Applications of One-Dimensional Models in Simplified Inelastic Analyses

By

S. A. Kamal, J. M. Chern, D. H. Pai
Foster Wheeler Energy Corporation

ABSTRACT

Recent experience in elevated temperature design/analysis indicates that a rigorous inelastic analysis may often be costly and time-consuming, especially when the structural shapes are complex. The simplified rules currently available in design guides are frequently overly conservative and thus are of limited usefulness in many practical design situations. As an effort to fill the gap between these two approaches, this paper presents an approximate inelastic analysis based on geometric simplification with emphasis on its applicability, modeling, and the method of defining the loading conditions. Two problems are investigated: a one-dimensional axisymmetric model of generalized plane strain thick-walled cylinder is applied to the primary sodium inlet nozzle of the Clinch River Breeder Reactor Intermediate Heat Exchanger (CRBRP-IHX), and a finite cylindrical shell is used to simulate the branch shell forging ("Y") junction. The results are then compared with the available detailed inelastic analyses under cyclic loading conditions in terms of creep and fatigue damages and inelastic ratchetting strains per the ASME Code Case N-47 requirements. In both problems, the one-dimensional simulation is able to trace the detailed stress-strain response. The quantitative comparison is good for the nozzle, but less satisfactory for the "Y" junction. Refinements are suggested to further improve the simulation.

*Research performed under Subcontract No. 7463 with Foster Wheeler Energy Corporation under Union Carbide Corporation contract W-7405-eng-26 with the U.S. Department of Energy.
INTRODUCTION

The ASME Code Case N-47 [1] requires the design of Class 1 components in elevated temperature service to satisfy limits on inelastic strains, and creep and fatigue damages. To demonstrate the compliance of these limits, a typical stress analysis consists of a thermal analysis to determine the thermal load, and an elastic analysis to identify the critical sections of the structure and to qualify (or disqualify) the use of elastic rules; if necessary, an inelastic analysis is then performed. The first two analyses are relatively simple and can be performed with sufficient structural details using available general-purpose finite element computer programs. However, an inelastic analysis often requires great analytical effort and can be performed only for confined areas of the structure due to cost and schedule considerations. Even this type of inelastic analysis is often considered too costly and time-consuming, and further simplifications are desirable.

Currently available techniques, such as the elastic analysis rules in N-47, are too conservative to be useful for many LMFBR applications. The Bree and O'Donnell-Porowski type of approach [2] is useful for situations where ratchetting is the main concern. However, the method is applicable only when the dominant secondary stress distribution is of the through-the-wall gradient type. For many LMFBR applications, high stresses arise from axial temperature gradients and/or geometry discontinuities, and the dominant failure modes are creep damage and creep-fatigue interaction. In these situations, the Bree and O'Donnell-Porowski type of approach is no longer applicable.

* Numbers in brackets designate references listed at the end of this paper.
Simplified inelastic analyses that focus on the critical areas of a structure have been investigated using a generalized plane strain model [3] and a finite cylindrical shell model [4]. The applicability of this type of simplified one-dimensional model was also investigated recently for a variety of structural components [5]. Reference [5] has indicated that the simplified analysis technique using one-dimensional models may yield non-conservative results when applied to abrupt geometric change such as a notch.

In the present investigation, the approach using simplified structural models is applied to two components in the Clinch River Breeder Reactor Intermediate Heat Exchanger (CRBRP-IHX); the primary sodium inlet nozzle and branch shell forging ("Y") junction. Results of thermal-elastic analysis and inelastic analysis using detailed finite element models are available for these two components [6,7]. The results of thermal-elastic analysis of the detailed model are used in the simplified analysis to identify the critical sections and to define the loading histograms. The accuracy of the simplified analysis was then assessed based on the results of the detailed inelastic analysis. In the case of the primary nozzle, the dominant thermal load is due to the through-the-wall temperature gradient and therefore the generalized plane strain model is used in the simplified analysis. On the other hand, a finite cylindrical model is used in the case of the Y-junction where the axial temperature gradient becomes dominant.
GENERALIZED PLANE STRAIN THICK-WALLED CYLINDER

A thick-walled circular cylinder under generalized plane strain condition has been proposed in Reference [3] to model critical sections in the LMFBR components subjected to severe thermal transients. The application of this approach using a special purpose computer code [8] to a critical section of the FFTF-IHX primary inlet nozzle has shown that the results compare favorably with the detailed inelastic analysis of the nozzle. As indicated in Reference [3], the use of this simplified model is appropriate when the thermal transients in the actual components are characterized by a dominant through-the-wall temperature gradient. This approach is herein extended and applied to the CRBRP-IHX primary inlet nozzle.

The primary inlet nozzle of the CRBRP-IHX is welded to the primary shell. Both the nozzle and the shell are made of type 316 stainless steel and subjected to the pressure and temperature of the primary sodium. A combined thermal and elastic analysis of the nozzle and shell was previously performed [7] with an axisymmetric model in which the primary shell was modeled as a spherical shell with twice the radius of the actual cylindrical shell to conservatively simulate the maximum pressure stresses. It was revealed that the stresses were generally too high to satisfy the elastic analysis rules of Code Case N-47, and an inelastic analysis was required. The detailed inelastic analysis [8] was performed using the axisymmetric model of Figure 1 and a finite element program [9].

From the thermal-elastic analysis of the detailed model, we found that the most critical areas were in the weld region and the inlet portion
of the nozzle. Specifically, based on the ranges of secondary stress and primary-plus-secondary stress, the most critical locations were found on the inner surfaces of section A in the weld region and section B in the inlet nozzle as indicated in Figure 1.

The loading of the axisymmetric finite element model, shown in Figure 1, consists of: 1) the thermal load induced by the fast transient events 1U and 2U, 2) the nozzle load $F_z$ resulted from the deadweight and thermal expansion of the attached piping system, and 3) the primary sodium pressure $p_1$. The loading histogram used in the detailed inelastic analysis is shown in Figure 2. The basic unit cycle consists of five (5) sub-cycles, each of which includes a slow heat-up from hot standby condition followed by a hold-time of 303 hours at 100% power condition. The down-shock thermal transients 1U and 2U represent the upset conditions of reactor trips from full power with normal and minimum decay heats \([7,10]\), respectively. In addition, creep deformation is to take place during the hold-time at 100% power condition. It is postulated that this unit cycle is to occur 174 times in the lifetime of the IHX.

In the detailed inelastic analysis, the instantaneous elastic-plastic material properties were assumed temperature-dependent and the creep equation of rational polynomial type was used for creep strain calculation. The analysis was carried out for six (6) unit cycles including 9090 hours of creep; conservative extrapolations were then used to extend the results to 174 unit cycles. Results obtained from this detailed inelastic analysis agreed with the elastic analysis on the locations of the most critical areas. For instance, the inelastic analysis did indicate that the largest creep damage occurs at sections A and B, and the largest inelastic strain
occurs at section A. Simplified inelastic analyses using the axisymmetric generalized plane strain model was performed to simulate the stress and strain behaviors at the two critical locations A and B.

**Structural Modeling**

The dimensions of the simplified model, namely the mean radius \( r \) and thickness \( t \), are determined by the normal to the middle surface of the nozzle, as illustrated in Figure 3. With these dimensions, a pressure load will produce approximately the same elastically calculated membrane stresses in the simplified models and at the simulated sections of the actual nozzle.

**Loading Conditions**

The loading applied to the simplified model, as shown in Figure 3, consists of: 1) an equivalent internal pressure \( p_i^* \), 2) an equivalent through-the-wall temperature distribution \( T^*(r) \), and 3) an axial force with mean axial stress \( Q^* \). The values of these load parameters at each load step of the loading histogram are obtained from the elastically calculated stresses and the temperature distributions at the critical sections being simulated. Two separate simplified analyses corresponding to surface condition and average condition have to be performed in the simplified approach.

**Surface Condition Analysis.** In order to evaluate the creep-fatigue damage and the 5% limit on peak inelastic strain which occur on the surface, the simplified model should simulate the stress condition on the surface of the
critical section. The stress values are selected from the inner or outer surface, whichever gives the higher secondary stress range.

As a result of thermal-elastic analysis of the detailed model, all the stresses due to the separate effects of pressure \( p_i \), axial force \( F_z \), and thermal transients are available and these stresses can also be separated into membrane and bending components. The pressure load \( p_i^* \) and through-the-wall temperature gradient \( \Delta T_\theta \) for the simplified model are derived on the basis of the circumferential stress of the detailed model:

\[
\sigma_{\theta\text{tot}} = \sigma_{\theta p} + \sigma_{\theta F} + \sigma_{\theta T}^s
\]  
(1)

where the superscript \( s \) denotes the combined membrane and bending stress at the surface, and the subscripts, \( p \), \( F \), and \( T \) denote the effects due to pressure load, axial force and thermal transient. Since \( p_i^* \) is to define the primary membrane stress and \( \Delta T_\theta \) the thermally-induced bending stress, we note the relation

\[
\sigma_{\theta T}^s = \sigma_{\theta T}^m + \sigma_{\theta T}^b
\]  
(2)

\( \sigma_{\theta T}^m \) and \( \sigma_{\theta T}^b \) being respectively the membrane and bending components of the thermally induced circumferential stress. Thus, we derive the expressions for \( p_i^* \) and \( \Delta T_\theta \) as

\[
p_i^* = (\sigma_{\theta\text{tot}}^s - \sigma_{\theta T}^s) \cdot \frac{t}{r} \quad (t \ll \frac{r}{E \alpha})
\]  
(3)

\[
\Delta T_\theta = \frac{2(1-\nu)}{E \alpha} \sigma_{\theta T}^b
\]  
(4)
where \( t \) and \( \bar{r} \) are the thickness and mean radius of the simplified model, and \( E \), \( \alpha \), and \( \nu \) are respectively the elastic modulus, coefficient of thermal expansion and Poisson's ratio of the structural material. Since the actual temperature distribution \( T(r) \) at the critical section has an equivalent gradient \( \Delta T_r \) which may differ from \( \Delta T_\theta \) given by Eq. (4), the equivalent temperature distribution \( T^*(r) \) of the simplified model is therefore conservatively defined as

\[
T^*(r) = T(r) + \frac{(\Delta T_\theta - \Delta T_r)}{t} (r - \bar{r})/t \quad \text{if} \quad \Delta T_\theta > \Delta T_r,
\]

\[
T^*(r) = T(r) \quad \text{if} \quad \Delta T_\theta < \Delta T_r
\]

It should be noted here that the actual peak stress at the surface will generally differ from the combined membrane and bending stress. This difference is assumed to be negligibly small except for the thermal loading. To preserve the thermal peak stress, the deviation of the actual temperature distribution \( T(r) \) from linear variation has been included in the equivalent temperature distribution \( T^*(r) \) in the above definition.

The expression for the mean axial stress \( \bar{Q}^* \) of the simplified model is based on the meridional stress of the actual model. Similar to Eq. (1), we have

\[
\bar{\sigma}_{\phi_{\text{tot}}} = \bar{\sigma}_{\phi_P} + \bar{\sigma}_{\phi_F} + \bar{\sigma}_{\phi_T}
\]

Since the temperature gradient \( \Delta T_\theta \) defined previously will give rise to an axial bending stress \( \bar{\sigma}_{\phi_T} \) on the surface of the simplified model approximately equal to the circumferential bending stress \( \bar{\sigma}_{\theta T} \), the meridional stress can be reproduced in the simplified model if \( Q^* \) is defined as
Using Eq. (6) and separating $\sigma_s^T$ into the membrane and bending components $\sigma_s^T$ and $\sigma_{T\theta}^T$, we can rewrite the above expression in the form:

$$Q^* = \sigma_{\phi_{\text{tot}}}^s - \sigma_{\theta T}$$

(7)

It should be noted that the stress difference in the parentheses of the above equation should be small in order to justify the use of generalized plane strain model.

**Average Condition Analysis.** For the evaluation of the inelastic strain accumulations corresponding to the 1% and 2% limits that reflect the complete sectional behavior, the load parameters for the simplified model are derived from the membrane stresses at the critical section of the actual model. With the same notations as in the surface condition analysis, the membrane components of the circumferential and meridional stresses obtained from the thermal-elastic analysis can be expressed as

$$Q^* = \sigma_{\phi P}^s + \sigma_{\phi F}^s + \sigma_{\phi T}^s + (\sigma_{\phi T}^s - \sigma_{\theta T})$$

(8)
\[ \bar{\sigma}_{\theta_{\text{tot}}} = \bar{\sigma}_{\theta_{p}} + \bar{\sigma}_{\theta_{F}} + \bar{\sigma}_{\theta_{T}} \]  
(9)

\[ \bar{\sigma}_{\phi_{\text{tot}}} = \bar{\sigma}_{\phi_{p}} + \bar{\sigma}_{\phi_{F}} + \bar{\sigma}_{\phi_{T}} \]  
(10)

Thus,
\[ Q^* = \bar{\sigma}_{\phi_{\text{tot}}} \]  
(11)

\[ p_{1}^* = \frac{\bar{\sigma}_{\theta_{\text{tot}}}}{t} \frac{1}{\bar{r}} \]  
(12)

The equivalent temperature distribution \( T^*(r) \) in the simplified model is conservatively selected to give the maximum possible thermal bending stress:

\[ T^*(r) = T(r) + (\Delta T_{\phi} - \Delta T_{r})(r - \bar{r})/t \text{ if } \Delta T_{\phi} > \Delta T_{r}, \]

\[ T^*(r) = T(r) \text{ if } \Delta T_{\phi} \leq \Delta T_{r} \]  
(13)

where \( \Delta T_{\phi} \) is an equivalent temperature gradient calculated from thermally induced bending stresses:

\[ \Delta T_{\phi} = \frac{2(1 - \nu)}{E \alpha} \max (\bar{\sigma}_{\theta_{T}}, \bar{\sigma}_{\phi_{T}}) \]

Results

Simplified inelastic analyses for the two critical sections were performed for six (6) unit load cycles as in the detailed inelastic analysis. To be consistent with the detailed inelastic analysis (DA), the creep damage \( D_{C} \) in the simplified analysis (SA) was calculated by
assuming a linear time-variation of effective stress between the beginning and the end of each hold-time, and the effects of exposure to sodium and interstitial transfer of carbon and nitrogen were also accounted for by applying appropriate stress factors [7]. Also, to avoid the effect of approximations that might be introduced in the detailed model by extrapolating the stress distribution to the surface, the comparisons of stress and creep damage are made at the integration point of the detailed model nearest the surface that is located at 0.028t from the inner surface (Figure 1). Other comparisons were made at the surface. It should also be noted that although the analyses were carried out for only six (6) unit cycles, the predictions were extrapolated to the end of design life by a conservative scheme based on the cyclic change at the sixth unit cycle. While a complete comparison of the simplified and detailed analyses are given in Reference [11], including the contribution due to each transient event in each unit cycle, the comparison presented in this paper is concerned mainly with the end-of-life prediction.

Let us first examine the creep damage factors. Table 1 shows the \( D_c \) values for the simplified and detailed analyses of sections A and B. The SA predictions are conservative compared to DA at both sections by about 8% at section A and 17% at section B. This good agreement in the prediction of creep damage factor is due to the ability of the simplified analysis to closely simulate the stress variations during the creep hold-time. For instance, the effective stress variations at section A are compared in Figure 4 for the first unit cycle, where the simplified results for both surface condition and average condition are shown and the sign of hoop stress is attached to the effective stress to make the comparison
more illustrative. (Note that the results based on surface condition, which should be used for creep damage evaluation, evidently have better agreement with the detailed analysis.) It is seen that the simplified analysis can simulate the magnitude and detailed trend of the stress history in a complete unit cycle. The comparison for section B follows a similar trend.

The fatigue damage factors $D_f$ are also compared in Table 1 for sections A and B. The agreement between the SA and DA results is excellent at section A but the SA results seem to be overly conservative at section B. Since the fatigue damage is sensitive to the equivalent strain range, when the latter quantity is small as in the present case, a slight difference in the strain range determined in the analysis tends to result in a large difference in fatigue damage. Indeed, a better agreement for the equivalent strain range between the SA and DA analyses was demonstrated in Reference [11].

Since the fatigue damage is insignificant compared to the creep damage, the combined creep-fatigue damage in the nozzle follows the trend of the creep damage. For the prediction of this combined damage factor, the simplified analysis is found to be conservative by 8% at section A and 19% at section B over the detailed analysis.

In inelastic strain accumulations corresponding to the 1%, 2% and 5% limits at sections A and B of the nozzle are compared in Table 2. Since section A is located in the region of the weld, the strain limits are reduced by a factor of 0.5 for this section. The strain accumulations at the end of lifetime are conservatively calculated based on the results of the first six unit cycles by extrapolation. Since the extrapolation is performed independently for each category of strain limit the results may become inconsistent in that the cyclic growth for the 5% category may be smaller than the growth for the 2% category, and,
similarly, the growth of the 2% category may be smaller than that for the 1% category. To eliminate such inconsistency, the extrapolated strain accumulation for the 2% category is used for both 5% and 2% categories when the former prediction is less than the latter prediction. Similarly, the extrapolated strain accumulation for the 1% category is used for both 2% and 1% categories when the aforementioned inconsistency is revealed. It is indicated by Table 2 that at section A the simplified analysis differs from the detailed analysis by a range of 122% (conservative) to -23% (nonconservative), while the difference at section B ranges from 221% (conservative) to -33% (nonconservative).

In an effort of trying to understand why a considerably wide range of discrepancy exists between the simplified and detailed analysis predictions of inelastic strain accumulations, something pertinent to the inelastic analysis in general was discovered. The basic reason for this discrepancy was found to be that the creep and plastic strains have cyclic changes of similar magnitude but opposite signs. While the simplified analysis can give reasonably accurate predictions for creep strain and plastic strain separately, the error in the prediction of combined inelastic strain can be greatly magnified [11]. This type of numerical difficulty may also persist in the detailed inelastic analysis since it usually cannot be assured that the creep and plastic strains are determined with the same accuracy.
Discussion

The simplified analysis procedures presented in this section have been developed through a series of systematic investigations. While it seems to be an obvious engineering judgement to select the generalized plane strain model for the simplified analysis of an inlet nozzle which is subjected to a thermal loading dominated by radial temperature gradient, it has taken considerable effort to select the appropriate loading conditions for the simplified model in order to achieve reasonably accurate predictions in the various categories of design limit. The average condition analysis is basically the approach adopted in Reference [3] with the improvement that the radial temperature gradient is to simulate the
larger bending stress in the circumferential and meridional directions rather than the circumferential bending stress alone. In such an approach, the mechanical load parameters (i.e., the pressure and axial loads) are derived from the membrane stresses of the detailed elastic model. However, this approach did not give a sufficiently close prediction of the stresses during the creep hold-time at section A. The surface condition analysis was then adopted on the basis that:

1) Since, with a few exceptions of heavy section, the locations that are critical in creep-fatigue damage and peak inelastic strain accumulation are always found on the surface, the load parameters should be derived from the surface stress condition for the predictions of these categories;

2) Since the circumferential and meridional bending stresses may be substantially different, the effect on the axial stress due to the temperature gradient introduced in the simplified model should be corrected when the axial load is defined.

In the selection of temperature distribution, it was first attempted for the sake of simplicity to use only a linear variation across the section that was constructed with the mean temperature of the section and the through-the-wall temperature gradient. However, realizing that the temperature gradient duplicates only the bending stress but not the peak stress on the surface, it was decided to preserve the nonlinear distribution of the actual temperature.

Finally, it should be noted that the simplified analysis proposed herein requires the following two quantities of the elastic results be small:
1) the meridional bending stress due to mechanical load;
2) the difference between the meridional and circumferential bending stresses induced by thermal loads.

The first restriction is due to the assumption in the simplified model that the axial stresses are simulated in terms of their resultant but not the actual distribution. This restriction can be relaxed if the simplified model is extended to include the axisymmetric bending moment as a load parameter. The second restriction is due to the inherent nature of generalized plane strain model in which the radial temperature gradient will cause the same elastically calculated bending stresses in both axial and circumferential directions.

**FINITE CIRCULAR CYLINDRICAL SHELL**

A formulation for the elastic-plastic-creep analysis of a thin, finite circular cylindrical shell is given in Reference [4] and the associated computer code [12] is also available. This simplified structural model is herein applied to the Y-junction of the CRBRP-IHX in which the axial temperature gradient is a dominant load.

The Y-junction of the CRBRP is a forging made of type 304 stainless steel. This junction connects the inner junction of the upper tubesheet to the inner cylinder of the upper channel complex and to the thermal liner. The outer and inner surfaces of this junction are in contact with the intermediate and primary sodium, respectively, while the annular gap between the upper channel cylinder (outer leg) and the thermal liner (inner leg) is filled with stagnant nitrogen at atmospheric pressure. A finite element model for this junction is shown in Figure 5.
The loading of the Y-junction component consists of: 1) temperature gradients in both radial and meridional directions as a result of thermal transient events NC, 2E and 1U, and under the 100% power condition; 2) pressures of primary and intermediate sodium; and 3) blow-off axial force applied at the boundary ends of the inner and outer legs. The loading histogram for a typical unit load cycle is as shown in Figure 6. Each of the three subcycles in this unit load cycle includes a slow heat-up from hot standby followed by 303 hours of creep hold-time at 100% power condition. The slow normal cooldown NC and the downshock emergency and upset transient events, 2E and 1U, bring the operating condition back to the hot standby. Creep is assumed to take place only at the 100% power condition. The unit load cycle is postulated to occur 290 times during the lifetime of the IHX.

The thermal elastic analysis [7] based on detailed finite element model has showed that the stresses were too high to satisfy the elastic analysis rules of Code Case N-47. It was also indicated that the most critical area was at the bottom area of the outer leg where the secondary stress range for the 2E and 1U transient events became maximum, and the primary-plus-secondary stresses during the creep hold-time were also high. Specifically, the most critical locations were found at the inner surface of section A and the outer surface of section B as indicated in Figure 5.

A detailed inelastic analysis was subsequently performed [8] using the axisymmetric finite element model of Figure 5. The material properties used were temperature-dependent and the creep correlation was of the Black-burn double exponential type. The analysis was carried out for six (6)
unit cycles that included 5,454 hours of creep time at 100% power condition. The results indicated that the largest inelastic strain accumulation occurred at the bottom of the inner leg while the largest creep-fatigue damage occurred near the bottom of the outer leg. In the outer leg, the inner surface of section A had the highest effective stress during the hold-time; however, a larger creep damage was found at the outer surface of section B because of the higher temperature and the exposure to sodium. This is consistent with the indication of the thermal elastic analysis. The simplified analysis was performed only for the outer leg using the special purpose computer code [12].

Structural Modeling

A close examination of the thermal analysis results indicates that the thermal stresses in the outer leg of the Y-junction are induced mainly by the meridional, rather than radial, temperature gradients. Therefore, a finite circular cylindrical shell is an appropriate simplified model because of its ability in simulating the axial as well as radial temperature gradients. The simplified model for the outer leg of the Y-junction is shown in Figure 7. The mean radius $r$ and the thickness $t$ are simply those of the outer leg. The length $L$ is chosen so as to simulate the free end conditions at the upper end of the outer leg. The displacement boundary conditions at the lower end ($x = 0$) are to be specified by the thermal elastic analysis results of the actual model.
Loading Conditions

The temperature distributions in the radial and axial directions of the simplified model are extracted from the actual model and, for simplicity, a linear through-the-wall temperature gradient is assumed. The axial force and pressure load are chosen so that they respectively simulate the average meridional and circumferential stresses at $x = L$ of the actual model. To reflect the strain-controlled nature of the major loading of the Y-junction, boundary conditions of displacement type are applied at the lower end of the simplified model. Thus, both radial displacement ($w$) and meridional rotation ($dw/dx$) obtained in the finite element model are used to specify the boundary conditions at $x = 0$.

Results

The simplified inelastic analysis was performed for six (6) unit cycles of the loading histogram shown in Figure 6. In view of the simplified nature of the analysis, the elastic-plastic material properties at the average temperature of the loading cycle were used.

To assess the general capability of the simplified analysis in predicting the creep damage (which depends on the effective stress and temperature during the creep hold-time), the effective stress histories at the integration point nearest the inner surface of section A are compared with the detailed analysis in Figure 8. As can be seen from this figure, the SA can indeed simulate the general trend of stress history in a complete unit cycle, however the rapid stress relaxations that are found to persist in the SA will result in reduced creep damage prediction. The results at section B follow the same trend.
The end-of-life creep damage factors predicted by the SA and DA analyses at the two critical locations are compared in Table 3. The creep damage is under-predicted by the SA by 40% at section A and over-predicted by about 1% at section B.

The end-of-life predictions of fatigue damage are also compared in Table 3. The SA predicts a total fatigue damage of 0.180 (vs. 0.008 per DA) at section A and 0.764 (vs. 0.074 per DA) at section B which are conservative by factors of 22.5 and 10.3, respectively, over the DA results.

The inelastic strain accumulations predicted by the SA and DA are compared in Table 4. Since all the first six cycles results indicate decreasing cyclic growths, the end-of-lifetime predictions are conservatively based on the cyclic change at the sixth unit cycle [8]. According to these conservative estimates, the SA is found to under-predict the inelastic strain accumulations at section A by a range from -81% to -27% among the categories of 5%, 2% and 1% limits; the under-predictions at section B range from -81% to -50%. It should be mentioned here that the very conservative rules are applied to derive the lifetime predictions for a simple comparison between the two analyses; substantial reduction in the predictions is certainly possible in design evaluation when the trend of cyclic change is properly accounted for [8].

Discussion

The simplified inelastic analysis performed for the Y-junction was shown to be capable of simulating the trend for the time-variation of the stress conditions in a complete unit cycle, whereas the detailed
predictions have been less satisfactory than in the case of the primary nozzle. However, it is demonstrated in Reference [11] that the thermal transient considered is so severe that the other alternative methods provided by the RDT F9-5T are not applicable.

The nonconservative nature of the present simplified analysis is attributed to the displacement boundary conditions at the lower end of the cylindrical shell. These boundary conditions were selected over the force boundary conditions on the basis that the stresses in the Y-junction are mainly caused by thermal loading and interaction with other components in the structural system, which are of a strain-controlled nature. As expected, the stress relaxation in the simplified model was found to be faster than that in the detailed model, thus resulting in smaller inelastic strain and creep damage. The force boundary conditions are expected to give predictions that are too conservative to be useful. A compromise approach that will improve the results is to perform two separate analyses. In the first analysis the displacement boundary conditions are applied as before but the creep effect is neglected. The stress history obtained in this analysis is then used to specify the force boundary conditions for the second analysis in which both the plastic and creep effects are considered. In this manner, the over-stressing that would be introduced by the force boundary conditions can be eliminated by the first analysis while the slower stress relaxation can be preserved by the second analysis.
CONCLUSIONS AND RECOMMENDATIONS FOR FUTURE WORK

In the present investigation, the simplified structural models of axisymmetric thick-walled cylinders under generalized plane strain condition and cylindrical shell have been applied to the practical design of the CRBRP-INX components. Comparisons made with the currently available results of detailed inelastic analysis have indicated that the trend of the time-variation of stress in a complete load cycle can be simulated. The qualities of simplified prediction are found to vary depending on the simplified model and the category of design limit.

In the first application where the thick-walled cylinder was used to simulate the two critical sections of the primary inlet nozzle, it was found that:

1) For the creep damage, the simplified analysis is conservative by 8% at section A and 17% at section B for the entire lifetime;

2) For the fatigue damage, the conservatism became 20% at section A and 160% at section B;

3) For the combined creep and fatigue damage, the simplified analysis is conservative by 8% at section A and 22% at section B;

4) For the various categories of inelastic strain accumulation, the difference between the simplified and detailed analyses at Section A ranges from 122% (conservative) to -23% (nonconservative), while the difference at Section B ranges from 221% (conservative) to -33% (nonconservative).
The use of thick-walled cylinder model for this application is successful from the standpoint of uniform accuracy, and is reliable since the stress history pattern can be traced. However, in view of the less accurate prediction in inelastic strain accumulation, the thick-walled cylinder model should be refined to extend its capability to account for the axisymmetric bending moment resulting from the radial distribution of the axial stress.

In the second application, where the cylindrical shell model was utilized to simulate the two critical locations of the Y-junction, it was found that:

1) For the creep damage, the simplified analysis is nonconservative by 40% at section A and conservative by 1% at section B; however, since section B is more critical than section A, the simplified analysis is still slightly conservative in terms of maximum creep damage.

2) For the fatigue damage, the simplified analysis is conservative by one order of magnitude;

3) For the combined creep and fatigue damage, the simplified analysis is conservative by 30% at section A and by 153% at section B.

4) For the various categories of inelastic strain accumulation, the simplified analysis is persistently nonconservative by a range from 27% to 81% at section A, and from 50% to 81% at section B.

It is seen in this application that the predictions are less satisfactory than in the case of thick-walled cylinder model, especially in inelastic strain accumulations. It is noted, however, that due to the
severity of thermal loading and elastic follow-up that the Y-junction is subjected to, the other alternative methods provided in the RDT Standard F9-5T for evaluation of ratchetting strain are not applicable.

Although the current simulation using the finite cylinder model was found to be nonconservative in inelastic strain accumulations, it was able to trace the general trend of the stress behavior history. Therefore, further improvement of the present method seems worthwhile. To refine the modeling technique, the simplified inelastic analysis should be performed in two steps. In the first step, the displacement boundary conditions are used and the analysis is performed without considering the creep effect. The force boundary conditions derived from the first analysis should then be applied to the second analysis which takes full consideration of elastic-plastic-creep material properties.
REFERENCES


REFERENCES (Continued)


ACKNOWLEDGEMENTS

Results presented in this paper were obtained from work performed under DOE/RRT 189a OH048, High-Temperature Structural Design Methods. We wish to thank Mr. T. G. Yahr and Dr. J. M. Corum of Oak Ridge National Laboratory for their guidance and suggestions during the course of this work.
### TABLE 1 CREEP AND FATIGUE DAMAGE FACTORS OF THE PRIMARY INLET NOZZLE

<table>
<thead>
<tr>
<th>Section</th>
<th>Analysis</th>
<th>Creep Damage, $D_c$</th>
<th>Fatigue Damage, $D_f$</th>
<th>Combined Creep and Fatigue Damage $D_{cf}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>DA</td>
<td>0.596</td>
<td>0.010</td>
<td>0.619</td>
</tr>
<tr>
<td></td>
<td>SA</td>
<td>0.641</td>
<td>0.012</td>
<td>0.669</td>
</tr>
<tr>
<td>B</td>
<td>DA</td>
<td>0.324</td>
<td>0.005</td>
<td>0.336</td>
</tr>
<tr>
<td></td>
<td>SA</td>
<td>0.380</td>
<td>0.013</td>
<td>0.410</td>
</tr>
</tbody>
</table>

**Note:** The combined damage is defined for 304 and 316 stainless steels as

\[
D_{cf} = D_c + \frac{7}{3} D_f \quad \text{for } D_c \geq D_f
\]

\[
= \frac{7}{3} D_c + D_f \quad \text{for } D_c < D_f
\]

where, according to Reference [1], the maximum allowable value of $D_{cf}$ is 1.
Table 2. Inelastic Strain Accumulations (%) of Primary Inlet Nozzle

<table>
<thead>
<tr>
<th>Section</th>
<th>Analysis</th>
<th>Peak Strain 5% Limit</th>
<th>Surface Strain* 2% Limit</th>
<th>Average Strain 1% Limit</th>
</tr>
</thead>
<tbody>
<tr>
<td>A**</td>
<td>DA</td>
<td>0.856</td>
<td>0.833</td>
<td>0.290</td>
</tr>
<tr>
<td></td>
<td>SA</td>
<td>0.681</td>
<td>0.679</td>
<td>0.644</td>
</tr>
<tr>
<td>B</td>
<td>DA</td>
<td>0.263</td>
<td>0.198</td>
<td>0.130</td>
</tr>
<tr>
<td></td>
<td>SA</td>
<td>0.843</td>
<td>0.132</td>
<td>0.101</td>
</tr>
</tbody>
</table>

Notes: * The larger value of the inner and outer surfaces is selected.

** Since Section A is located in a weld region, the strain limits should be reduced by a factor of 0.5.
### TABLE 3. CREEP AND FATIGUE DAMAGE FACTORS OF THE Y-JUNCTION

<table>
<thead>
<tr>
<th>Section</th>
<th>Analysis</th>
<th>Creep Damage, $D_c$</th>
<th>Fatigue Damage, $D_f$</th>
<th>Combined Creep and Fatigue Damage, $D_{cf}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>DA</td>
<td>0.564</td>
<td>0.008</td>
<td>0.583</td>
</tr>
<tr>
<td></td>
<td>SA</td>
<td>0.340</td>
<td>0.180</td>
<td>0.760</td>
</tr>
<tr>
<td>B</td>
<td>DA</td>
<td>0.890</td>
<td>0.074</td>
<td>1.063*</td>
</tr>
<tr>
<td></td>
<td>SA</td>
<td>0.902</td>
<td>0.764</td>
<td>2.684</td>
</tr>
</tbody>
</table>

Note: * Value based on the most conservative assumptions. Substantial reduction is possible in design evaluation when the trend of cyclic change is accounted for.[8].
### Table 4. Inelastic Strain Accumulations (%) of Y-Junction

<table>
<thead>
<tr>
<th>Section</th>
<th>Analysis</th>
<th>Peak Strain 5% Limit</th>
<th>Surface Strain* 2% Limit</th>
<th>Average Strain 1% Limit</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>DA</td>
<td>2.707</td>
<td>2.228**</td>
<td>0.651</td>
</tr>
<tr>
<td></td>
<td>SA</td>
<td>1.223</td>
<td>1.145</td>
<td>0.125</td>
</tr>
<tr>
<td>B</td>
<td>DA</td>
<td>2.588</td>
<td>2.355**</td>
<td>0.708</td>
</tr>
<tr>
<td></td>
<td>SA</td>
<td>0.790</td>
<td>0.702</td>
<td>0.132</td>
</tr>
</tbody>
</table>

**Notes:**
- * The larger value of the inner and outer surfaces is selected.
- ** Values based on the most conservative assumptions. Substantial reduction is possible in design evaluation when the trend of cyclic change is accounted for [8].
FIGURE 1
FINITE ELEMENT MODEL FOR DETAILED INELASTIC ANALYSIS OF PRIMARY INLET NOZZLE

Shell Elements of Type 15 [9]

Internal Pressure $P_i$

Solid Elements of Type 28 [9]

Circumferential Weld to the Primary Shell

$\frac{dv}{ds} = 0$

$u = 0$

$\frac{dv}{ds} = 0$

Shell Elements
FIGURE 2
UNIT HISTOGRAM FOR INELASTIC ANALYSIS OF PRIMARY INLET NOZZLE

TRANSIENTS
1U - Reactor trip from full power with normal decay heat.
2U - Reactor trip from full power with minimum decay heat.
FIGURE 3

DIMENSIONS AND LOADING IN SIMPLIFIED INELASTIC ANALYSIS OF PRIMARY INLET NOZZLE
FIGURE 4

EFFECTIVE STRESS VARIATION DURING FIRST UNIT CYCLE AT SECTION A IN PRIMARY INLET NOZZLE

NOTES:
C1: Creep for 303 hrs
2U & 1U: Thermal Transients
See Figure 3 for Unit Histogram.
Figure 5

Finite element model for detailed inelastic analysis of Y-junction.
Figure 6

Unit Histogram for Inelastic Analysis of Y-Junction

Transients

2E - Emergency Condition, Loss of Primary Pony Motor with Valve Failure.
1U - Upset Condition, Trip from Full Power with Normal Decay Heat.
FIGURE 7

DIMENSIONS AND LOADINGS IN SIMPLIFIED ANALYSIS OF Y-JUNCTION

\[ \overline{r} = 48.74 \text{ cm (19.19 in.)} \]
\[ t = 4.78 \text{ cm (1.88 in.)} \]
\[ L = 49.58 \text{ cm (19.52 in.)} \]
FIGURE 8

EFFECTIVE STRESS VARIATION DURING FIRST UNIT CYCLE AT SECTION A IN Y-JUNCTION

NOTES:
C: Creep for 301 hrs
U: Normal Cooldown
2E & 1U: Thermal Transients
See Figure 7 for Unit Histories.