-	-	0.26	0.22	-
T-32	22.2	1.32	50.8	-	0.57	0.53	-	-	0.44	0.41	-
T-33	22.2	1.32	25.4	-	0.77	0.71	-	-	0.58	0.54	-
T-34	22.2	1.28	6.35	-	0.88	0.79	-	-	0.69	0.62	-
T-35	22.2	1.30				unflawed					
T-36	22.2	1.29				unflawed					
T-37	22.2	1.30	25.7	0.80	0.81	0.76	-	0.62	0.62	0.59	-
T-38	22.2	1.31	25.3	0.82	0.82	0.81	-	0.63	0.63	0.62	-
T-39	19.1		25.3	0.69	0.69	c	-	-	-	-	-
T-40	19.1		25.2	0.68	0.68	c	-	-	-	-	-
T-41	22.2	1.29				unflawed					

Table 3. (Cont'd)

Spec. No.	Tube Diam (mm)	Wall Thick. (mm)	Flaw Length (mm) ^b	Flaw Depth (mm)				Flaw Depth/Wall Thick. (a/h)			
				A	B	C	D	A	B	C	D
T-42	22.2	1.29					unflawed				
T-45	22.2	1.29					unflawed				
T-46	22.2	1.28					unflawed				
T-47	22.2	1.29	25.3	0.72	0.74	0.71	-	0.56	0.58	0.55	-
T-48	22.2	1.29	25.65	0.71	0.72	0.71	-	0.56	0.56	0.55	-
T-55	22.2	1.29	6.35	1.16	1.14	1.17	-	0.90	0.89	0.91	-
T-56	22.2	1.29	25.40	0.87	0.88	0.84	-	0.67	0.68	0.65	-
T-57	22.2	1.29	6.20	0.98	0.99	0.97	-	0.75	0.76	0.75	-
T-61	22.2	1.28					unflawed				
T-62	22.2	1.30					unflawed				
T-63	22.2	1.30					unflawed				
T-65	22.2	1.30	51.74	1.15	1.20	1.17	-	0.89	0.93	0.90	-
T-66	22.2	1.30	25.40	1.17	1.17	1.17	-	0.90	0.90	0.90	-
T-67	22.2	1.30	25.40	0.84	0.86	0.84	-	0.65	0.66	0.65	-
T-68	22.2	1.29	25.40	0.84	0.84	0.84	-	0.65	0.65	0.65	-
T-69	22.2	1.29	25.40	0.85	0.85	0.81	-	0.66	0.66	0.63	-
T-70	22.2	1.28	25.40	0.85	0.85	0.81	-	0.66	0.66	0.63	-
T-72	22.2	1.27	25.40	1.17	-	1.17	-	0.92	-	0.92	-
T-73	22.2	1.26	25.40	1.18	-	1.17	-	0.94	-	0.93	-
T-74	22.2	1.26	25.40	1.18	1.19	1.17	-	0.93	0.94	0.92	-
T-75	22.2	1.29	25.40	0.85	0.84	0.81	-	0.66	0.65	0.63	-
T-76	22.2	1.29	6.35	1.15	1.13	1.14	-	0.89	0.87	0.89	-
T-77	22.2	1.28	6.35	1.14	1.15	1.12	-	0.90	0.90	0.88	-
T-78	22.2	1.28	6.35	1.18	0.94	1.19	-	0.92	0.74	0.93	-
T-79	22.2	1.27	50.8	1.17	1.17	1.17	-	0.92	0.92	0.92	-
T-80	22.2	1.28	50.8	1.17	1.18	1.17	-	0.92	0.93	0.92	-
T-81	22.2	1.27	50.8	1.18	1.18	1.17	-	0.93	0.93	0.92	-
T-82	22.2	1.29					unflawed				
T-83	22.2	1.28					unflawed				
T-84	22.2	1.29	25.4	1.17	1.17	1.17	-	0.91	0.91	0.91	-
T-85	22.2	1.29					unflawed				
T-86	22.2	1.28					unflawed				
T-87	22.2	1.28					unflawed				
STD-1A	22.2	1.27	50.55	0.26	0.26	-	10.6	0.20	0.20	-	0.21
STD-1B	22.2	1.27	50.62	0.39	0.41	-	16.3	0.31	0.32	-	0.33
STD-1C	22.2	1.27	50.75	0.62	0.62	-	24.7	0.49	0.49	-	0.49
STD-1D	22.2	1.27	6.71	0.81	0.70 ^d	-	32.7	0.64	0.55 ^d	-	0.65

^a A = Pretest measurement by nondestructive optical microscopy.

B = Pretest measurement by replication.

C = Posttest measurement by fractography.

D = Measurement by destructive metallography (specimen not pressure-tested).

- ^b Nominal flaw lengths except for those specimens characterized by technique A (see footnote b) for which actual values are reported.
- ^c Not tested.
- ^d Replica torn.

Table 4. Flaw length and flaw depth along length of flaw in various specimens as determined by pretest optical microscopy (all values in mm).

Flaw Para- meters	T-37	T-38	T-39	T-40	T-47	T-48	T-57	T-58	T-59
Flaw Length	25.66	25.33	25.33	25.16	25.36	25.66	6.19	26.24	52.38
Position along Flaw									
0.23	-	-	-	0.67	-	-	-	-	-
0.30	-	-	0.49	-	-	-	-	-	-
0.34	-	0.60	-	-	-	-	-	-	-
0.38	-	-	-	-	0.67	0.57	0.85	0.97	0.93
0.40	0.78	-	-	-	-	-	-	-	-
2.03	-	-	-	-	-	-	0.97	-	-
3.56	-	-	-	-	-	-	0.97	-	-
5.33	-	-	-	-	-	-	0.99	-	-
5.81	-	-	-	-	-	-	0.90	-	-
6.35	0.81	0.81	0.69	0.68	0.72	0.71	-	1.02	1.01
12.7	0.82	0.80	0.69	0.67	0.72	0.72	-	1.02	-
19.1	0.83	0.80	0.69	0.68	0.72	0.71	-	1.03	1.03
24.90	-	0.81	-	0.66	-	-	-	-	-
24.98	-	-	-	-	0.73	-	-	-	-
25.19	-	-	0.66	-	-	-	-	-	-
25.27	0.47	-	-	-	-	-	-	-	-
25.28	-	-	-	-	-	0.70	-	-	-
25.40	-	-	-	-	-	-	-	-	1.03
25.86	-	-	-	-	-	-	-	0.90	-
31.75	-	-	-	-	-	-	-	-	1.02
38.10	-	-	-	-	-	-	-	-	1.01
44.45	-	-	-	-	-	-	-	-	1.02
52.00	-	-	-	-	-	-	-	-	0.43
Avg. Depth (mm)	0.82	0.80	0.69	0.68	0.72	0.71	0.98	1.02	1.02

Table 5. Summary of constant-pressure failure tests of specimens with and without flaws at various temperatures to validate the ANL creep rupture model. Also shown are failure times predicted by the creep rupture model.

Test No.	Crack Geometry			Loading History ^a		Failure Conditions			
	Axial Length (mm)	a/h	m _p	Max. T (°C)	p (MPa)	T _f (°C)	p _f (MPa)	t _f (min)	Pred. t _f (min)
T-37	25.4	0.59	1.96	667	16.2	667	16.2	336	128
T-56 ^b	25.4	0.65	2.21	667	16.2	667	16.2	74	62
T-38	25.4	0.62	2.07	700	13.2	700	13.2	28	88
T-41	Unflawed		1	700	31.03	700	31.03 ^c	38	42
T-61 ^d	Unflawed		1	700	30.68	700	30.68	49	45
T-47	25.4	0.55	1.82	750	9.65	750	9.65	186	176
T-60	Unflawed		1	750	22.68	750	22.68	29	43
T-48	25.4	0.55	1.82	800	9.65	800	9.65	26	32
T-42	Unflawed		1	800	16.2	800	16.2	33	50

^a Specimen pressure was first ramped to the indicated pressure at room temperature at a rate of 15.9 MPa/min (2300 psi/min) and held constant. The temperature was then increased following the EPRI ramp (Fig. 66) to the indicated temperature.

^b Duplicate of test listed immediately above.

^c Pressure decreased gradually from 32.8 to 29.3 MPa (4750 to 4250 psi) during hold.

^d Duplicate of test listed immediately above, but with pressure held constant at 30.7 MPa (4450 psi) during hold.

Table 6. Constant-pressure failure tests of specimens with deep cracks to validate the new m_p correlation proposed by ANL. Also shown are failure times predicted by the ANL creep rupture model.

Test No.	Crack Geometry			Loading History ^a		Failure Conditions			
	Axial Length (mm)	a/h	m_p	max. T (°C)	p (MPa)	T _f (°C)	P _f (MPa)	t _f (min)	Pred. t _f (min)
T-55	6.35	0.91	2.45	800 ^a	5.17	800	5.17	420	180
T-78 ^b	6.35	0.92	2.61	800	5.17	800	5.17	246	130
T-83	Unflawed		1	800	12.41	800	12.41	228	202
T-72	25.4	0.92	7.62	800	2.45	800	2.45	19	23
T-84	25.4	0.91	6.85	800	3.10	800	3.10	2	18
T-87	Unflawed		1	800	20.06	800	20.06	12	15
T-66	50.8	0.90	7.83	800	2.07	800	2.07	23	15
T-85	Unflawed		1	800	20.69	800	20.69	10	13

^a The temperature of the specimens was first increased from room temperature to 800°C in 1 h with no internal pressure. The pressure was then ramped at 15.9 MPa/min (2300 psi/min) to the indicated value.

^b Duplicate of test listed immediately above.

Table 7. Constant-pressure temperature ramp tests to validate the ANL creep rupture model. Also shown are failure temperatures predicted by the creep rupture model.

Test No.	Crack Geometry			Loading History ^a		Failure Conditions	
	Axial Length (mm)	a/h	m _p	Ramp rate (°C/min)	p (MPa)	T _f (°C)	Pred. T _f (°C)
T-62	Unflawed		1	20	16.2	913	916
T-63	Unflawed		1	2	16.2	843	838
T-71	Unflawed		1	0.2	16.2	779	770
T-67	25.4	0.65	2.21	20	7.34	892	916
T-68	25.4	0.65	2.21	0.2	7.34	770	778
T-74	25.4	0.93	8.60	0.2	1.86	860	918
T-73	25.4	0.93	8.60	0.2	7.34	612	604
T-76	6.35	0.89	2.22	20	7.52	915	911
T-77	6.35	0.9	2.32	0.2	7.17	778	766
T-59	50.8	0.79	3.93	2	5.17	810	801
T-79	50.8	0.92	9.68	0.2	1.69	815	914
T-80 ^b	50.8	0.92	9.68	0.2	1.69	859	914
T-81	50.8	0.93	10.99	0.2	1.50	678	768

^a The specimens were first pressurized to the indicated value at room temperature; the temperature was then ramped to 600°C in 1 h, followed by the indicated temperature ramp to failure.

^b Duplicate of test listed immediately above.

Table 8. Isothermal pressure ramp tests to establish maximum flow stress curve. Also shown are failure pressures and times predicted by the ANL creep rupture model.

Test No.	Crack Geometry			Loading History ^a		Failure Conditions			
	Axial Length (mm)	a/h	m _p	T (°C)	Ramp rate (MPa/min)	p _f (MPa)	t _f (min)	Pred. p _f (MPa)	Pred. t _f (min)
T-35	Unflawed		1	840	15.86 ^b	20.69	3.3	20.69	5.8
T-36	Unflawed		1	840	15.86 ^b	27.58	1.8	27.58	2.8
T-46	Unflawed		1	840	15.86	28.89	1.8	28.89	2.2
T-45	Unflawed		1	840	1.59	21.31	13	21.31	15
T-86	Unflawed		1	800	1.59	25.72	16	25.72	18
T-82	Unflawed		1	800	15.86 ^b	33.10	2.2	33.10	3.4
T-57	6.35	0.75	1.58	800	15.86	24.27	1.5	24.27	1.8
T-58	25.4	0.79	3.32	800	15.86	10.96	0.7	15.13	0.9
T-70	25.4	0.65	2.21	840	15.86	15.51	1.0	17.72	1.1
T-69	25.4	0.65	2.21	800	15.86	18.89	1.2	20.82	1.3
T-75	25.4	0.65	2.21	700	15.86	23.24	1.5	33.51	2.1

^a Specimen temperatures were ramped following the INEEL ramp (Fig. 65) to the indicated temperatures and held constant, with no internal pressure. The pressure was then ramped at the indicated rate to the final value.

^b Held at maximum pressure.

Table 9. Flaw sizes and temperatures at failure for INEEL ramp tests. Also shown are the predicted failure temperatures by the ANL creep rupture model.

Axial Length (mm)	19.05-mm (0.750-in.)-Diam Tube				22.23-mm (0.875-in.)-Diam Tube			
	Test No.	a/h	T _f (°C)	Pred. T _f (°C)	Test No.	a/h	T _f (°C)	Pred. T _f (°C)
Unflawed	T-2	Unflawed	823	837	T-10	Unflawed	835	844
Unflawed	T-13	Unflawed	826	837	T-30	Unflawed	843	844
6.35	T-12	0.59	798	806	T-7	0.65	803	806
6.35	-	-	-	-	T-34	0.62	802	809
25.4	T-11	0.61	758	752	T-9	0.54	768	784
25.4	-	-	-	-	T-33	0.54	778	792
50.8	T-3	0.45	774	788	T-6	0.36	808	805
50.8	T-14	0.35	805	803	T-32	0.41	800	803
50.8	T-4	0.2	808	816	T-5	0.16	825	824
50.8	-	-	-	-	T-31	0.22	817	818

Table 10. Flaw sizes and temperatures at failure for EPRI ramp tests. Also shown are predicted failure temperatures by the ANL creep rupture model.

Axial Length (mm)	19.05-mm (0.750-in.)-Diam Tube				22.23-mm (0.875-in.)-Diam Tube			
	Test No.	a/h	T _f (°C)	Pred. T _f (°C)	Test No.	a/h	T _f (°C)	Pred. T _f (°C)
Unflawed	T-22	Unflawed	828	833	T-19	Unflawed	839	842
Unflawed	-	-	-	-	T-28	Unflawed	843	842
6.35	T-23	0.57	767	772	T-18	0.66	779	786
6.35	-	-	-	-	T-24	0.66	781	782
25.4	T-29	0.54	726	702	T-16	0.57	724	692
25.4	-	-	-	-	T-27	0.55	724	727
50.8	T-21	0.40	760	745	T-15	0.30	794	786
50.8	-	-	-	-	T-26	0.41	770	762
50.8	T-20	0.12	814	814	T-17	0.20	816	802
50.8	-	-	-	-	T-25	0.21	817	811

Table 11. Errors in failure temperatures (°C) obtained when INEEL and EPRI ramp test data were analyzed with various models for m_p .

Errors	Models			
	ANL	BCL	PNNL	INEEL
Mean	2.5	7.5	11.6	14.5
RMS	9.7	14.2	18.9	18.8
Maximum	24.7	48.4	59.0	59.0

Table 12. Summary of failure type and flaw width after tests

Test. No.	Diam (mm)	Axial Crack length (mm)	Crack Depth, a/h (%)	Failure Type	Flaw Width (mm)	Failure Press. (MPa)	Failure Temp. (°C)
T1	19.05	-	40	-	-	-	-
T2	19.05	unflawed	0	rupture	-	16.2	823
T3	19.05	50.8	45	-	-	16.2	774
T4	19.05	50.8	20	fishmouth	0.9	16.2	808
T5	22.23	50.8	16	fishmouth	0.99	16.2	825
T6	22.23	50.8	36	fishmouth	0.64	16.2	808
T7	22.23	6.35	65	leak	-	16.2	803
T8		not tested	-	-	-	-	-
T9	22.23	25.4	54	fishmouth	0.25	16.2	768
T10	22.23	unflawed	0	rupture	-	16.2	835
T11	19.05	25.4	61	fishmouth	0.3	16.2	758
T12	19.05	6.35	59	leak	-	16.2	798
T13	19.05	unflawed	0	rupture	-	16.2	826
T14	19.05	50.8	35	fishmouth	0.74	16.2	805
T15	22.23	50.8	30	fishmouth	0.63	16.2	794
T16	22.23	25.4	57	fishmouth	0.22	16.2	724
T17	22.23	50.8	20	fishmouth	0.83	16.2	816
T18	22.23	6.35	66	leak	-	16.2	779
T19	22.23	unflawed	0	rupture	-	16.2	839
T20	19.05	50.8	12	fishmouth	1.03	16.2	814
T21	19.05	50.8	40	fishmouth	0.69	16.2	760
T22	19.05	unflawed	0	rupture	-	16.2	828
T23	19.05	6.35	57	leak	-	16.2	767
T24	22.23	6.35	66	leak	-	16.2	781
T25	22.23	50.8	21	fishmouth	0.79	16.2	817
T26	22.23	50.8	41	fishmouth	0.54	16.2	770
T27	22.23	25.4	55	fishmouth	0.19	16.2	724
T28	22.23	unflawed	0	rupture	-	16.2	843
T29	19.05	25.4	54	fishmouth	0.3	16.2	726
T30	22.23	unflawed	0	rupture	-	16.2	843
T31	22.23	50.8	22	fishmouth	0.78	16.2	817
T32	22.23	50.8	41	fishmouth	0.61	16.2	800
T33	22.23	25.4	54	fishmouth	0.23	16.2	778
T34	22.23	6.35	62	leak	-	16.2	802
T35	22.23	unflawed	0	rupture	-	20.7	840
T36	22.23	unflawed	0	rupture	-	27.6	840
T37	22.23	25.4	59	fishmouth	0.17	16.2	667
T38	22.23	25.4	62	fishmouth	0.13	13.2	700
T39	19.05	not tested	-	-	-	-	-
T40	19.05	not tested	-	-	-	-	-
T41	22.23	unflawed	0	rupture	-	31.0	700
T42	22.23	unflawed	0	rupture	-	16.2	800
T43	22.23	not tested	0	-	-	-	-
T44	22.23	not tested	0	-	-	-	-

Table 12. (Cont'd)

Test No.	Diam (mm)	Axial Crack length (mm)	Crack Depth, a/h (%)	Failure Type	Flaw Width (mm)	Failure Press. (MPa)	Failure Temp. (°C)
T45	22.23	unflawed	0	rupture	-	21.3	840
T46	22.23	unflawed	0	rupture	-	28.9	840
T47	22.23	25.4	55	leak	-	9.7	750
T48	22.23	25.4	55	leak	-	9.7	800
T49	19.05	not tested	-	-	-	-	-
T50	19.05	not tested	-	-	-	-	-
T51	19.05	not tested	0	-	-	-	-
T52	19.05	not tested	0	-	-	-	-
T53	19.05	not tested	0	-	-	-	-
T54	19.05	not tested	0	-	-	-	-
T55	22.23	6.35	91	leak	-	5.2	800
T56	22.23	25.4	65	fishmouth	0.18	16.2	667
T57	22.23	6.35	75	leak	-	24.3	800
T58	22.23	25.4	78	fishmouth	0.13	11.0	800
T59	22.23	50.8	79	leak	-	5.2	810
T60	22.23	unflawed	0	rupture	-	22.7	750
T61	22.23	unflawed	0	rupture	-	30.7	700
T62	22.23	unflawed	0	rupture	-	16.2	913
T63	22.23	unflawed	0	rupture	-	16.2	843
T64	22.23	not tested	-	-	-	-	-
T65	22.23	not tested	-	-	-	-	-
T66	22.23	50.8	90	leak	-	2.1	800
T67	22.23	25.4	65	leak	-	7.3	892
T68	22.23	25.4	65	leak	-	7.3	770
T69	22.23	25.4	63	fishmouth	0.33	18.9	800
T70	22.23	25.4	63	fishmouth	0.27	15.5	840
T71	22.23	unflawed	0	rupture	-	16.2	779
T72	22.23	25.4	92	leak	-	2.5	800
T73	22.23	25.4	93	leak	-	7.3	612
T74	22.23	25.4	92	leak	-	1.9	860
T75	22.23	25.4	63	fishmouth	0.38	23.2	700
T76	22.23	6.35	89	leak	-	7.5	915
T77	22.23	6.35	88	leak	-	7.2	778
T78	22.23	6.35	93	leak	-	5.2	800
T79	22.23	50.8	92	leak	-	1.7	815
T80	22.23	50.8	92	leak	-	1.7	859
T81	22.23	50.8	92	leak	-	1.5	678
T82	22.23	unflawed	0	rupture	-	33.1	800
T83	22.23	unflawed	0	balloon/leak	-	12.4	800
T84	22.23	25.4	91	leak	-	3.1	800
T85	22.23	unflawed	0	rupture	-	20.7	800
T86	22.23	unflawed	0	rupture	-	25.7	800
T87	22.23	unflawed	0	rupture	-	20.1	800

Table 13. Constant-pressure, temperature ramp tests for specimens with 360° or two symmetrical circumferential cracks. Specimens were pressurized at 16.2 MPa and subjected to temperature ramp of 2°C/min from 600°C to failure.

Test No.	Crack Geometry			Failure Temperature (°C)	
	2θ (deg)	a/h, avg.	a/h, max.	Experimental	Predicted
T-88	1 x 360°	0.67	0.71	796	772
T-89 ^a	1 x 360°	0.78	0.79	765	708
T-97 ^a	1 x 360°	0.77	0.77	728	716
T-90 ^b	2 x 90°	A-0.67	0.76	-	829
		B-0.75	0.77	815	
T-91 ^c	2 x 90°	A-0.75	0.77	-	821
		B-0.77	0.79	806	
T-92 ^d	2 x 90°	A-0.73	0.74	-	830
		B-0.75	0.77	805	
T-93 ^d	2 x 120°	A-0.77	0.81	-	794
		B-0.76	0.79	792	
T-95 ^d	2 x 120°	A-0.78	0.84	-	770
		B-0.81	0.90	783	
T-94 ^d	2 x 150°	A-0.79	0.82	775	746
		B-0.77	0.79	-	
T-96 ^d	2 x 150°	A-0.81	0.85	765	722
		B-0.82	0.87	-	
T-99 ^d	2 x 150°	A-0.52	0.54	820	856
		B-0.50	0.55	-	
T-107 ^d	2 x 150°	A-0.48	0.51	819	862
		B-0.45	0.49	-	

^a machined flaw, 0.25 mm wide; T-89 had 0.25-mm-deep bevel, then straight sides

^b straight sides, variable depth

^c straight sides, better depth consistency

^d beveled sides

Table 14. Constant-pressure temperature ramp tests for free-to-bend specimens with single circumferential crack. Specimens were pressurized at 16.2 MPa and subjected to temperature ramp of 2°C/min from 600°C to failure.

Test No.	Crack Geometry			Failure Temperature (°C)	
	2θ (deg)	a/h, avg.	a/h, max.	Experimental	Predicted
T-106	1x90°	0.83	0.85	785	804
T-109 ^a	1x180°	0.74	0.76	781	773
T-111 ^a	1x240°	0.66	0.68	771	787

^aTube was bent significantly; test T-111 failed with a "bang."

(a)

(b)

Figure 1. Magnification factor m_p as computed by BCL equation (Eq. 3b), ANL equation (Eq. 5c), INEEL equation (Eq. 4b), and the EDF equation (Eqs. 5a and 5b) as a function of crack length for crack depth-to-thickness ratios (a) $a/h = 0.5$ and (b) $a/h = 0.9$.

(a)

(b)

Figure 2. (a) True stress-strain curve used in finite-element analysis and (b) variation of calculated hoop stress enhancement factor in ligament with pressure for a 22.2-mm (7/8-in.)-diam tube with two axial part-through crack geometries at 300 and 750°C.

(a)

(b)

Figure 3. Typical shapes of a displaced internally pressurized tube with circumferential crack for cases of (a) free bending and (b) bending with lateral support.

(a)

(b)

Figure 4. (a) Variations of maximum rotation at cracked section with normalized pressure for free-to-bend tube that contained a 180° and a 240° through-wall circumferential crack, and laterally supported tube that contained a 240° crack. (b) Variations of normalized failure pressure with semiangle of circumferential through-wall crack for cases of free bending and complete constraint. Also shown are experimental failure pressure data and results from finite-element analyses (FEA). Open circle with arrow is a transversely supported tube (to simulate tube support plate) with a 240° crack.

Figure 5. Stress distributions through section at failure of tubes with (a) two symmetrically located part-through circumferential cracks and (b) single part-through circumferential crack.

(a)

(b)

Figure 6. Variations of experimental failure bending moments with crack angle and those predicted by (a) Kurihara model and (b) ANL model for unsymmetrical part-through circumferentially cracked 165.2-mm-diam (11-mm wall thickness) pipe specimens subjected to four-point bend test with constant internal pressure of 6.9 MPa (1000 psi) at room temperature). Dashed lines denote predicted failure bending moments for through-wall cracks; open symbols denote tests that failed by leakage, and filled symbols denote those that failed by breaking into two pieces.

Figure 7. Schematic representation of variation of stress with strain rate at given temperature and hardness state of a material.

(a)

(b)

Figure 8. Effects of (a) loading rate on failure pressure in isothermal failure test and (b) temperature ramp on failure temperature of unflawed 22.2-mm (7/8-in.)-diam Alloy 600 tube in failure test at a constant pressure of 16 MPa (2.35 ksi). Also shown are failure pressures and temperatures predicted by the flow stress model (dashed lines) using INEEL flow stress curve and by the ANL creep rupture model (solid lines).

Figure 9. Flow stress (when $k = 0.5$) curves for various product forms of Alloy 600.

Figure 10. Bilinear fit to existing creep data for Alloy 600. The three low outliers were not included in the analysis.

Figure 11. Correlation currently used in SCDAP and the existing data.

Figure 12. Experimental and predicted times to failure of unflawed Alloy 600 tubing under constant internal pressure. Tests were conducted isothermally and under constant-temperature ramps of 0.2°C/min, 2°C/min, and 20°C/min

Figure 13. Schematic diagram of high-temperature (severe-accident) tube-rupture facility.

(a)

(b)

Figure 14. Calculated and ANL simulation of (a) INEEL ramp and (b) EPRI ramp for high-temperature tests.

Figure 15. Experimental and predicted times to failure of flawed Alloy 600 tubing tested isothermally under constant internal pressure, for (a) shallow flaws, 25 mm (1 in.) long and ranging in depth from 56 to 65%, at temperatures between 667 and 800°C and (b) flaws with depths between 90 and 92% at 800°C. Arrow indicates pinhole failure in which pressure was undiminished after failure, but test was interrupted.

(a)

(b)

Figure 16. Experimental and predicted (a) failure pressures of flawed and unflawed tubes subjected isothermally to two pressure ramps at 700-840°C and (b) failure temperatures of flawed tubes subjected to two temperature ramps with constant internal pressures chosen so that the product of m_p and nominal hoop stress were kept approximately constant.

Figure 17. INEEL flow stress curve and "effective" flow stresses calculated from pressure ramp tests on flawed and unflawed tubes.

(a)

(b)

Figure 18. Predicted (by creep rupture model) versus observed (a) failure temperatures and (b) times to failure for high-temperature failure tests conducted with the INEEL and EPRI temperature ramps on 19- and 22-mm tubes.

Figure 19. Summary of experimentally observed failure temperatures for severe-accident tests on (a) 22.2-mm (7/8-in.)-diam tubes and (b) 19.1-mm (3/4-in.)-diam tubes. The abscissa shows average magnification factors (Eq. 5c) m_p for each crack configuration.

(a)

(b)

Figure 20. Predicted (by flow stress model) versus observed (a) failure temperatures and (b) times to failure for high-temperature failure tests conducted with the INEEL and EPRI temperature ramps on 19- and 22-mm-diam tubes.

(b)

(a)

(c)

Figure 21. Failed specimens (a) T-41 (tested at 700°C under 31.0 MPa, $t_f \approx 0.5$ h), (b) T-42 (tested at 800°C under 16.2 MPa, $t_f \approx 0.5$ h), and (c) T-83 (tested at 800°C under 12.4 MPa, $t_f \approx 4$ h). All three specimens were unflawed.

(a)

(b)

Figure 22. (a) Top view and (b) side view of failed specimens T-31 ($2c = 50$ mm [2 in.], $a/h = 20\%$).

(a)

(b)

Figure 23. (a) Top view and (b) side view of failed specimen T-75 ($2c = 25$ mm [1 in.], $a/h = 65\%$).

(a)

(b)

Figure 24. (a) Top view and (b) side view of failed specimen T-76 ($2c = 6$ mm [0.25 in.], $a/h = 90\%$).

(a)

(b)

Figure 25. Measured (a) crack opening displacements (CODs) and (b) crack opening angles (COAs) in 19- and 22-mm (0.75- and 0.875-in.) diam failed high-temperature test specimens with 6-, 25-, and 51-mm (0.25-, 1-, and 2-in.) cracks as a function of initial axial flaw depth.

Figure 26. Initial flaw depth (a/h) versus axial crack length for all specimens tested at high temperature.

Figure 27. Flaw depth (a/h) versus pressure for constant-pressure tests on specimens with 25-mm (1-in.) part-through axial crack, conducted isothermally or under a temperature ramp.

Figure 28. Variation of failure pressure with failure temperature for tests conducted on specimens with a 25-mm (1-in.)-long crack. Open symbols denote specimens that leaked (no measurable COD) and filled symbols denote specimens whose cracks opened (formed fishmouth). Numbers denote COD (in mm) at failure for specimens that formed fishmouths.

(a)

(b)

Figure 29. (a) creep properties of Alloy 600 at 727°C and (b) variation of h_2 with c/b .

Figure 30. Crack opening rate versus crack length at 732 and 871°C.

Figure 31. Time to open initially closed through-wall cracks to a COA of 45° for a 22.3-mm (7/8-in.)-diam steam generator tube at 732 and 871°C under constant internal pressure of 16 MPa (2350 psi).

Figure 32. Temperature variation of flow stress ($k = 0.5$) of Alloy 600 rod and plate specimens, derived from tensile test data as reported in literature. Bold line indicates lower bound to flow stress (calculated from solid line in Fig. 28 with Eq. 2a) for calculating instability pressures of tube specimens with through-wall cracks.

(a)

(b)

Figure 33. Pressure at failure as a function of temperature at failure for all specimens with (a) 6-mm (0.25-in.)-long axial crack and (b) 51-mm (2-in.)-long axial crack. Solid lines are predicted from the higher flow stress curve of Fig. 32.

Figure 34. Critical crack length as a function of temperature for a 22.3-mm (7/8-in.)-diam Alloy 600 tube at internal pressure of 16 MPa (2350 psi).

(a)

(b)

(c)

Figure 35. Typical failure of specimens with two symmetrical flaws, showing (a) little bending and no bulging (T-96), (b) little bending and some bulging (T-99), and (c) a single flaw (T-111) showing significant bending at failure.

Figure 36. Symmetrical part-through circumferentially flawed specimens with parallel sides and bevel sides.

Figure 37. Variation of failure temperatures with crack angle for specimens with two symmetrical cracks of various depths

Figure 38. Experimental failure temperatures and failure temperatures predicted by the Kurihara and ANL models for two symmetrical part-through circumferential cracks of various semiangular length θ .

Figure 39. Failure temperatures for specimens with one ($n = 1$) and two ($n = 2$) circumferential cracks.

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