ABSTRACT

In this paper, the effect of residual stress on the initiation of a crack at high temperature in a Type 347 austenitic steel weld is examined using the finite element method. Both two and three dimensional analyses have been carried out. Residual stresses have been introduced by prior mechanical deformation, using a previously developed notched compact tension specimen. It has been found that for the 347 weld material, peak stresses in the vicinity of the notch are approximately three times the yield strength at room temperature and the level of stress triaxiality (ratio between hydrostatic and equivalent stress) is approximately 1 (considerably higher than that for a uniaxial test). The finite element analysis includes the effects of stress redistribution and damage accumulation under creep conditions. For the case examined the analysis predicts that crack initiation will occur under conditions of stress relaxation if the uniaxial creep ductility of the material is less than 2.5%. Furthermore, the predicted life of the component under constant load (creep conditions) is significantly reduced due to the presence of the residual stress field.

Keywords: residual stress, creep crack initiation, fracture.

INTRODUCTION

Failure can occur in components operating at high temperature due to crack initiation and growth. A number of criteria have been developed to predict such failures and a common approach is to assume that failure occurs when the creep ductility of the material is exhausted. This failure mode is generally linked at the micro-scale to the initiation, growth and coalescence of voids. For such a model of creep failure the creep ductility is not a material constant but depends on the stress state, generally phrased in terms of stress triaxiality (ratio of hydrostatic stress to equivalent stress). An increase in triaxiality leads to an enhanced void growth rate at the same level of inelastic strain. The existence of secondary or residual stresses, due to thermal and/or prior mechanical loading, may also increase the likelihood of failure under creep conditions, though the effect is generally considered to be less severe at high than at low temperatures as the residual stresses relax with time. In this work, the effect of residual stress, introduced by pre-compression, on creep crack initiation in an austenitic steel weld metal is examined. A notched specimen developed by Sherry et al. [1] which provides a convenient method to introduce a residual stress field has been used. In [2] this specimen was employed in the study of crack initiation in 316 stainless steel at high temperatures and reasonable agreement with experimental data has been obtained. In this work the material examined is a Type 347 austenitic steel manufactured by manual metal arc (MMA) welding. A notched compact tension (CT) specimen has been subjected to pre-compression at room temperature. Following this the specimen is loaded at high temperatures and the time to failure is predicted through the use of an uncoupled damage variable, based on creep ductility exhaustion.

SPECIMEN GEOMETRY AND LOADING CONDITIONS

A schematic of the specimen is illustrated in Figure 1(a) and some of the key problem parameters are indicated in Figure 1(b). In Figure 1(b), the applied compressive load is $P$, $\Delta$ is the load point displacement, $A_n$ the notch opening; $r_p$ is the size of the plastic zone directly ahead of the notch root after unloading. The first stage of the work is to identify an appropriate load magnitude for pre-compression. The goal is to obtain a relatively high magnitude tensile stress field over a
significant distance ahead of the notch. Finite element calculations have been carried out to predict the magnitude and range of the residual stress field induced by compression of the CT specimen.

![Diagram](image)

**FIGURE 1. SCHEMATIC OF COMPACT TENSION (CT) SPECIMEN (SPECIMEN THICKNESS = 25 mm)**

The compression is to be carried out at room temperature where the majority of the inelastic deformation is rate independent; subsequently the specimen will be tested at 650°C where rate dependent (creep) effects dominate.

**MATERIAL PROPERTIES**

**Tensile data**

Tensile data have been supplied by British Energy [3]—[6]. Mean 0.2% proof stress and mean UTS values for TIG and MMA welded Type 347 steel are provided in [4] for a range of temperatures (20–700°C). Full stress-strain curves are also available for 347 TIG (Tungsten Inert Gas) weld material for a range of temperatures ([5] and [6]) but only proof stress data are available for the MMA weld material. Therefore the TIG stress-strain data are scaled to provide the stress-strain curves for MMA. Tensile testing of as welded MMA 347 material is underway to confirm the validity of this assumption. In Figure 2, the 0.2% proof stress obtained from the data in [6] for a TIG weld are compared with the R66 mean 0.2% proof stress values for MMA welds (from [4]). The magnitude of the factor used to scale each individual TIG data set is chosen based on this figure—at each temperature the TIG tensile data are scaled so that the 0.2% proof stress lies on the mean line for MMA in Figure 2. The factor used ranges from 1.1 at 20°C to 0.9 at 600°C.

![Graph](image)

**FIGURE 2. COMPARISON OF 0.2% PROOF STRESS FOR 347 TIG WELD AND MMA WELD.**

Figure 3 shows the engineering stress-strain curves for Type 347 MMA weld metal for temperatures up to 650°C. These data have been obtained by scaling the stress-strain data for 347 TIG from [6] using the appropriate factor obtained from Figure 2.

![Graph](image)

**FIGURE 3. TENSILE DATA FOR TYPE 347 MMA WELD METAL OBTAINED BY SCALING TYPE 347 TIG WELD METAL DATA**

**Creep behaviour**

The creep response for 347 MMA weld material has been examined in [3]. Primary and secondary creep models were fitted to creep and relaxation data from a number of 347 welds (as-welded and aged TIG and as-welded MMA). The 347 material is assumed to follow the RCC-MR model [7] which has an initial primary creep response
with time) followed by a secondary creep response (constant strain rate). The primary creep strain within the RCC-MR creep law is expressed as:

$$\varepsilon^c_p = A_p \sigma^n \varepsilon^m$$,  \hspace{1cm} (1)$$

where \(t\) is the time, \(\sigma\) is the stress and \(A_p, n_p, m\) are material parameters. The creep strain rate at constant stress is expressed by differentiating Eq. (1) with respect to time, i.e.,

$$\dot{\varepsilon}^c_p = A_p m \sigma^n \varepsilon^{m-1}$$.  \hspace{1cm} (2)$$

The secondary creep strain is given by

$$\varepsilon^c_s = \varepsilon^c_{pfo} + A \sigma^n (t-t_{f0})$$,  \hspace{1cm} (3)$$

where \(A\) and \(n\) are material constants. In Eq. (3) \(\varepsilon^c_{pfo}\) and \(t_{f0}\) are, respectively, the magnitude of the creep strain and the time at the end of primary creep, i.e.,

$$t_{f0} = A_f \sigma^n \varepsilon^m; \varepsilon^c_{pfo} = A_p \sigma^n \varepsilon^m$$,  \hspace{1cm} (4)$$

where \(A_f\) and \(n_f\) are given as

$$A_f = \left( \frac{1}{m A_p} \right)^{\frac{1}{m-1}} \text{ and } n_f = \frac{n-n_p}{m-1}$$.  \hspace{1cm} (5)$$

The material constants in the RCC-MR model for the 347 MMA weld material are taken from [3]. Figure 4 illustrates the creep response obtained from the model at 650°C and a range of stress values. For this temperature \(A_p = 2.53 \times 10^8\); \(n_p = 1.9\); \(m = 0.28\); \(A = 7.86 \times 10^{-3}\); \(n = 10.9\) (stress in MPa, time in hours). Since tertiary creep is not included in the model, these values are strictly applicable only up to 0.5% creep strain [3].

**Damage accumulation**

To predict the onset of crack growth a creep damage approach is used following the approach in [9]. A damage parameter, \(\omega\), is defined such that \(0 \leq \omega \leq 1\) and failure occurs when \(\omega = 1\). In this work \(\omega\) is defined as the ratio of the creep strain to the creep ductility and the rate of accumulation of damage is given as

$$\dot{\omega} = \frac{\dot{\varepsilon}^c}{\varepsilon^c_f}$$.  \hspace{1cm} (6)$$

where \(\varepsilon^c_f\) is the multiaxial creep ductility. The total damage at any time, \(t\), is then the integral of the damage rate from the time at which creep deformation is active, \(t_0\), up to the time, \(t\):

$$\omega = \int_{t_0}^{t} \dot{\omega} \, dt$$.  \hspace{1cm} (7)$$

The damage ratio is defined to take into account the effect of triaxiality (hydrostatic stress, \(\sigma_{mn}\) divided by equivalent stress, \(\sigma_e\)) on the growth and coalescence of microvoids and in this work uses the Cocks and Ashby relation [10], such that

$$\varepsilon^c_f = \sinh \left[ \frac{2}{3} \left( \frac{n-1/2}{n+1/2} \right) \right] / \sinh \left[ \frac{2}{3} \left( \frac{n-1/2}{n+1/2} \right) \sigma_e \right]$$.  \hspace{1cm} (8)$$

where \(\varepsilon^c_f/\varepsilon^c\) is the ratio of multiaxial to uniaxial creep ductility and \(n\) is the secondary creep exponent in Eq. 3 (the model assumes that the void growth occurs primarily under secondary creep conditions). Thus, to define the evolution of damage and the time to failure (\(\omega = 1\)) only one additional material parameter, the uniaxial creep ductility, \(\varepsilon^c\), is required, which, in principle can be obtained from a uniaxial creep test.

**FIGURE 4. CREEP RESPONSE FOR TYPE 347 WELD METAL USING THE RCC-MR MODEL.**

**PRE-COMPRESSION OF COMPACT TENSION (CT) SPECIMEN**

The geometry of the CT specimen is illustrated in Figure 1. The finite element mesh used to analyse the specimen is similar to that of Turski [2]. There are approximately 3000 four noded elements and both plane stress and plane strain analyses have been carried out using the commercial finite element (FE) code, ABAQUS [8]. For the plane stress analysis, linear ‘hybrid’ elements have been used (CPE4H) and for the plane strain analyses, standard CPS4 elements have been used. The smallest element size in the notch region is approximately 0.2 mm. Rigid elements (type R2D2) are used to model the contacting cylinder and it is assumed that there is negligible friction between the cylinder and the specimen.

The mechanical properties discussed in the previous section have been used in the FE analysis. The material is assumed to be isotropic and homogenous and linear elastic up to the yield strength and post-yield the material follows von Mises flow theory with isotropic strain hardening. Small displacements are assumed in the analysis.

**Results of 2D pre-compression analysis**

The specimens are compressed to a maximum load, \(P_{max}\), applied through the rigid cylinder (see Figure 1b). The load-displacement curves under plane stress and plane strain
Figure 5 shows conditions up to a maximum displacement of 3 mm. For convenience, both load and displacement are shown as positive in the figure.

![Figure 5: Load-Displacement Curve](image)

**FIGURE 5**, LOAD-DISPLACEMENT CURVE FOR COMPRESSION OF CT SPECIMEN UNDER PLANE STRESS AND PLANE STRAIN CONDITIONS.

As expected, the analysis predicts that a higher load is required under plane strain conditions to reach the same deformation as that under plane stress conditions.

![Figure 6: Typical Plastic Zone](image)

**FIGURE 6**, TYPICAL PLASTIC ZONE UNDER COMPRESSIVE LOAD (PLANE STRESS)

In Figure 6 a typical plastic zone under compressive load is illustrated for a plane stress analysis. The plastic zone is defined as the region where the equivalent (von Mises) plastic strain exceeds 0.1%. The contour shown corresponds to a load point displacement, $\Delta = 0.8$ mm and applied load, $P = 120$ kN.

After the required load has been applied, the cylinder is raised and the specimen unloaded. If the compressive load is sufficiently high the material near the notch undergoes yielding in compression followed by elastic unloading. Under certain circumstances, reverse yield may also occur as illustrated in Figure 7, which shows the stress-strain response for the first element at the notch root. The $\sigma_{22}$ stress is shown (see Figure 1), normalised by $\sigma_{0.2}$, the 0.2% proof stress at room temperature (= 480 MPa). It is seen in the figure that yielding in compression (point C on the figure) is followed by yield in tension (point T on the figure). Note that this response will depend on the cyclic hardening response, i.e. if kinematic hardening were assumed in the analysis, reverse yield would occur at a lower stress level.

![Figure 7: Normal Stress vs. Normal Strain](image)

**FIGURE 7**, NORMAL STRESS VS. NORMAL STRAIN FOR NOTCH ROOT ELEMENT UNDER PLANE STRESS CONDITIONS AND ISOTROPIC STRAIN HARDENING

In Figure 8 the size of the ‘reverse yield zone’ for $\Delta_{\text{max}} = 1.9$ mm is indicated for plane strain and plane stress conditions. Note that the overall extent of the reverse yield zone is much smaller than the plastic zone as it is limited to a small region adjacent to the notch (compare Figure 6 and Figure 8b).

![Figure 8: Reverse Yield Zone](image)

**FIGURE 8**, REVERSE YIELD ZONE AFTER PRE-COMPRESSION (a) PLANE STRAIN (b) PLANE STRESS

In Table 1 the size of the plastic zone, $r_p$, is illustrated for four levels of loading, $\Delta_{\text{max}} = 0.8$, 1.1, 1.9 and 3.0 mm. Also provided in the table are the peak load, $P_{\text{max}}$, and the mouth opening displacements, $\Delta_{\text{max}}^m$, corresponding to the maximum load line displacement, $\Delta_{\text{max}}$. It may be seen in Table 1 that at $\Delta_{\text{max}} = 3$ mm the plastic zone size (defined as region where the equivalent plastic strain exceeds 0.1%) is on the order of the notch length for plane strain conditions ($r_p = 12$ mm) and has spread throughout the specimen ($r_p = 47$ mm) for the plane stress analysis.
In Figure 9 and Figure 10 the distribution of residual stress for three representative displacement levels $\Delta_{\text{max}} = 0.5$, 1.4 and 3.0 mm is illustrated.

### TABLE 1, LOAD/DISPLACEMENT RESULTS OBTAINED FROM PLANE STRAIN PRE-COMPRESSION OF CT SPECIMEN.

<table>
<thead>
<tr>
<th>$\Delta_{\text{max}}$</th>
<th>$r_p$</th>
<th>$P_{\text{max}}$</th>
<th>$\Delta_m^{\text{max}}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>mm</td>
<td>mm</td>
<td>kN</td>
<td>mm</td>
</tr>
<tr>
<td>0.8</td>
<td>5.7</td>
<td>145</td>
<td>1.1</td>
</tr>
<tr>
<td>1.1</td>
<td>6.9</td>
<td>165</td>
<td>1.4</td>
</tr>
<tr>
<td>1.9</td>
<td>9.0</td>
<td>205</td>
<td>2.6</td>
</tr>
<tr>
<td>3.0</td>
<td>12</td>
<td>233</td>
<td>4.0</td>
</tr>
</tbody>
</table>

### TABLE 2, PEAK RESIDUAL STRESS DISTRIBUTIONS, TENSILE ZONE SIZE, $r_t$, AND REVERSE YIELD ZONE SIZE, $r_{pt}$.

<table>
<thead>
<tr>
<th>$\Delta_{\text{max}}$</th>
<th>$\sigma_{22}$</th>
<th>$\sigma_c$</th>
<th>$r_t$</th>
<th>$r_{pt}$</th>
<th>$\sigma_{22}$</th>
<th>$\sigma_c$</th>
<th>$r_t$</th>
<th>$r_{pt}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>mm</td>
<td>MPa</td>
<td>MPa</td>
<td>mm</td>
<td>mm</td>
<td>MPa</td>
<td>MPa</td>
<td>mm</td>
<td>mm</td>
</tr>
<tr>
<td>0.8</td>
<td>960</td>
<td>730</td>
<td>3.05</td>
<td>1.65</td>
<td>760</td>
<td>740</td>
<td>4.7</td>
<td>2.0</td>
</tr>
<tr>
<td>1.1</td>
<td>1030</td>
<td>770</td>
<td>3.35</td>
<td>1.05</td>
<td>810</td>
<td>790</td>
<td>5.2</td>
<td>2.5</td>
</tr>
<tr>
<td>1.9</td>
<td>1200</td>
<td>880</td>
<td>3.7</td>
<td>9.0</td>
<td>930</td>
<td>900</td>
<td>5.7</td>
<td>3.6</td>
</tr>
<tr>
<td>3.0</td>
<td>1320</td>
<td>970</td>
<td>3.9</td>
<td>11.0</td>
<td>1030</td>
<td>1000</td>
<td>6.0</td>
<td>2.4</td>
</tr>
</tbody>
</table>

It may be seen in Figure 9 and Figure 10 that under plane strain conditions the peak normal stress ($\sigma_{22}$) is approx. $2.8\sigma_{0.2}$ (1320 MPa) for $\Delta_{\text{max}} = 3$ mm, while for plane stress the peak is lower, approx $2.1\sigma_{0.2}$ (990 MPa).

### FIGURE 9, NORMAL STRESS (NORMALISED BY ROOM TEMPERATURE 0.2% PROOF STRESS) PLOTTED AGAINST DISTANCE AHEAD OF THE NOTCH ROOT (PLANE STRAIN).

### FIGURE 10, NORMAL STRESS (NORMALISED BY ROOM TEMPERATURE 0.2% PROOF STRESS) PLOTTED AGAINST DISTANCE AHEAD OF THE NOTCH ROOT (PLANE STRESS).

In Table 2 the peak stress levels ahead of the notch are shown. Here $\sigma_c$ and $\sigma_0$ are hydrostatic (mean) and equivalent (von Mises) stress, respectively. The maximum intensity of the residual stress field is controlled by the saturation of the tensile response at room temperature. Though not shown in Figure 3 the maximum stress at room temperature is 1012 MPa. Thus in the FE analysis the equivalent (von Mises) stress cannot exceed this value. Note also that the peak stress at 650°C is approx. 360 MPa (see Figure 3) and thus, regardless of the stress value obtained at room temperature, the equivalent von Mises stress in the finite element analysis at 650°C cannot exceed 360 MPa. The extent of the tensile region ($\sigma_{22}$ positive), $r_t$ is also provided in Table 2. It may be seen that the size of the tensile region ranges from $r_t = 2.05$ mm for $\Delta_{\text{max}} = 0.8$ mm to 3.9 mm for $\Delta_{\text{max}} = 3$ mm under plane strain conditions.

### Results of pre-compression analysis (3D analysis)

It is seen from Table 2 that the size of the tensile zone, $r_t$, does not increase significantly for displacements greater than 2 mm. In this case the tensile zone sizes under plane strain and plane stress are approx. 3.7 and 5.7 mm respectively. Therefore this level of deformation is examined using three dimensional FE analysis. The 3D FE mesh uses approximately, 30,000 eight noded three dimensional ‘hybrid’ brick elements (type C3D8H). The in-plane design of the 3D mesh is identical to that of the 2D mesh.

In Figure 11 the load vs. load line displacement is illustrated for the 2D and 3D analyses. It may be seen from Figure 11 that at a global level the CT specimen behaves as it if is primarily under plane stress conditions. Similar conclusions may be made when observing the plastic zone evolution in the 3D specimen.
Although the global deformation is predominantly plane stress, close to the notch root the deformation approaches plane strain conditions. This is illustrated in Figure 12 which shows that near the notch at the central plane the full 3D stress distribution is very close to the 2D plane strain distribution.

Figure 13 illustrates contours of residual stress, $\sigma_{22}$, on the central plane of the specimen following a pre-compression of 2 mm. In the figure the red region corresponds to the region where the stress exceeds 500 MPa, (close to the room temperature 0.2% proof stress) and the blue regions indicate the compressive zone in the specimen. Figure 14 illustrates the corresponding contours of plastic strain, $\varepsilon_p$, the red region indicating the area where the plastic strain exceeds 2%. The maximum plastic strain is 18% as indicated in the figure and the average plastic strain in the element directly ahead of the notch (element size 0.2 mm) is approx. 16%.

Relaxation of residual stress at high temperature

Following pre-compression the specimen is heated up to temperatures where creep processes are dominant in the material. Due to the reduction in yield strength at higher temperatures (see Figure 3) this leads to an instantaneous reduction in the residual stress. In Figure 15 the reduction in normal stress following heating up to 650°C is illustrated. The stresses are again normalised by the room temperature 0.2% proof stress, $\sigma_{0.2}$. It is seen that the peak normal stress reduces by almost 50% from $2.5\sigma_{0.2}$ to $\sigma_{0.2}$. Though not shown, a similar reduction is seen for the other stress components. Note that little creep strain is associated with this reduction in stress (it arises from a reduction in the elastic strain at high temperatures) and thus does not lead to the type of creep damage described earlier.

Prediction of damage and crack growth at high temperatures

The effect of residual stress on crack initiation is examined by comparing the predicted time to crack initiation in the CT specimen without residual stress to that for the specimen containing residual stress under the same primary load and with no primary load (i.e. stress relaxation alone). The time to crack initiation is taken to be the time for the damage, $\omega$, defined using Eqs. 6–8, to reach unity over a microstructurally significant distance. For a case of constraint stress triaxiality ($\sigma_m/\sigma_e = \text{constant}$) this corresponds to the time for the creep strain over that distance to reach the multiaxial...
failure strain, $\varepsilon^*$. The microstructurally significant distance chosen is 120 $\mu$m (0.12 mm) which corresponds to a typical grain size for austenitic steels (see e.g. [11]).

Contours of the damage parameter $\omega$ are illustrated in Figure 16, for a CT specimen with an applied primary load $F = 20$ kN at a time of approx. 10,000 hours. The uniaxial failure strain, $\varepsilon_f$, for this analysis is 5%. The red region corresponds to elements for which $0.8 < \omega < 1.0$, i.e. these elements have failed or are close to the point of failure. It may be seen in Figure 16 that the peak damage is at a significant distance ahead of the notch (which has a radius of 2.5 mm). This is due to the fact that, although the maximum creep strain is at the notch root, the highest level of triaxiality is ahead of the notch leading to a reduction in the multiaxial ductility through Eq. 8. Similar results have been seen in [12] for relatively blunt circumferentially notched bars. The predicted time to crack initiation for the CT specimen at a load of 20 kN is approx. 8,500 hours.

In Figure 17 the damage contours for a pre-compressed specimen ($\Delta_{\text{max}} = 2$ mm) with the same primary load of 20 kN and after the same number of hours is presented. Note that the region of high damage ($0.8 < \omega < 1.0$) has now shifted closer to the notch and the size of this contour is considerably larger than in Figure 16. Thus the presence of the residual stress field is predicted to affect both the time to failure and the location of failure initiation for the CT specimen. The shift in the location of peak damage is consistent with the existence of a region of high triaxiality near the notch due to the pre-compression (see Figure 13 and Figure 15). The time to crack initiation based on a characteristic distance of 0.12 mm is now less than 100 hours, i.e. the time to crack initiation is significantly lower due to the pre-compression. Note that the model assumes that no damage is incurred during the pre-compression of the specimen and the uniaxial failure strain is unchanged by pre-compression. Therefore, the additional driving force to crack initiation for the pre-compressed specimen is due only to the residual stress in the specimen prior to the application of the mechanical load. Thus the time to crack initiation for the pre-compressed specimen may be an overestimate.
In Figure 18 the corresponding result is illustrated for the case of pre-compression followed by primary load. The same primary load as applied in Figure 18 is examined in this figure. The strong effect of the residual stress on the predicted time to failure under both plane stress and plane strain conditions is noted (compare Figure 18 and Figure 19). For example, if $\varepsilon_f = 5\%$, it is found that the predicted time to crack initiation is reduced from 8500 hours to less than 100 hours for a load of 20 kN.

**Failure during stress relaxation**

If the compact tension specimen is maintained at temperature with no applied (primary) load the residual stresses in Figure 15 will relax to zero and the elastic strains in the specimen will be converted to creep strains. Provided sufficient elastic strain has been introduced into the specimen, the level of creep strain generated in the specimen may be sufficient for crack initiation to occur. It should be noted that the relevant failure strain is the multiaxial failure strain which depends on the stress triaxiality $\sigma_{22}/\sigma_{0.2}$ through Eq. 8. The level of triaxiality in the vicinity of the notch following heating up to 650°C is approx. 1.15 leading to a multiaxial failure strain of approx. 0.16$\varepsilon_f$ from Eq. 8, where $\varepsilon_f$ is the uniaxial failure strain. If the initial strain level in the vicinity of the notch is lower than this value then crack initiation during relaxation is not expected.

In Figure 20 the relaxation of stress in the notch vicinity is illustrated. It is seen that after 10,000 hours the peak stress has relaxed from 550 MPa (approx 1.15$\sigma_{0.2}$) to 120 MPa (approx. 0.25$\sigma_{0.2}$). As seen in Figure 4, the levels of creep deformation at this stress and temperature are very low.

In Figure 21 the corresponding creep strain evolution for an element near the notch is illustrated. It is seen that after 10,000 hours no significant increase in creep strain above 0.3% is observed. Thus failure is not likely to occur under these conditions if $\varepsilon_f$ is above approx. 2%. This is indeed observed from the results presented in Figure 22. When $\varepsilon_f$ is above 2.3%, failure due to creep ductility exhaustion is not predicted under plane strain conditions even for times > 30,000 hours. If the uniaxial creep ductility, $\varepsilon_f = 2\%$ then crack initiation is predicted to occur after approx. 9,000 hours due to the residual stress alone.

![Figure 18](image1.png)

**Figure 18. Effect of Uniaxial Failure Strain $\varepsilon_f$ on Time to Crack Initiation for Primary Load Only**

In Figure 19 the corresponding result is illustrated for the case of pre-compression followed by primary and secondary load. It is seen that after 10,000 hours no significant increase in creep strain above 0.3% is observed. Thus failure is not likely to occur under these conditions if $\varepsilon_f$ is above approx. 2%. This is indeed observed from the results presented in Figure 22. When $\varepsilon_f$ is above 2.3%, failure due to creep ductility exhaustion is not predicted under plane strain conditions even for times > 30,000 hours. If the uniaxial creep ductility, $\varepsilon_f = 2\%$ then crack initiation is predicted to occur after approx. 9,000 hours due to the residual stress alone.

![Figure 19](image2.png)

**Figure 19. Effect of Uniaxial Failure Strain $\varepsilon_f$ on Time to Crack Initiation for Primary and Secondary Load**

![Figure 20](image3.png)

**Figure 20. Stress Relaxation at 650°C Following Pre-Compression at Room Temperature Under Plane Strain Conditions**
CONCLUSIONS
In this work, the effect of a residual stress introduced by pre-compression on the time to crack initiation at high temperatures was examined using the finite element method. It is found that a pre-compression of 2 mm on a notched specimen leads to tensile residual stresses at the notch of approximately 3 times the yield strength. When the specimen is heated to 650°C these stresses relax somewhat but still high to have significant effect on the predicted time to crack initiation under creep loading. It is also found that if the uniaxial creep ductility for the material is below 2%, crack initiation can occur under stress relaxation at 650°C following a pre-compression of 2 mm at room temperature.

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