Structural Evaluation of Electrosleeved Tubes Under Severe Accident Transients *

by

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Structural Evaluation of Electrosleeved Tubes Under Severe Accident Transients

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Abstract

A flow stress model was developed for predicting failure of Electrosleeved PWR steam generator tubing under severe accident transients. The Electrosleeve, which is nanocrystalline pure nickel, loses its strength at temperatures greater than 400°C during severe accidents because of grain growth. A grain growth model and the Hall-Petch relationship were used to calculate the loss of flow stress as a function of time and temperature during the accident. Available tensile test data as well as high temperature failure tests on notched Electrosleeved tube specimens were used to derive the basic parameters of the failure model. The model was used to predict the failure temperatures of Electrosleeved tubes with axial cracks in the parent tube during postulated severe accident transients.

1. Introduction

Prediction of failure of a complex composite material like the Electrosleeved steam generator tubing under severe accident transients is a difficult problem. The Electrosleeve material is almost pure Ni and derives its strength and other useful properties from its nanocrystalline microstructure, which is stable at reactor operating temperatures. However, it undergoes rapid grain growth at the high temperatures that are expected during severe accidents, resulting in a loss of strength and a corresponding decrease in the flow stress. The magnitude of this decrease depends on the time-temperature history during the accident. Low-temperature tensile data on the Electrosleeve material (without the tube) are available, but the available tensile data on Electrosleeve at high temperatures in either the aged or unaged condition are very limited. Initially, the assumption was made that there were no experimental failure data available on Electrosleeved tube with cracks. Therefore, analytical models were exclusively relied upon to predict failure of the composite structure with cracks, using the available tensile data to determine the parameters of the models.

Following initial modeling and analysis, Framatome Technologies, Inc. (FTI) provided failure data from six tests on unsleeved and Electrosleeved tubes with and without notches under simulated severe accident loading. In contrast to the prediction by the model that the damaging effect of notch length should level off with increasing notch length, the FTI test data indicated that the failure temperature of the Electrosleeved tube decreased almost linearly with notch length. On the other hand, the failure temperatures of the unsleeved and degraded Alloy 600 tubes were predicted reasonably well by the flow stress model presented in Ref. 1.
Subsequent to their initial tests, FTI supplied twelve Electrosleeved tube specimens, three of which had 2 in. long throughwall notches for testing at Argonne National Laboratory (ANL). Eight other specimens were notched by electro-discharge machining at ANL with notch lengths of 0.5, 1, and 3 in., all nominally 100% throughwall of the parent tube. All specimens were tested to failure using a temperature and pressure history that closely simulated those for a station blackout (SBO) with a depressurized secondary side (Case 6).2 This paper describes the basis for the analytical models and shows how the basic parameters of the models were revised on the basis of the test results. Although the models overestimated the failure temperatures of the FTI tests by 35–70°C initially, the failure temperatures were predicted within ±15°C using the revised parameters. Finally, the models are used to predict failure of Electrosleeved steam generator tubing during postulated severe accidents. The reference geometry considered in this paper is a 7/8-in.-diameter, 0.050-in.-wall-thickness Alloy 600 tubing, with a nominal 0.038-in.-thick Electrosleeve (Fig. 1).

2. Flow Stress Criterion

The flow stress failure criterion for tubes with axial cracks can be stated as follows1:

\[ \sigma_{\text{lig}} = H(T, t), \]  
(1a)

where \( H \) is the flow stress (dependent on the temperature history) of the Electrosleeve and \( \sigma_{\text{lig}} \) is the ligament stress, given by Eq. 1b.

\[ \sigma_{\text{lig}} = m_p \sigma_h \]  
(1b)

where \( m_p \), which depends on the axial crack length and depth, is the ligament stress magnification factor, \( \sigma_h \) is the nominal hoop stress (calculated using the mean radius and total thickness of the tube and the sleeve).

2.1 Determination of \( m_p \) for axial cracks

Initially the hoop stress magnification factor \( m_p \) for the crack tip ligament in the Electrosleeve was estimated from the ANL equation for a single-layer shell used in Ref. 1. However, the \( m_p \) factor could be reduced if the flow stress of the Electrosleeve ligament is significantly lower than that of the parent tube. In fact, detailed analyses of available tensile data of the Electrosleeve (to be discussed later) showed that, at the temperatures of interest, the flow stress ratio between the parent tube and the Electrosleeve varies between 2-3. To determine the effect of the flow stresses of the Electrosleeve and Alloy 600 on \( m_p \), a series of finite element analyses (FEA) was conducted for a bi-layer tube with a 100% throughwall crack in the outer layer (simulating Alloy 600) under a constant temperature and increasing pressure loading. The ratio between the flow stress of the outer layer and the inner layer (simulating the Electrosleeve) was varied between 1 and 3. The results, plotted in Fig. 2a, confirm that the values of \( m_p \) are indeed reduced significantly when the flow stress ratio is increased. Note that the \( m_p \) value calculated by FEA for flow stress ratio =1 is close to that derived from the ANL correlation for cracks ≤ 1 in., but levels off with increasing crack length beyond 2 in. On the other hand, the \( m_p \) calculated by the ANL correlation continues to increase with increasing crack length, although the actual increases are small beyond a crack length of 2 in.

Because the FEA grid may not have been sufficiently fine to obtain highly accurate solutions, the FEA results were used to calculate the ratio between the \( m_p \) for the Electrosleeved tube and the homogeneous tube as a function of the ratio between the flow stress of the parent tube and the
Electrosleeve, as shown in Fig. 2b. This $m_p$ ratio was then used to scale the $m_p$ calculated by the ANL correlation for a homogeneous tube to obtain the effective $m_p$ of the Electrosleeved tubes with notches as indicated in Eq. 2 (FSR denotes flow stress ratio):

$$m_p(\text{eff.}) = \frac{m_p(\text{FEA})}{m_p(\text{FEA, FSR} = 1)} \times m_p(\text{ANL})$$

(2)

3. Material properties data for Electrosleeve

Data provided by FTI show that the Electrosleeve material is stronger than the tube material at the reactor operating temperature. However, at high temperatures (≥400°C), the Electrosleeve begins to lose its hardness because of grain growth (Fig. 3a). The thermal aging effect is a complicated phenomenon consisting of at least two steps. In the first step, the phosphide precipitates in the grain boundary, which prevent grain growth, are dissolved, and in the second step grain growth occurs. The starting or initial hardness of the FTI isothermal aging specimens show a very large specimen-to-specimen scatter. Therefore, the loss of hardness data for each specimen was normalized with respect to its initial hardness, as shown in Fig. 3a. The data in Fig. 3a suggest that the hardness of the material decreases with time, albeit at a relatively slow rate, starting very early. The nucleation times for this process for specimens aged at > 425°C are relatively short and are ignored in the nucleation model to be discussed later. The data in Fig. 3a also suggest the existence of a second process with longer nucleation times that involves very rapid decrease in hardness with time and is very likely linked to the process of rapid grain growth. The reciprocal of the incubation time for the onset of rapid loss of hardness (rapid grain growth) has a temperature-dependent activation energy as shown in Fig. 3b. For analyses of loss of hardness, the continuously varying activation energy curve $Q$ was replaced by the step function indicated in Fig. 3b. Sensitivity studies showed that the results are not sensitive to the form chosen for $Q$.

Data for the yield and ultimate tensile strengths of the Electrosleeve material were reported by FTI from room temperature to 343°C (650°F). The flow stress, which is the average of the yield and ultimate tensile strengths, as a function of temperature is shown in Fig. 4. The single high temperature Electrosleeve data point in Fig. 4 was estimated from a tensile test conducted on aged material and will be discussed later. Initially, in the absence of any other flow stress data at high temperature, the solid line in Fig. 4 was used as an estimate for the unaged flow stress curve of the Electrosleeve. It should be remembered that, during a severe accident, the actual flow stress of the Electrosleeve is reduced from the unaged curve ($H_u$) shown in Fig. 4 because of grain growth. The high temperature tests conducted by ANL (to be discussed later) and FTI suggested that the unaged flow stress of the Electrosleeve material is less than that shown in Fig. 4.

FTI also submitted to the NRC data from a series of tensile tests at 343°C on specimens that were exposed to isothermal pre-aging treatment at high temperatures for various times (Fig. 5a). The data at 760°C is for a tensile test that was conducted at 760°C.

FTI also provided failure data from six tests on internally pressurized tubes that were subjected to a variety of temperature ramps simulating those expected during SBO accidents. The initial temperature ramp rate up to ~ 500°C varied between 3-5°C/min, which was generally followed by a ramp rate of 7-9°C/min until failure. However, in some cases the ramp rate was gradually decreased to 1.2 °C/min above 705°C. Three tests were conducted on unsleeved Alloy 600 tubes with and without any degradation and three on Electrosleeved tubes (7/8 in. Dia.) with 0.5, 1 and 2 in. throughwall axial notches in the parent tube.
4. Analytical models

Two analytical models were originally developed for estimating the failure temperature under severe accident transients - a model based on linear damage rule and a model based on the Hall-Petch relationship. In both models, a basic assumption is the existence of a temperature-dependent unaged (i.e., without grain growth) flow stress curve of the Electrosleeve. This unaged flow stress curve is largely a theoretical construct of the models because to establish it directly from tensile tests at high temperatures would be difficult due to the grain growth that would occur in the specimens unless the specimens could be heated up, stabilized, and tested very rapidly. Therefore, it was calculated from high temperature failure data using the models. Ideally, high temperature failure tests on specimens subjected to severe accident temperature and pressure ramps should be used to derive the flow stress curve of the Electrosleeve. However, such test data were not available when the models were first developed. Therefore, the unaged flow stress curve of the Electrosleeve was derived initially by analysis of a single tensile test on a specimen pre-aged and tested at 760°C (rectangle in Fig. 4) and the FTI tensile test data at \( \leq 343°C \) (Fig. 4). Subsequently, the flow stress curve of the Electrosleeve was revised on the basis of high temperature failure tests conducted at ANL.

Both models gave comparable results for failure temperatures. Since the Hall-Petch model was more mechanistically based, it was selected for use in failure prediction.

4.1 Model based on Hall-Petch equation

In this model the "nucleation" phase is explicitly separated from the "growth" phase of the grain growth phenomenon. As mentioned earlier, it was assumed that the Electrosleeve has an initial "unaged" flow stress curve \( H_i(T) \), e.g., Fig. 4. The hardness or flow stress (at a sufficiently high strain rate) of the Electrosleeve material was assumed to depend on the grain size by the Hall-Petch relationship, i.e.,

\[
H(T) = A d^{-n} f(T),
\]

where \( H(T) \) is the flow stress at any temperature \( T \), \( d \) is the grain diameter, \( n \) is the Hall-Petch exponent, and \( f(T) \) is a correction factor for temperature. During high temperature exposure, the growth rate of grain diameter was assumed as Eq. 4.

\[
\dot{d} = \begin{cases} 
0 & \text{for } t < t_n \\
\frac{B}{d} \exp\left(\frac{-Q_g}{RT}\right) & \text{for } t \geq t_n,
\end{cases}
\]

where \( t_n \) is the nucleation time to loss of flow stress (i.e., onset of grain growth), \( B \) is a constant, \( Q_g \) is the activation energy for grain growth, \( R=1.987 \text{ cal/mole}^\text{°C} \). Recrystallization due to plastic straining was ignored. The form of the grain growth rate equation was chosen such that, under isothermal aging, the grain growth follows a parabolic law. Under isothermal aging, the reciprocal of the nucleation time \( (1/t_n) \), which has an activation energy \( Q_n \), is given by the following equation:

\[
\frac{1}{t_n} = C \exp\left(\frac{-Q_n}{RT}\right)
\]

where \( C \) is a constant. The variation of \( Q_n \) with \( T \) is given in Fig. 3b.
The tensile data reported by FTI on pre-aged specimens of Electrosleeve material were used to calculate the values of various parameters in Eqs 3-5. Integrating Eq. 4, using Eq. 5 and assuming $Q_n = Q_g = Q$,

$$d(t) = \left[ d_i^2 \frac{2B}{C} + 2B \exp \left( -\frac{Q}{RT} \right) \right]^{1/2},$$

where $d_i$ is the grain diameter of the as-received material and $T$ is the aging temperature. Substituting Eq. 6 into Eq. 3, denoting the tensile testing temperature as $T_0$, the initial "unaged" flow stress at $T_0$ as $H_0$ and solving,

$$t \exp \left( \frac{-Q}{RT} \right) = \frac{d_i^2}{2B} \left[ \left( \frac{H_0}{H} \right)^{2/n} - 1 \right] + \frac{1}{C},$$

where

$$H_0 = A d_i^{-n} f(T_0).$$

Results from the FTI tensile data ($T_0=343°C$) on pre-aged specimens are plotted in Fig. 5b using coordinates suggested by Eq. 7a with $n = 0.33$. Values of $d_i/2B$ and $1/C$ were obtained from the slope and intercept of the linear fit. As mentioned earlier, the specimen that was aged for 30 min at 760°C was also tensile tested at 760°C. Since, prior to constant temperature aging, this specimen was ramped from 327°C to 760°C at the slow rate of 5.8°C/min, an analysis using an activation energy of 35 kcal/mole gave an effective aging time at 760°C of 39 min. A reduction factor for the flow stress at 760°C compared to that at 343°C was obtained by fitting the data, excluding the data at 760°C, by a best-fit line and extending the line to the value of the time-temperature parameter corresponding to the test at 760°C, as shown by dotted line in Fig. 5b. The rectangular symbol in the flow stress curve shown in Fig. 4 is the estimated value of unaged flow stresses at 760°C as obtained by applying the reduction factor to the flow stress at 343°C.

Nucleation times to onset of loss of flow stress (i.e., grain growth) under isothermal aging were calculated using Eq. 5 and the step-wise varying approximation to the activation energy data shown in Fig. 3b. The calculated nucleation times and those for the rapid loss of hardness as derived from the FTI data (Fig. 3a), plotted in Fig. 6, show that $n=0.33$ fits the data better.

Under a variable temperature history, Eq. 5 can be generalized to give the time to nucleation as follows:

$$C_i \int_0^t exp \left( \frac{-Q}{RT(t)} \right) dt = 1$$

Similarly, Eq. 4 can be integrated to give the grain diameter at any time $t$.
Substituting Eqs 8-9 into Eq. 3 and solving for the flow stress $H$ at any time,

$$d(t) = \begin{cases} d_i & \text{for } t < t_n \\ d_i^2 + 2B_i^t & \text{for } t \geq t_n \end{cases}$$

and

$$H(t) = \begin{cases} H_i(t) & \text{for } t < t_n \\ \left(1 + \frac{2B}{d_i^2} t_n^t \exp\left(-\frac{Q}{RT(t)}\right)\right)^{-n/2} & \text{for } t \geq t_n \end{cases}$$

where $H_i(t)$ is the initial "unaged" flow stress at $T(t)$. Ligament failure is predicted to occur when Eq. 1a is satisfied.

5. Predicted Versus Observed Failure temperatures

The studies in Ref. 2 showed that the most severe challenge to the integrity of steam generator tube arises from station blackout (SBO) sequences in which the secondary system dries out and the primary system fails to depressurize (a “high-dry” sequence, Case 6). In this case the $\Delta p$ across the tube wall is approximately constant $= 2.35$ ksi and the time-temperature history is given in Fig. 7. For this case the tube temperature is $684^\circ$C when the surge line failure occurs.

5.1 Initial Failure Predictions for FTI tests

FTI simulation of the SBO (Case 6) temperature ramp consisted of a variety of temperature ramps with the initial rate (at $< 500^\circ$C) varying between 3 and 5$^\circ$/min. Above a temperature of $500^\circ$C, the notched Electrosleeved specimens were ramped at 7-9$^\circ$/min to failure, except for the test with a 0.5 in. notch for which the ramp rate was gradually reduced to 1.2$^\circ$/min above $705^\circ$C.

The model using the Hall-Petch equation to represent the changes in the flow stress was used to predict failure temperatures of the tests conducted by FTI on 7/8 in. diameter unsleeved tubes, both degraded and undegraded, and Electrosleeved tubes with 100% deep axial notches in the parent tube. The details of the notch and tube geometry of the specimens are included in Table 1. Failure temperatures were calculated using the temperature ramps for each specimen supplied by FTI. The predicted failure temperatures for the notched unsleeved Alloy 600 tubes were independent of the temperature history. The comparison between the predicted and observed failure temperatures is shown in Table 2. The predictions for the Electrosleeved tubes in Table 2 were made using the high-temperature flow stress curves given in Fig. 4. The predicted failure temperatures overestimate the experimentally observed failure temperatures of the Electrosleeved tubes by 35-70$^\circ$. Note that these predictions were made without the benefit of a single high-temperature pressurized tube test. The predictions are in much better agreement with the observed values, if a modified flow stress curve that includes the results of high temperature pressurized tube tests, is used, as will be discussed later.

The failure temperatures of the two unsleeved degraded Alloy 600 tubes were predicted quite well by the flow stress model of Ref. 1. Note that these two tests are consistent with each other because the $m_p$ value for a 50% deep 2 in. crack is approximately 2, which is also the hoop stress magnification factor for a 50% uniformly thinned tube. The test on undegraded and unsleeved Alloy 600 involved a hold at
constant temperature, which the flow stress model cannot handle. However, the creep rupture model presented in Ref. 1 can predict the failure time within a factor of 2.

5.2 ANL test results and revised unaged flow stress curve

As mentioned earlier, FTI provided twelve Electrosleeved specimens three of which were notched. Eight additional specimens were notched (= 0.0075 in. wide) at ANL by electro-discharge machining. Eleven tests were conducted at ANL. The time-temperature history for these tests closely simulated the SBO sequence identified as Case 6 in Ref. 2 and consisted of holding the pressure differential constant at 2.35 ksi while ramping the temperature from 300°C to 545°C at 4.2°C/min followed by 12.5°C/min ramp until failure. (solid line in Fig. 7). Note that the FTI tests were conducted using different temperature ramps, as discussed in section 5.1. A summary of all the tests conducted by ANL as well as by FTI is given in Table 1. The failure temperatures for the ANL tests were used to recalculate the unaged flow stress curve of the Electrosleeve material using the Hall-Petch model (with n=0.33) and the effective m_p factors from Fig. 2b and Eq. 2. The revised unaged flow stress curve is compared with the previously estimated flow stress curve (Fig. 4) in Fig. 8. Note that the revised curve has a different shape and falls below the earlier estimated curve.

An examination of Table 1 shows that the geometries of the Electrosleeved tubes have some variations. An upper bound to the predicted failure temperatures was obtained by using the following:

\[
\text{Tube thickness} = 0.051 \text{ in.}, \text{ sleeve thickness} = 0.040 \text{ in. and notch depth}=0.048 \text{ in.,}
\]

and a lower bound was obtained by using the following:

\[
\text{Tube thickness} =0.049 \text{ in.}, \text{ sleeve thickness} = 0.035 \text{ in. and notch depth}=0.049 \text{ in.}
\]

The two bounds together with the test data are plotted in Fig. 9a. In cases where the notch depth was less than the full thickness of the parent tube wall, an effective flow stress for the ligament (average flow stress weighted by thickness) was used. Both the test data and the model indicate that the decrease in failure temperature with notch length saturates at a notch length of ~ 3 in. and no significant additional decrease of failure temperature should occur at longer notch lengths. The tube-to tube variations in geometry give rise to a significant difference between the two bounds, and a much better correlation between the predicted and the observed failure temperatures is obtained if the actual geometry for each specimen is used in calculating the predicted failure temperatures (Fig. 9b).

5.3 Revised failure predictions for the FTI tests

Fig. 10 shows a comparison of the failure temperatures as reported by FTI and the two bounds based on the same bounding geometrical assumptions as in Fig. 9a. All the tests tend to fall near the lower bound curve, which is not surprising because the thicknesses of the Electrosleeve were close to the lower bound thickness assumed for the curve. The predicted failure temperatures (using actual geometry and actual temperature ramp) are within 15°C of the observed values (Fig. 9b). The experimental failure temperatures of the Electrosleeved tubes with throughwall notches appear to vary almost linearly with notch length (the curve is actually slightly concave downwards), as evident in Fig. 10, which is quite different from the predicted concave-upwards shape of the bounds.
6. Predicted Failure Temperatures for Severe Accidents

6.1 Case 6 (SBO severe accident)

As mentioned earlier, calculations were done for the temperature and pressure histories that closely simulate Case 6 of Ref. 1. Failure calculations were performed for the reference 7/8 in. dia. tube (wall thickness = 0.050 in. and Electrosleeve thickness = 0.038 in.) with 100% throughwall cracks of various lengths in the parent tube. The results, plotted in Fig. 11, show that failure temperatures for 100% throughwall 3, 2, 1 and 0.5 in. long cracks are 641, 647, 691 and 792°C, respectively. Since the surge line failure occurs when the tube temperature = 684°C, cracks ≤ 1 in. long are predicted to survive the Case 6 transient. Note that the reduction is 6°C in failure temperature going from a crack length of 2 in. to 3 in. and an additional 6°C from 3 in. to 6 in. Thus although the ANL correlation suggests that the failure temperature continuously decreases with increasing crack length, from a practical viewpoint the additional decrease beyond a crack length of 3 in. is negligible. It is anticipated that if the cracks were part-throughwall rather than 100% throughwall, the predicted failure temperatures would be significantly higher than those indicated in Fig. 11. Such calculations are currently in progress.

6.2 Case 20C (SBO with pump seal leakage)

The variation of temperature and pressure differential during an SBO severe accident with pump seal leakage (Case 20C) is shown in Fig. 12a. The variations of temperature, ligament flow stress and ligament stresses for various crack lengths with time are plotted in Fig. 12b for the reference 7/8 in. dia. tube (wall thickness = 0.050 in. and electrosleeve thickness = 0.038 in.) with 100% throughwall cracks. Since none of the ligament stresses exceed the flow stress before surge line failure, tubes with 0.5, 1 and 2 in. cracks are predicted to survive this transient.

7. Discussions and Conclusions

A flow stress-based model has been developed for predicting failure under expected severe accident transients. The model accounts for the loss of flow stress of the Electrosleeve with aging at high temperatures. Aging has been simulated using a grain growth model and the hardness data supplied by FTI on electrosleeve material aged at high temperatures. Thus, there is reason to expect some uncertainty in the calculated loss of flow stress with aging. FTI has suggested that the flow stress of Ni-200 at high temperature should provide a reasonable estimate for the flow stress of electrosleeve after grain growth. A comparison of flow stress data of Ni-200 and Ni-201 with the calculated flow stress of the electrosleeve for the Case 6 ramp rate (including the effect of aging) is shown in Fig. 13. The data for Ni-201 extends only to 649°C. The two FTI data points at 593°C and 760°C on aged electrosleeve material fall quite close to the Ni-200 curve. In the temperature range of interest for severe accidents, i.e., >650°C, the calculated aged flow stress curve is close to but a little below the Ni-200 flow stress curve. Note that the FTI data at 760°C on aged Electrosleeve falls below the Ni 200 curve and is closer to the calculated flow stress curve. Thus, the present estimates for loss of flow stress with aging are consistent with the FTI assumption for the severe accident transient.

Finite element analyses were conducted to validate the $m_p$ factor used in the model for calculating average ligament stress in single layer shells with part-through axial cracks. The same model showed that the $m_p$ factor for the Electrosleeve ligament in a 100% throughwall axial crack is reduced when the flow stress of the Electrosleeve is reduced compared to that of the parent tube. The reduction is greater for shorter cracks. Therefore, a flow stress and crack length-dependent correction factor was applied to the $m_p$ factor calculated with the ANL correlation that was developed originally for single layer shells.
Eleven high temperature tests simulating an SBO (Case 6) pressure and temperature ramp have been conducted on notched Electrosleeved tubes supplied by FTI. The test results indicate a leveling off of failure temperature with crack length beyond 2 - 3 in., which is consistent with the FEA results. The flow stress data at low temperatures supplied by FTI together with the ANL test results were used to derive an unaged flow stress curve of the electrosleeve from room temperature to high temperatures. The unaged flow stress curve was used in the model for predicting failure. All the test data fall within the upper and lower bounds calculated on the basis of limiting geometrical parameters observed in the specimens. Also, high temperature test data on notched unsleeved as well as notched Electrosleeved tubes reported by FTI can be predicted reasonably well by the flow stress model.

The reference Electrosleeved tube with throughwall axial cracks ≤ 1 in. in the parent tube is predicted to survive the postulated SBO (Case 6) transient until surge line failure. The same tube with throughwall axial cracks of any length ≤ 3 in. is predicted to survive the severe accident transient Case 20C until surge line failure. These maximum survivable crack lengths should be increased if the crack depth is less than 100% throughwall.

The failure temperature of the Electrosleeved tube under any severe accident scenario can be increased by increasing the electrosleeve thickness. For example, the effect of an increase of electrosleeve thickness from 0.038 in. to 0.043 in. on the ligament failure temperature of tubes with various throughwall axial cracks subjected to the reference Case 6 SBO ramp is shown in Fig. 14. There is an increase of 20 - 30°C in the failure temperature relative to that of the reference Electrosleeved tube depending on the crack length.

The proposed model with the unaged flow stress curve of the electrosleeve material reported here are valid for temperature ramps that are not significantly different from the ramp rate (12.5°C/min) used in the ANL tests because creep effects are neglected in the model. The rate effect that is predicted by the model is due to grain growth only. Predicted failure temperatures at ramp rates significantly different from 12.5°C/min will be accurate if grain growth effects predominate creep effects.

Acknowledgement

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References


Table 1  Summary of simulated severe accident tests conducted at ANL and FTI on notched Electrosleeved tubes

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Notch length (in.)</th>
<th>Notch depth (in.)</th>
<th>Tube wall thickness (in.)</th>
<th>Electrosleeve thickness (in.)</th>
<th>Failure temperature (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>BTF-21</td>
<td>0.5</td>
<td>0.0490</td>
<td>0.0490</td>
<td>0.0410</td>
<td>807</td>
</tr>
<tr>
<td>BTF-13</td>
<td>0.5</td>
<td>0.0492</td>
<td>0.0510</td>
<td>0.0400</td>
<td>806</td>
</tr>
<tr>
<td>BTF-4</td>
<td>1.0</td>
<td>0.0482</td>
<td>0.0510</td>
<td>0.0390</td>
<td>722</td>
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<tr>
<td>BTF-10</td>
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<td>0.0500</td>
<td>0.0520</td>
<td>0.0380</td>
<td>724</td>
</tr>
<tr>
<td>BTF-14</td>
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<td>0.0500</td>
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<td>0.0510</td>
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<td>0.0500</td>
<td>0.0380</td>
<td>653</td>
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<td>0.0510</td>
<td>0.0370</td>
<td>653</td>
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<td>0.0395</td>
<td>643</td>
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<td>0.0493</td>
<td>0.0510</td>
<td>0.0350</td>
<td>630</td>
</tr>
<tr>
<td>BTF-5*</td>
<td>3.0</td>
<td>0.0490</td>
<td>0.0490</td>
<td>0.0440</td>
<td>673</td>
</tr>
<tr>
<td>BTF-23**</td>
<td>0.5</td>
<td>0.049</td>
<td>0.050</td>
<td>0.035</td>
<td>731</td>
</tr>
<tr>
<td>BTF-25**</td>
<td>1.0</td>
<td>0.051</td>
<td>0.051</td>
<td>0.036</td>
<td>691</td>
</tr>
<tr>
<td>R.5.2**</td>
<td>2.0</td>
<td>0.051</td>
<td>0.050</td>
<td>0.036</td>
<td>611</td>
</tr>
</tbody>
</table>

*One tip of the notch in this specimen was about 0.1 in. from the end of the Electrosleeve  
**these tests were conducted by FTI

Table 2  Observed and initial predictions of failure temperatures for the FTI high temperature tests on pressurized unsleeved and Electrosleeved tubes.

<table>
<thead>
<tr>
<th></th>
<th>Electrosleeved Alloy 600 tube</th>
<th>Unsleeved Alloy 600 tube</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0.5 in. 100%TW</td>
<td>1.0 in. 100%TW</td>
</tr>
<tr>
<td>Observed failure temp.</td>
<td>731°C</td>
<td>691°C</td>
</tr>
<tr>
<td>Predicted failure temp.</td>
<td>766°C</td>
<td>728°C</td>
</tr>
</tbody>
</table>

* this test was held at 764°C until it failed after 82 min  
** predicted by the creep rupture model of NUREG/CR-6575
Fig. 1. Reference geometry for Electrosleeved steam generator tube with an axial crack.

Fig. 2. (a) Comparison of $m_p$ values calculated by ANL correlation with those by FEA for ratios of flow stress of Alloy 600 and the Electrosleeve of 1, 2, and 3, and (b) Assumed variation of the $m_p$ reduction factor with flow stress ratio (FSR). Symbols denote values calculated by FEA.
Fig. 3. (a) Variations of normalized Vickers Hardness Number (VHN) of the Electrosleeve material with time under isothermal aging at various temperatures and (b) variation of activation energy for the reciprocal of the time to onset of rapid reduction of flow stress (or grain growth) with temperature.

![Graph showing variations of normalized Vickers Hardness Number (VHN) and activation energy with time and temperature.]

Fig. 4. Flow stress (without aging) vs. temperature plot for electrosleeve material and Alloy 600. The Electrosleeve data (square symbol) at 760°C was estimated from tensile data on a single specimen pre-aged and tested at 760°C, using $n=0.33$. Note that this flow stress curve of the Electrosleeve was subsequently modified on the basis of ANL tests.

![Graph showing flow stress vs. temperature for Electrosleeve material and Alloy 600.]
Fig. 5. (a) Flow stress data on the Electrosleeve material pre-aged for various times at high temperatures. All the tensile tests were conducted at 343 °C, except for the test on the specimen pre-aged at 760 °C, which was conducted at 760 °C and (b) normalized flow stress vs. time-temperature parameter plot for electrosleeve material.

Fig. 6. Variation of calculated "nucleation" times to onset of rapid loss of flow stress (or grain growth) under isothermal aging with aging temperature for Hall-Petch exponents of $n = 0.33$ and $n = 0.40$, using a temperature-dependent activation energy given by the step function in Fig. 3b. Also shown are nucleation times for rapid loss of flow stress derived from the FTI data shown in Fig. 3a.
Fig. 7. Calculated variation and ANL test simulation of temperature during an SBO (case 6) severe accident transient.

Fig. 8. Original unaged flow stress curve (dashed line) of the Electrosleeve estimated from FTI tensile data before the ANL tests were conducted and revised unaged flow stress curve (solid line) of the Electrosleeve calculated using the ANL tests.
FIG. 10
Variation of FTL test failure temperatures and predicted upper and lower bounds to the failure temperatures with notch length.

FIG. 9
Prediction of ANL test failure temperatures and predicted upper and lower bounds to the actual test temperature ramp.

(a) Variation of ANL test failure temperatures with notch length and (b) comparison of actual and predicted failure temperatures of FTL tests using actual notch and predicted geometry and notch length.
Fig. 11. Predicted ligament failure temperature during the SBO severe accident (Case 6) vs. crack length for 100% throughwall cracks in the parent tube. Also shown is the tube temperature at the time of surge line failure.

Fig. 12. (a) Variation of temperature and pressure during SBO with pump seal leakage (case 20C) severe accident transient. Also shown is the time at surge line failure and (b) time variations of temperature, flow stress of the Electrosleeve, and average ligament stresses predicted for 100% throughwall cracks of length 0.5 in., 1 in., and 2 in. in the parent tube during the severe accident Case 20C. The flow stresses for times less than ≈228 minutes are > 60 ksi. Note that the ligament stresses are well below the flow stress up to surge line failure.
Fig. 13. Comparison of calculated flow stresses (including aging) of Electrosleeve (solid line) with flow stress data (long dash line with open symbols) of Ni 200 (Huntington) and Ni-201 (ASTM). Also shown are the flow stress of the unaged electrosleeve (short dashed line) and two FTI flow stress data (filled circle) on 30-min aged specimens.

Fig. 14. Effect of Electrosleeve thickness on the predicted ligament failure temperature of a tube with throughwall axial cracks.