Abstract

In this paper CCG behaviour of Inconel 718 is investigated by means of numerical simulations of some preliminary Creep Crack Growth (CCG) experimental tests. At first, a criterion of crack length versus time is given to control the crack growth; the crack is assumed to grow constantly from the experimental initial value to the final one, under the external load, according to the experimental results of CCG tests. Then a damage variable, based on the equivalent (von Mises) creep strain rate and the Cocks and Ashby void growth model, is used to predict CCG. The creep behavior of the material is described by a secondary creep law using the creep properties obtained by creep tests previously carried out on the same material. The development of the creep zone is observed and the load line displacement is compared with the experimental one. In the case of simulation based on a damage variable also the crack growth is calculated and compared with the crack growth measured experimentally.

Keywords: Creep rupture, Crack Growth, Damage, Finite Element analysis.

1. Introduction

Inconel 718 superalloy is widely used for structural components operating at high temperature in the power generation and petro-chemical industry. These components are often subjected to non-uniform stress and temperature distribution during service that may cause localized creep damage in the form of initiated cracks which can propagate and ultimately cause fracture.

Creep crack growth (CCG) is important in designing at high temperature and also important in predicting the residual life during the service [9]. With advances in finite element formulation, in many studies FE analyses have been used to estimate CCG rate and validate experimental data. To account for the loss of the load bearing capacity due to creep damage within the elements of the FE mesh, at first, damage variable to account for the evolution of creep damage within the material and special procedures to remove elements which have reached a critical level of damage have been used. The removal of element has resulted inadequate to model the growing of a sharp crack within a creeping material and alternative approaches based on a node-release technique have been adopted in more recent works. In [10] the crack growth rate is assumed a priori, based on experimental data, and the analyses have been used to study the development of the creep zone and the creep zone expansion rate along the crack growth direction in a Compact Tension (CT) specimen made of nickel base superalloy at 650 °C. In [5] the damage approach has been used in conjunction with the node-release technique to predict the CCG and the rupture life in a CT specimen made in carbon-manganese steel at 360 °C.

In this paper, crack growth in two CT specimens made in Inconel 718 superalloy, and experimentally tested at 687 °C, is simulated by means of finite element code ABAQUS using the node release technique. At first, a criterion of crack length versus time is given to control the crack growth; the crack is assumed to grow constantly from the experimental initial value to the final one, under the external load, according to the experimental results of CCG tests. Then a damage variable, based on the equivalent (von Mises) creep strain rate and the Cocks and Ashby void growth model, is used to predict CCG. The creep behavior of the material is described by a secondary creep law using the creep properties obtained by creep tests previously carried out on the same material. The development of the creep zone is observed and the load line displacement is compared with the experimental one. In the case of simulation based on a damage variable also the crack growth is calculated and compared with the crack growth measured experimentally.

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model, is used to predict CCG. In both cases the creep response is described by a secondary creep law using the creep properties obtained by creep tests previously carried out on the same material.

During the crack growth analysis, the load-line displacement is recorded as function of the time. The FE results are compared with the experimental ones. Moreover, the evolution of the creep damage behind the current crack tip is examined. In the case of simulation based on a damage variable also the crack growth is calculated and compared with the crack growth measured experimentally.

2. Material

The material is a commercial prepared Inconel 718 nickel superalloy, hot worked by forging in a 150 mm diameter bar. The chemical composition is shown in table 1. The heat treatment consisted of two hours at 1025 °C followed by water quenching, then six hours at 780 °C followed by air cooling. Figure 1 shows the microstructure of Inconel 718. The mechanical properties after heat treatment are presented in table 2 both for ambient temperature and for the temperature of interest in this paper.

Table 1. Chemical composition of Inconel 718 (wt%).

<table>
<thead>
<tr>
<th></th>
<th>C</th>
<th>Si</th>
<th>Mn</th>
<th>P</th>
<th>S</th>
<th>Cr</th>
<th>Mo</th>
<th>Ni</th>
<th>Al</th>
<th>B</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0.032</td>
<td>0.16</td>
<td>0.1</td>
<td>0.006</td>
<td>0.001</td>
<td>17.26</td>
<td>2.98</td>
<td>50.86</td>
<td>0.55</td>
<td>0.003</td>
</tr>
<tr>
<td></td>
<td>0.08</td>
<td>0.03</td>
<td>5.15</td>
<td>1.03</td>
<td>1ppm</td>
<td>0.007</td>
<td>bal.</td>
<td>1ppm</td>
<td>3ppm</td>
<td></td>
</tr>
</tbody>
</table>

Table 2. Mechanical properties of Inconel 718.

<table>
<thead>
<tr>
<th></th>
<th>E [GPa]</th>
<th>ν</th>
<th>σ_y [MPa]</th>
<th>σ_U [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>T=25 °C</td>
<td>200</td>
<td>0.294</td>
<td>1090</td>
<td>1270</td>
</tr>
<tr>
<td>T=687 °C</td>
<td>165</td>
<td>0.292</td>
<td>950</td>
<td>1100</td>
</tr>
</tbody>
</table>

Figure 1. Microstructure of Inconel 718.
3. Uniaxial Creep behaviour

Uniaxial tension creep tests were carried out, at constant applied load, on specimen having 5 mm gauge diameter and a gauge length of 25 mm. Specimen machined along longitudinal and radial direction of the heat-treated 150 mm diameter bar have been tested. The initial applied stress level ranging from 500 to 600 MPa and the test temperature has been fixed at 687 °C, controlled by means on three thermocouples on the gauge length to ±0.5 °C.

Creep curves, at four stress levels, for experimental tests performed on specimen machined along the longitudinal direction are presented in figure 2. The extent of primary and secondary creep is reduced substantially compared to the characteristic behavior of pure metals and a period of accelerating creep rate accounts for much of the life to rupture. The limited extent of the secondary creep period can be demonstrated very effectively by plotting creep rate as a function of time (figure 3). The duration of each creep stage, varying the initial stress level is summarized in table 3.

The same results are reported in figures 4 and 5 and in table 4 respectively, for creep tests along the transversal direction. In all cases, the primary transient creep of the transversal sample was of the normal type, while the creep resistance in the longitudinal direction exceeds that in the transversal direction. At comparable stress level, creep curves in the longitudinal direction give an higher creep strain rate and an higher rupture time than the corresponding one in the transversal direction. The percentage duration of each creep stage for creep tests along the transversal direction show, the same importance of the accelerating creep rate period that on the life to rupture.

In Inconel 718 superalloy, as in most of the engineering alloy used in power generation and propulsion, creep strength is derived from the presence of precipitate particles dispersed within the matrix. These dispersion are unstable with respect to time and temperature. The intrinsic instability of the particle-microstructure has led to the not unreasonable suggestion that this is the primary cause of the extended period of tertiary creep illustrated in figure 2 and 4. However, Dyson and McLean [2] have demonstrated that this intuitive view does not stand up to quantitative scrutiny for nickel based superalloy. Basically, the shape of creep curve in these alloys are primarily a function of strain and not time/temperature and it is compatible with a dislocation sub-structure that is unstable to deformation. Moreover, Dyson and Gibbons in [4], analyzing experimental data from a variety of sources indicate that the rate at which tertiary creep strain accumulates in nickel-based superalloy is dependent on creep strain at fracture (ductility). This dependence is particularly strong when ductility is low (≤5%). When the ductility is about 10%, the intrinsic instability to strain of the dislocation substructure (strain-softening) is dominant, when ductility is low, less then 2%, the rate of evolution of grain boundary cavitation is sufficiently high for it to be the primary cause of creep acceleration.

Adopting a procedure similar to that used in [4] the creep rates at increasing strains in the tertiary creep stage were normalized to the secondary creep rate. Results for the experimental test carried out in this work are reported in figure 6 for longitudinal direction and in figure 7 for the transversal direction. A significant feature is that the rate of increase of $\dot{\varepsilon}/\dot{\varepsilon}_{ss}$ with creep strain is highly dependent on the rupture ductility of the material and rapid increase of $\dot{\varepsilon}/\dot{\varepsilon}_{ss}$ with strain is associated with low ductility at failure in creep. Creep ductility results a critical parameter influencing the rate at which tertiary creep develops in the examined alloy and it is lower in the transversal direction than in the longitudinal direction.

### Table 3. Duration of each creep stage for creep tests along the longitudinal direction.

<table>
<thead>
<tr>
<th>Initial stress [MPa]</th>
<th>T [°C]</th>
<th>tR [h]</th>
<th>Primary stage duration [%]</th>
<th>Secondary stage duration [%]</th>
<th>Tertiary stage duration [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>512</td>
<td>687</td>
<td>313</td>
<td>6.4</td>
<td>31.9</td>
<td>61.7</td>
</tr>
<tr>
<td>543</td>
<td>687</td>
<td>168</td>
<td>5.9</td>
<td>23.8</td>
<td>70.3</td>
</tr>
<tr>
<td>568</td>
<td>687</td>
<td>118</td>
<td>5.9</td>
<td>19.4</td>
<td>74.7</td>
</tr>
<tr>
<td>600</td>
<td>687</td>
<td>67</td>
<td>7.4</td>
<td>7.4</td>
<td>85.2</td>
</tr>
</tbody>
</table>

### Table 4. Duration of each creep stage for creep tests along the transversal direction.

<table>
<thead>
<tr>
<th>Initial stress [MPa]</th>
<th>T [°C]</th>
<th>tR [h]</th>
<th>Primary stage duration [%]</th>
<th>Secondary stage duration [%]</th>
<th>Tertiary stage duration [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>494</td>
<td>687</td>
<td>384</td>
<td>13.0</td>
<td>23.4</td>
<td>63.6</td>
</tr>
<tr>
<td>543</td>
<td>687</td>
<td>152</td>
<td>5.2</td>
<td>23.0</td>
<td>71.8</td>
</tr>
<tr>
<td>555</td>
<td>687</td>
<td>107</td>
<td>5.7</td>
<td>17.1</td>
<td>77.1</td>
</tr>
<tr>
<td>600</td>
<td>687</td>
<td>80</td>
<td>2.5</td>
<td>16.1</td>
<td>81.4</td>
</tr>
<tr>
<td>612</td>
<td>687</td>
<td>61</td>
<td>3.3</td>
<td>13.1</td>
<td>83.6</td>
</tr>
</tbody>
</table>

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Figure 2. Creep curves along longitudinal direction, varying the initial applied stress.

Figure 3. Strain rate as function of time along longitudinal direction, varying the initial applied stress.

Figure 4. Creep curves along transversal direction, varying the initial applied stress. (x) interrupted test.
Figure 5. Strain rate as function of time along transversal direction, varying the initial applied stress.

Figure 6. Rate of acceleration of tertiary creep as function of creep strain, longitudinal direction.
In order to use uniaxial creep tests results in FEM numerical simulation of CCG, creep deformation can be considered as composed of three regimes, primary, secondary and tertiary creep regimes. The use of an average creep rate obtained directly from creep rupture data has been proposed [5] to account for all three stages of creep. This average creep rate, is defined by

$$\dot{\varepsilon} = \frac{\varepsilon_R}{t_R} = \dot{\varepsilon}_0 \left( \frac{\sigma}{\sigma_0} \right)^{n_A} = A_A \sigma^{n_A}$$  \hspace{1cm} (1)

Where $\varepsilon_R$ is the uniaxial failure strain, $t_R$ is the time to rupture and $\sigma$ is the applied stress. Figure 8 and 9 show the dependence of on stress as obtained from the previous creep curve for longitudinal and transversal direction. The material constants, in the range of the examined stress, are reported in table 5.

<table>
<thead>
<tr>
<th>$\sigma$ [MPa]</th>
<th>$\varepsilon_R$ [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>450</td>
<td>2.8</td>
</tr>
<tr>
<td>500</td>
<td>2.6</td>
</tr>
<tr>
<td>550</td>
<td>2.4</td>
</tr>
<tr>
<td>600</td>
<td>2.2</td>
</tr>
<tr>
<td>650</td>
<td>2.0</td>
</tr>
</tbody>
</table>

Table 5. Material constants for Inconel 718.
4. CCG experimental tests

The specimen used for the experimental tests is a 1/2 inch Compact Tension according to the ASTM E1457 Standard. The crack plane is parallel to the hot working direction and the crack growth in the radial direction. The specimens are fatigue pre-cracked, at room temperature, so that the crack length is between 2 and 4 mm from the notch root. CCG tests are conducted at constant load, at 687 °C with temperature control within 2 °C, and under three different values of the stress intensity factors. During the tests, the crack growth measurement is made by the potential drop technique. Also the crack opening displacement at the load line (pin center to center) is measured with two linear variable displacement transducers. The results of the test show that an initial transition crack growth stage occupies about 20% of life, the constant crack growth rate period occupies about 50% of life and accelerating period occupies a small part of the rupture life. The increase of the creep crack during the constant rate period is small and crack grows rapidly during the acceleration stage.

By the potential drop measurement, an initiation time \( t_i \), corresponding to a crack growth equal 0.2 mm is obtained, and for the extension following initiation, the creep crack extension rate \( da/dt \) is related to the fracture mechanics parameter \( C^* \), evaluated using the expression provided in ASTM E1475 and the experimental load line displacement due to creep, as reported in table 6 [6].

<table>
<thead>
<tr>
<th>( K_I ) [ MPa m(^{1/2})]</th>
<th>22</th>
<th>27</th>
<th>38</th>
</tr>
</thead>
<tbody>
<tr>
<td>( t_i ) [h]</td>
<td>5.7</td>
<td>0.12</td>
<td>0.02</td>
</tr>
<tr>
<td>( da/dt ) [mm/h]</td>
<td>1.05 \times 10^{-3}</td>
<td>1.67</td>
<td>11.91</td>
</tr>
<tr>
<td>( C^* ) [W/m²]</td>
<td>1.7 \times 10^{-2}</td>
<td>6</td>
<td>25</td>
</tr>
</tbody>
</table>

Table 6. Main results of CCG tests.

5. Finite element modelling

Creep crack growth in the standard CT specimens tested with \( K_I = 22 \) and 27 MPa m\(^{1/2}\) has been simulated using a model that consists of 2048 four-node plane-strain isoparametric elements, as shown in figure 11, where an extremely dense mesh was generated around the crack growth area. The mesh size along the crack path is approximately 0.050
mm, which is similar to grain size of the examined alloy (figure 1). Only the upper half of the specimen was considered due to the symmetry. A rigid surface was introduced along the symmetry plane. The nodes on the crack plane are constrained to the rigid surface and can slide along it, before to be involved in the crack growth and to be released. It is assumed that the crack grows in the plane of the initial crack front, i.e. on the symmetry plane, which idealizes the actual condition of multiple microscopic cracks linking up ahead the main crack front. The load was applied to a rigid pin constructed to fit the hole as shown in the mesh. The rigid pin and the hole were modeled as contact surface. Calculation was performed using elastic-plastic-creep behavior. The creep response is described by a secondary creep law using the average creep properties reported in table 5 for the transversal direction, that is the direction perpendicular to the crack plane. The plastic response is assumed to be governed by a Mises flow rule with isotropic hardening and material data of table 2. The post yield strain is treated as piecewise linear up to the rupture load ($\sigma^*_R$) and no strain hardening occurs beyond this value.

All FE analyses were conducted using Abaqus 6.3 and two methods for modelling crack extension were considered. The first, which can be named the debonding model, use a criterion of crack length versus time to control the crack growth, i.e., the debonding of the nodes on the crack plane from the rigid surface. As the crack grows from one nodal position to the next, the force carried by the nodes is gradually released over a number of time increments. To keep the crack open during the crack growth, the rate of force release due to crack growth was chosen to be faster than the rate of force relaxation due to creep. The minimum time increment, adopted, just after the debonding of a node, was as small as $10^{-14}$ h to ensure convergence. The variation of the time increment during the analysis was automatically selected on the base of the reference parameter, fixed equal to 0.0001, that controls the accuracy of the analysis and allows about 1% relative error in the stress estimated near the crack tip. The second method, named damage model, evaluate the damage within the elements close the symmetry plane and ahead the crack tip and release the crack tip node when the damage at crack tip reaches a critical value, as results the crack propagates through the mesh along the symmetry axis of the model.

The creep damage theory assumes that the extent of the creep damage can be adequately represented by a single continuum variable parameter $\omega$, being clearly continuum the nature of the damage that is due to the distribution, over many grains, of creep cavitation. The cavitation damage variable is assumed to vary between 0 and 1. In this paper, as proposed in [8], a ductility exhaustion approach is used to account for the accumulation of the creep damage. Ahead of a growing crack there is a process zone, in which the material experiences damage accumulation, and the local damage approaches 1 at the crack tip when the creep ductility of the material is exhausted here. The rate of damage accumulation is

$$\dot{\omega} = \frac{\dot{\varepsilon}_c}{\varepsilon_f^*}$$

where $\dot{\varepsilon}_c$ is the equivalent creep strain rate and $\varepsilon_f^*$ is the multiaxial failure strain accounting for the constrain effect the crack tip. The total damage at any instant is the integral of the damage rate in Eq. (2) up to that time

$$\omega = \int_0^t \dot{\omega} dt$$

When the local accumulated strain reaches the local multiaxial creep ductility, the damage is equal 1. It is assumed that the principal component of creep damage is the formation of voids, and that the contribution of other components such as changes in the size or shape of precipitates is negligible, then $\omega$ can obtained from a number of available void growth models. In this paper the Cocks and Ashby model [1] is assumed to describe the multiaxial creep ductility of the material. The model describe the ratio of multiaxial to uniaxial failure strain

$$\frac{\varepsilon_f^*}{\varepsilon_f} = \sinh \left[ \frac{2}{3} \left( \frac{n-1/2}{N+1/2} \right) \right] / \sinh \left[ \frac{2}{3} \left( \frac{n-1/2}{N+1/2} \right) \sigma_m / \sigma_e \right]$$

where $\sigma_m / \sigma_e$ is the ratio between the hydrostatic stress and the equivalent, von Mises, stress, also referred as triaxiality, that gives the life time dependence upon the stress state. Although the creep damage variable is defined such
that $0 \leq \omega \leq 1$, because creep rupture occurs by the coalescence of creep cavitation. Dyson and McLean [3] argue that a value of 0.3 is unlikely to be exceeded. This criteria has been used in the present paper.

To account for the damage accumulation ahead the crack tip, a user subroutine was used. The subroutine holds fixed the y-displacement of each node of the crack path along the symmetry plane, until the two integration points close to the symmetry plane, of the element at the crack tip, reaches the damage of 0.3. Then the node is released and the constraint in the y-direction is no longer applied.

5. Finite element Results

A first results of finite element analysis is shown in figure 12, where is reported the comparison between the load-line displacement from experimental test and numerical calculations for the test with $K_I = 27$ MPa. Tertiary creep behaviour (rapid increase in displacement towards the end of the test) is predicted by both the FE analyses, but the results of the debond model are more close to the experimental one.

About the comparison between the load-line displacement from experimental test and numerical calculation for the interrupted test with $K_I = 22$ MPa, it can be seen (figure 13) that in both case the predicted amount of load line displacement is less than that observed in the experiment, so both models seem be non-conservative.

Experimental and numerical crack growth, obtained from damage model, for the examined experimental tests are reported, respectively in figure 14 and 15. Damage model provides a realistic representation of the stationary crack growth period. The amount of crack growth predicted in the period of accelerating growth can be observed only for the test with $K_I = 27$ MPa m$^{-1/2}$ and is lower than the experimentally measured. This behavior of the numerical model can be due to the rate of damage accumulation in the second and third element from the crack tip. Although the damage in the first element at the crack tip accumulates in a fast way, the slow accumulation of the damage in the second and third element leading to a lower rate of the crack growth compared to the experimental result.

Figure 16 shows the contour plot of the equivalent creep strain at final stage of growth for the test with $K_I = 27$ MPa m$^{-1/2}$. The grey area of the filled contour plot denote regions where the equivalent creep strain are greater then 0.002. Displacement shown in the plot are magnified by a factor 4. Creep deformation seems to be confined to a thin region adjacent to the crack growth path, resulting in a damage wake behind the crack tip. The extension of the creep zone increases with the increase of the crack growth.

Figure 11. FEM mesh used for CCG analysis of CT specimen.
6. Conclusions

This paper presents numerical simulation of preliminary CCG test in a CT specimens, using a damage variable to quantify time dependent crack tip degradation. The material examined is a Nickel-base superalloy at 687 °C. A power law creep model is used to describe the constitutive behavior of the material.

At first, a criterion of crack length versus time is given to control the crack growth; the crack is assumed to grow constantly from the experimental initial value to the final one, under the external load, according to the experimental

Figure 12. Comparison of experimental and numerical load-line displacement for test with $K_I = 27$ MPa $m^{\frac{1}{2}}$.

Figure 13. Comparison of experimental and numerical load-line displacement for test with $K_I = 22$ MPa $m^{\frac{1}{2}}$. 
Figure 14. Comparison of experimental and numerical crack length for test with $KI = 27 \text{ MPa m}^{\frac{1}{2}}$.

Figure 15. Comparison of experimental and numerical crack length for test with $KI = 22 \text{ MPa m}^{\frac{1}{2}}$.

Figure 16. Field of creep equivalent strain for test with $KI = 27 \text{ MPa m}^{\frac{1}{2}}$. 
results of CCG tests. Then a damage variable, based on the equivalent (von Mises) creep strain rate and the Cocks and Ashby void growth model, is used to predict CCG.

The results obtained from the analyses suggest that in terms of load line displacement results both methods seems to be non conservative.

The damage model allow us to predict the crack growth rate in the stationary period with agreement with experimental results, while in the accelerating period it predicts a lower crack growth rate than the experimentally observed value.

7. References