EFFECT OF RESIDUAL STRESSES ON CRACK SHAPE OF CORNER CRACKS AT HOLES IN NICKEL BASE SUPERALLOYS

R. Branco\textsuperscript{1}, F. V. Antunes\textsuperscript{b}, J.A. Martins Ferreira\textsuperscript{b} and J.M. Silva\textsuperscript{c}

\textsuperscript{a} Department of Mechanical Engineering, ISEC, Polytechnic Institute of Coimbra, Portugal.
\textsuperscript{b} CEMUC, Department of Mechanical Engineering, University of Coimbra, Portugal.
\textsuperscript{c} Department of Aerospace Sciences, University of Beira Interior, Portugal.

ABSTRACT

The main objective of this paper is to study the crack shape evolution in double-U and central hole specimens with corner cracks, both representative of gas turbine discs in terms of critical zones with stress concentrations. An automatic crack growth technique is employed, consisting of a three iterative steps: 3D finite element analysis, K calculation and crack propagation. Complementary experimental work was developed in RR1000, a nickel base superalloy applied in turbine disks, in order to

\footnotesize{\textsuperscript{1} Corresponding author. Tel: +351 239 790 200; Fax: +351 239 790 201. E-mail: rbranco@isec.pt}
obtain crack shapes and fatigue crack growth at elevated temperature. The effect of residual stresses on crack shape is investigated.

**Keywords**: Corner cracks at holes, stress intensity factor, crack shape, propagation stages, residual stresses.

## 1. INTRODUCTION

Aeronautic industry is highly demanding in terms of safety and economy. Turbine disks made of nickel base superalloys are critical components subjected to severe fatigue loading which cannot fail in service. Fracture and fatigue properties of materials are normally obtained from standard test pieces with through thickness cracks, such as the CT or the MT specimens. However, the application of results from CT specimens to corner cracks gives conservative life predictions, i.e., the fatigue life is underestimated (Tong et al, 1999; Brown et al, 1982). Therefore, considering that corner and surface cracks are quite frequent, alternative specimen geometries have been developed, as illustrated in figure 1. The fatigue propagation in some recent nickel base superalloys, such as Udimet 720 or RR1000, has been studied using specimens representative of gas turbine discs in terms of notch geometry and bulk stress, such as the corner crack specimen (Antunes et al, 2001) presented in figure 1c, the double-U specimen showed in figure 1d (Evans et al, 2005; Silva et al, 2010), or the washer specimen (Claudio, 2005). The double-U specimen was developed to reproduce the geometry of the discs at the connection with the blades, since this is a high stress concentration region prone to fatigue failures.

Stress intensity factor solutions have been proposed by different authors for corner crack geometries (Raju et al, 1979; Pickard et al, 1986), however, quarter-circular or quarter-elliptical ideal shapes are usually assumed. In practice, the shapes of corner cracks are different from these ideal shapes, namely under conditions of high temperature fatigue. In this case, depending on test parameters, an important tunnelling effect can be observed, which is mainly attributed to the change of crack propagation mechanism with the stress state (Antunes et al, 2001). The crack shape has a great influence on the distribution of the stress intensity factor along the crack front (Branco et al, 2008a; Branco et al, 2008b). Additional surface effects influencing crack shapes are residual stresses and crack closure. Shot peening is one of the many techniques available to mechanically improve the surface properties of components. The process creates strain hardening and a layer of compressive residual stress at the surface, by plastic
deformation. This compressive layer offsets the applied stress, resulting in a benefit in terms of fatigue, corrosion-fatigue and fretting fatigue (Byrne et al, 2002). However, little information is published about the possible effect of shot peening in long crack propagation.

The fatigue life, the crack shape evolution and the $K$ solutions for the specimens mentioned in figure 1 can be studied using an automatic numerical procedure (Lin et al, 1999). A 3D finite element model is developed to calculate the displacement field, which is used to obtain the stress intensity factors along the crack front. Finally, by applying an adequate crack growth model, taking into consideration experimental $da/dN-\Delta K$ curves, it is possible to define the new crack front position. Repetition of this procedure up to the final fracture enables the characterization of crack shape evolution and fatigue life. Surface effects, such as crack closure, residual stresses or change of propagation mechanism can be taken into consideration in this propagation model. A wide range of planar cracks, fastener holes, notched and unnotched round bars, under tension, bending and combined load have been simulated (Lin et al, 1997; Lin et al, 1998; Lin et al, 1998a). Other geometries have been studied, namely MT specimens (Branco et al, 2008a), CT specimens (Branco et al, 2008b), edge flaws in a round bar (Couroneau et al, 1998), short deep and long shallow semi-elliptical surface cracks (Gilchrist et al, 1991) and composite-repaired aluminum plates (Lee et al, 2004). The automatic crack growth technique has been also applied to more complex situations involving realistic components and mixed mode loading (Richard et al, 2007; Sander et al; 2005; Sander et al 2006).

The aim of the present article is to study the crack shape evolution in double U and central hole specimens using numerical and experimental approaches. The effect of residual stresses on crack shape is investigated. The experimental work was developed in RR1000, a powder metallurgy nickel base superalloy applied in turbine disks, to obtain crack shapes and fatigue crack growth at elevated temperature.

2. NUMERICAL AND EXPERIMENTAL PROCEDURES

2.1. Numerical

Figures 1a and 1d exhibit the geometries of the central hole and the double-U specimens, respectively. Considering the symmetries of geometry and loading, only one quarter of the specimens was studied assuming adequate boundary conditions (figure 2). The restrictions at the head of the specimen avoid rotation
and bending, and intend to simulate the boundary conditions imposed by the rigid grips of the testing machine. The corner crack is plane, normal to the axis of the specimen and exists in its middle-section, therefore mode-I loading occurs along the whole crack front. The material was assumed to be continuous, homogeneous, isotropic and with linear elastic behaviour.

Figure 1 a) Central hole specimen; b) single edge notch tension specimen; c) corner crack specimen; d) double-U specimen (dimensions in millimeters).

The non-commercial finite element software ModuleF was used to develop the mesh (figure 3a). This consisted of a spider web pattern with three concentric rings centred on the crack tip disposed along eighteen layers (figure 3b); a large regular mesh was used away from the crack front to reduce the number of elements and consequently the computational effort. Isoparametric pentahedric singular elements (with mid-side nodes positioned at quarter point positions) were

<table>
<thead>
<tr>
<th>Central hole:</th>
<th>Double-U:</th>
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<tr>
<td>Ref. 1: ( \sigma = 0 )</td>
<td>Ref. 1: ( \sigma = 0 )</td>
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<tr>
<td>Ref. 2: ( dx = 0 )</td>
<td>Ref. 3: ( dx = 0 )</td>
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<tr>
<td>Ref. 4: ( dx = 0; dy = 0 )</td>
<td>Ref. 4: ( dx = 0; dy = 0 )</td>
</tr>
</tbody>
</table>

Figure 2. Physical model (R = 5 mm; H/2 = 90 mm, W/2 = 22.5 mm, t = 5.025 mm).
considered around the crack front. 3D isoparametric elements were considered elsewhere: 20-node hexahedral elements and 15-node pentahedric elements. A full Gauss integration was used for these elements, i.e., $3 \times 3 \times 3$ integration points for the hexahedric elements and 21 integration points for the pentahedric elements. The model contains 1142 nodes and 948 elements (294 pentahedric elements and 654 hexahedral elements). The mesh was refined near the free surfaces to account for surface effects. The nodes along the crack front were positioned on a cubic spline, which provides a good simulation of the real shape (better than a polygonal line).

Figure 3. a) Assembled model; b) spider web mesh; c) layers of spider web mesh.

The automatic crack growth technique employed in this article is schematically presented in figure 4. The automatic procedure generates automatically the three-dimensional finite element mesh since several variables such as geometry, boundary conditions, loading, initial crack shape, elastic properties and fatigue crack growth rate, are previously defined. Then, the displacement field is obtained (figure 4a) which enables the calculation of the stress intensity factor along the crack front (figure 4b). After that, the crack increments are defined using experimental $da/dN-\Delta K$ results (figure 4c) and consequently the new crack front is obtained (figure 4d) connecting all nodes.
Finally, cubic spline functions are used to redefine the positions of mid-side nodes (figure 4e), which gives more realistic crack shapes and contributes to improve the accuracy of the procedure. This crack front is considered as the initial crack shape of the next increment. The entire procedure can be repeated until the final fracture or as long as necessary.

The stress intensity factor along the crack front was calculated using the two point extrapolation method (Zhu et al, 1995). At a generic point \( P \), located on the crack surface, in mode-I, it can be written as follows,

\[
K_I = \sqrt{\frac{\pi}{8r}} \times E' \times v_p
\]

where \( v_p \) is the crack displacement, \( r \) is the radial distance from the crack tip, \( E' \) is the modified Young modulus with \( E' = E/(1-\nu^2) \) in plane strain state and \( E' = E \) in plane stress state and \( \nu \) is the Poisson’s ratio. Using the \( K \) values calculated earlier, the local increments are then calculated using the Paris law for a finite number of load cycles. The propagation at each crack front node (under remote mode-I loading) occurs in a normal direction of the tangent to the crack front at that position (see figure 4c). The crack increment at an arbitrary node along the crack front can be calculated by the following expression,

\[
\Delta a_{i}^{(j)} = \left[ \frac{\Delta K_{i}^{(j)}}{\Delta K_{\text{max}}^{(j)}} \right] \Delta a_{\text{max}}^{(j)}
\]

where \( \Delta a_{\text{max}}^{(j)} \) is the maximum crack growth increment for the \( j^{th} \) iteration. Once \( \Delta K \) varies with crack growth, Euler algorithm can be used to calculate the number of load cycles according to the equation,

\[
N^{(j+1)} = N^{(j)} + \Delta N^{(j)} \Leftrightarrow N^{(j+1)} = N^{(j)} + \frac{\Delta a_{\text{max}}^{(j)}}{C[\Delta K_{\text{max}}^{(j)}]^m}
\]

where \( C \) and \( m \) are the Paris law constants.
2.2. Experimental Procedure

Experimental work on double-U specimens was carried out to validate the numerical predictions. This research was carried out using the RR1000 nickel base superalloy, developed by Rolls-Royce for specific usage in turbine discs of aeroengines. The specimens were tested at 650ºC in a servo hydraulic testing machine with a 100 kN load capacity. Testing temperature was obtained via an electrical furnace. The superalloy’s chemical composition and mechanical properties at room temperature and at 650ºC are presented in Tables 1 and 2, respectively (Claudio, 2005).

Table 1. Chemical composition of the RR1000 nickel base superalloy (mass percentage)

<table>
<thead>
<tr>
<th>Ni</th>
<th>Co</th>
<th>Cr</th>
<th>Mo</th>
<th>Ta</th>
<th>Ti</th>
<th>Al</th>
<th>B</th>
<th>C</th>
<th>Zr</th>
<th>Hf</th>
<th>O₂</th>
</tr>
</thead>
<tbody>
<tr>
<td>52.4</td>
<td>18.5</td>
<td>15.0</td>
<td>5.0</td>
<td>2.0</td>
<td>3.6</td>
<td>3.0</td>
<td>0.015</td>
<td>0.027</td>
<td>0.06</td>
<td>0.07</td>
<td>---</td>
</tr>
</tbody>
</table>

Table 2. Mechanical properties of the RR1000 nickel base superalloy

<table>
<thead>
<tr>
<th></th>
<th>Room temp.</th>
<th>650ºC</th>
</tr>
</thead>
<tbody>
<tr>
<td>E [GPa]</td>
<td>214</td>
<td>188.6</td>
</tr>
<tr>
<td>0.2% Proof. [MPa]</td>
<td>1086</td>
<td>1034</td>
</tr>
<tr>
<td>UTS [MPa]</td>
<td>1602</td>
<td>1448</td>
</tr>
<tr>
<td>Poisson’s ratio, ν</td>
<td></td>
<td>0.255</td>
</tr>
</tbody>
</table>

Fatigue tests were performed with a 0.1 load ratio taking into consideration two waveforms: sinusoidal with 5Hz frequency and trapezoidal with 30s dwell
time (1-30-1-1s). The potential drop technique was used for crack propagation monitoring purposes using a DCPD pulsed system coupled with the controller of the servo hydraulic machine. During the tests the loading conditions (either stress ratio or loading frequency) were changed to record visible marks on the fracture surface which enables the identification of crack shapes. Figure 5a exhibits the fracture surface of a particular specimen. At least three corner cracks are distinctly observed. The two smaller visible crack shapes were measured for validation purposes. Figure 5b compares these crack shapes with the ones predicted by the application of the automatic crack growth technique described earlier.

![Fracture surface of LF001 specimen tested with a 5Hz sinusoidal load and R=0.1; b) Numerical versus experimental results.](image)

The crack lengths of each mesh layer for the smallest and the following visible crack shapes were measured. The former was used to define the initial crack shape in the numerical procedure whilst the latter was compared with the corresponding numerical prediction (dashed line). Both crack shapes (dashed and full lines) are almost similar which indicates a good validation. Differences between experimental loading and the loads considered in the numerical analysis can explain this mismatch.

Eight test specimens were shot peened on all the surfaces with peening parameter 110H 6-8A 100%. This parameter was based on previous optimization studies made by Rolls-Royce plc. They also provided the residual stress profiles in the conditions “as-machined” and “shot peened” which are plotted in figure 6 (Cláudio, 2005). The residuals were measured at room temperature, before any
thermal exposure, by X-Ray diffraction combined with electrolytic polishing to assess in depth values. According to Cláudio (2005), and as can be seen in figure 6, the residual stress level introduced by the shot peening is very high. At a 20 µm depth, the stress reaches 1520 MPa which is close to the tensile limit of the material. However, the effect of shot peening almost disappears at a depth greater than 100 µm.

Figure 6. Residual stress measurements made in RR1000 washer specimens at room temperature and estimated profile after 100h at 650ºC (Cláudio, 2005).

Figure 7a presents predictions of the crack opening level ($\sigma_{\text{open}}/\sigma_{\text{max}}$) obtained by fixing the stress range and increasing the minimum and maximum stresses, as it is schematised in figure 7b. Each cycle in figure 7b corresponds to one numerical prediction (●) in figure 7a. The first node behind the crack tip was used to quantify the crack opening level. The ratio between maximum stress and yield stress ($\sigma_{\text{max}}/\sigma_{\text{ys}}$) is also presented, showing that it increases up to about 0.78 for $R=0.64$. The monotonic plastic zone is expected to increase with $K_{\text{max}}$, while the cyclic plastic zone is expected to be constant. From the figure it is possible to conclude that the increase of the mean stress produces a clear increase of $U$. For $R>0.64$ no closure is observed, i.e., $U=1$. 
Schijve (1981), based on the work of Newman (1976), proposed the equation,

$$\frac{\sigma_{op}}{\sigma_{max}} = 0.45 + 0.22R + 0.21R^2 + 0.12R^3$$

(4)

to describe the variation of the opening level ($\sigma_{op}/\sigma_{max}$) with R. In the same year, Koning (1981) proposed the following model for the 7075-T6 aluminium alloy:
The predictions obtained according to these models are plotted in figure 7a. Despite the discrepancies in loading conditions, materials and closure definition, a good agreement can be found between the predictions and the numerical simulation results up to R=0.

### 3. Presentation and Analysis of Results

#### 3.1. Stable Crack Shapes

Figures 8 and 9 illustrate the crack shape development from four initial crack shapes (one quarter-circular, two quarter-elliptical and one irregular) for both double-U and central notch specimens, respectively. Many profiles were suppressed in order to provide a better illustration. The effect of the initial crack shape is clearly shown during the early propagation stage. Different crack profiles were obtained in each case during this period. However, with further propagation the crack tends towards similar profiles. By comparing both figures, for double-U and central hole specimens, there are no significant differences. However, the crack front is usually not balanced which can be explained by a non-uniform stress distribution along the crack in both free surfaces (hole and front directions), as can be seen in figure 10.

Figure 10 shows the evolution of the stress concentration factors at the hole edge direction (K\textsubscript{t,z}) and at the front surface direction (K\textsubscript{t,x}) for both double U and central hole specimens. The stress concentration factors were defined as the ratio between local and remote tension stresses. The normal stress at the hole edge caused by remote uniform stress tension is about three times the remote stress whilst the normal stress decreases continuously along the plate surface direction towards the remote stress. Comparing both specimen geometries, it can be concluded that the central hole specimen has greater stress concentration factors at the hole edge, whereas in the normal direction to the hole edge (frontal surface) the values are quite similar and homogeneous.
Figure 8. Shape development for double-U specimen considering a: a) quarter-circular initial crack shape ($a_0/c_0 = 1$); b) irregular initial crack shape ($a_0/c_0 = 0.90$); c) quarter-elliptical initial crack shape ($a_0/c_0 = 1.53$); d) quarter-elliptical initial crack shape ($a_0/c_0 = 0.66$).

Figure 9. Shape development for the central hole specimen considering a: a) quarter-circular initial crack shape ($a_0/c_0 = 1$); b) irregular initial crack shape ($a_0/c_0 = 0.90$); c) quarter-elliptical initial crack shape ($a_0/c_0 = 1.53$); d) quarter-elliptical initial crack shape ($a_0/c_0 = 0.66$).
In truth, observing the crack shape development, it seems that a quasi quarter-elliptical crack shape is reached and maintained after the early propagation stage. This is a consistent behaviour which is independent on the initial crack shape.

In order to investigate how much close to the quarter-elliptical crack shape are those profiles, a quantitative study of crack shape variation was carried out. Two effective characterising parameters were defined. The residual difference \( h_i \) can be written as follows,

\[
h_i = \frac{d_i}{r_i + d_i}
\]

being the variables defined in figure 11 \((r_i \text{ and } \theta_i \text{ are the polar coordinates of the } i^{th} \text{ node along the predicted crack front}). The standard residual deviation \( st \) which gives a general appreciation of whole crack front, is defined by the expression,
where \( d_i \) is the difference between the radius of \( i^{th} \) and \( i^{th} \) nodes, and \( n \) is the number of corner nodes of the crack front. The average crack length \( (r') \) for a given crack front was calculated by the mean of the nodal crack length \( (r_i) \) of each mesh layer (see figure 3c).

\[
st = \sqrt{\frac{1}{n} \sum_{i=1}^{n} d_i^2}
\]  

(7)

Figure 11. Definition of the dependent parameters.

Figure 12a shows the distribution of the residual difference around the crack front during the crack growth for several crack fronts in both double U and central hole specimens with \( r/t = 1 \) and initial crack shapes with \( a_0/c_0 = 1.0 \). The crack fronts analysed are exhibited at the bottom of this figure. As can be seen, the residual difference is always zero at both end points because the perfect quarter elliptical shape is defined through these two points. Besides, this variable is always negative in the remaining points, which means that the predicted crack shape lays inside the corresponding quarter-ellipse. On the other hand, the residual difference increases continuously during the crack propagation, reaching absolute values higher than 8%. It is also clearly observed that the influence of the specimen geometry is basically small once the residual differences for both double U and central hole are very close and present the same type of evolution. Additionally, this figure shows a comparison between the results obtained in this specific study and the ones found by Lin et al (Lin et al, 1998) in their studies concerned with corner cracks at fastener holes with similar ratios of \( a_0/c_0 \) and \( r/t \). As observed, the evolutions of \( d_i \) with the angle \( \theta \) are quite similar, even though in this situation smaller residual differences are obtained.

Figure 12b presents the evolution of the standard residual deviation with the crack growth for both double-U and central hole specimens with \( r_0/t = 0.2 \). Two
initial crack shapes with $a_0/c_0=1.53$ (dashed lines) and $a_0/c_0 = 1.0$ (full lines) are analysed. Basically, it can be seen that the $st$ parameter increases continuously as the crack propagates and is about 7% as the crack nearly approaches the back surface of the specimen. Lin et al (1998) studied exhaustively the evolution of $st$ for different values of $a/t$ and $a_0/c_0$ and have concluded that the maximum value of $st$ is about 6%. An important effect of the initial crack shape is clearly observed in figure 12b. Nevertheless, the crack propagation tends asymptotically to preferred crack paths independently on the value of $a_0/c_0$. There is a more intense convergence in the early propagation regime, specially exhibited in the case with $a_0/c_0=1.53$, which may occur due to a higher driving force of crack in this period which forces more rapidly the crack towards the equilibrium. Figure 12b also shows that the influence of specimen geometry on the standard residual deviation is not relevant since the evolutions of $st$ are always quite similar for both specimens. The previous conclusions demonstrate that the numerical predictions are close to the quarter-elliptical crack shape. Therefore, accurate analyses of fatigue crack growth based on this presupposition can be successfully carried out.
Figure 12. Evolution of: a) \( d_i \) with \( \theta \) during the crack growth; b) \( st \) with the \( r' \) during the crack growth for both double-U and central hole specimens.

### 3.2. Crack Propagation Stages

The crack shape change during the propagation can be characterised by studying the variation of \( a/c \) against the ratio \( r_0'/t \). Figure 13a exhibits the variation of both variables employing the present simulation technique for several initial quarter-elliptical crack shapes with different values of \( a_0/c_0 \) taking into consideration the double U specimen. The dashed line presents the evolution of an initial quarter-elliptical crack shape with \( a_0/c_0=1 \) for the central hole geometry.

Two propagation stages are perfectly distinguished in figure 13a. During the early propagation (I) stage the crack path depends significantly on the initial crack shape and for each case a different trajectory is followed by the crack while in the remaining propagation (II) the crack follows a preferential path independent on the initial crack shape. These concepts have already been quoted in literature by several authors. Lazarus (1999) analysed an embedded crack in an infinite body under uniform and cyclic tension and observed that, after certain propagation time the crack reaches a circular shape independently on the initial crack shape analysed. Lin et al (Lin et al, 1997) studied three semi-elliptical surface cracks with different initial aspect ratios, subjected to tension and bending and concluded...
that all these cracks propagate towards a preferred aspect ratio. Similar results were found in their studies with cracks emanating from fastener holes and cracks in notched an unnotched fatigued round bars (Lin et al, 1998a). It is also observed that the cracks which are initially more distant from the second propagation stage require prolonged growth to reach it, but for each $a_0/c_0$ only a single path can be followed by the crack. Besides, comparing the evolutions of $a_0/c_0=1$ for both geometry it can be found again minimal differences between those curves which is in accordance with the general conclusions obtained before.

A different analysis is carried out in figure 13b. Three distinct initial crack shapes (plotted at the bottom of the figure) with the same average crack length ($r_0'$) and unitary $a_0/c_0$ ratios are studied for a central hole specimen. As can be seen, in this case the curves are similar during the whole crack propagation. Therefore, crack path depends only on the initial state which is a good indication of the robustness of $a/c$ ratio as a dependent parameter to quantify variations of crack shape. A mathematical model suitable to predict both stages has already been enunciated by Branco et al (2007) in their studies involving double U and central hole geometries. According to the authors the early propagation model has an exponential behaviour which only depends on its initial remoteness while the subsequent propagation is quite stable and can be fitted by a fourth order polynomial function.

![Graph showing crack propagation and aspect ratio](image)
3.3. Effect of Residual Stress

As earlier mentioned (see figures 6 and 7), the residual stress level introduced by shot peening causes mean stress changes. This effect is also responsible by a variation of the crack closure level. Thus, in order to study the effect of residual stresses on crack shape and fatigue crack growth, an effective stress intensity range was considered, given as follows:

$$\Delta K_{i,\text{eff}}^{(j)} = U_i \cdot \Delta K_i^{(j)}$$  \hspace{1cm} (8)$$

where $U_i$ is the fraction of the load cycle for which the crack remains fully open. In this study, a value of $U=0.48$ was defined on the surface layers. This value is representative of the residual stress field under simulation.

Figure 14 shows the crack shape development for four initial crack shapes (one quarter-circular, two quarter-elliptical and one irregular) for the double-U specimen considering crack closure on the surface layers. Similar results were obtained for the central hole specimen. Many profiles were suppressed in order to provide a better illustration.
Effect of Residual Stresses on Crack Shape of Corner Cracks…

Figure 14. Shape development for double-U specimen, with $U=0.48$, considering: a) quarter-circular initial crack shape ($a_0/c_0 = 1$); b) irregular initial crack shape ($a_0/c_0 = 0.90$); c) quarter-elliptical initial crack shape ($a_0/c_0 = 1.53$); d) quarter-elliptical initial crack shape ($a_0/c_0 = 0.66$).

In this case, there is no doubt that the crack grows towards a non quarter-elliptical crack shape. A superficial delay at the hole edge and at the front surface can be clearly distinguished. This effect is more evident especially near the front surface whilst at the hole edge it is also observed but with a smoother intensity. These conclusions are illustrated in figure 15a which exhibits the distribution of the residual difference around the crack front during the crack growth for several crack fronts in a double U specimen with $r/t = 1$ and an initial quarter-circular crack shape ($a_0/c_0 = 1.0$). As enunciated, near the front surface (angle amplitudes about $0^\circ$) the residual differences are always greater than near the hole edge (amplitudes about $90^\circ$). Naturally, for $\theta=0^\circ$ and $\theta=90^\circ$ the values of $h_i$ are null because both crack lengths are used to define the quarter-elliptical crack shape.

Figure 15b presents the evolution of the standard residual deviation with the crack growth for a double-U specimen with $r_0/t=0.1$ and for an initial crack shapes with $a_0/c_0=1.0$. Two situations with and without crack closure are compared. In the latter, as presented in figure 12b, this parameter increases rapidly at the early propagation stage and then remains approximately constant.
The maximum differences are about 7%, as observed before in figure 12b. With crack closure, the standard deviation is always greater. At the beginning of the propagation $st$ increases suddenly and then drops steadily to values about 9%. It is interesting to observe that both initial periods (characterized by high $st$ gradients) have, apparently, the same range. The effect of the initial crack shape is shown again in this figure.

Figure 15. Evolution of: a) $d_i$ with $\theta$ during the crack growth with $U=0.48$; b) $st$ with the $r'$ with $U=0.48$ during the crack growth for double-U specimen.
4. CONCLUSIONS

In this paper, the effects of residual stresses on the crack shape evolution in double U and central hole specimens were investigated, employing both numerical and experimental approaches. The main conclusions are:

• an automatic crack growth technique consisting of a three iterative steps (3D FEM analysis, K calculation and crack propagation) was successfully implemented and used to study the crack shape development;
• experimental work was carried out using the RR1000 nickel base superalloy. The specimens were shot peened on all the surfaces with peening parameter 110H6-8A;
• fatigue tests were carried out with constant load ratio (R=0.1). During these tests the loading conditions were changed to record visible marks on the fracture surface which were used to validation purposes. Through the comparison of the numerical predictions and these visible crack shapes it was found that both data are quite close;
• the crack shape development is strongly dependent on the initial crack shape but after a short initial propagation stage tends towards a preferred propagation path independently on the initial crack shape. The crack grows under a not balanced fashion which can be explained by the non-uniform stress concentration factors at the hole edge and the front surface. At the hole edge the normal stress is approximately three times the remote tension whereas at the front surface the normal tension tends towards the remote tension;
• The geometrical proximity of the crack profiles to the quarter-elliptical crack shape was investigated. It was found that this assumption is adequate and can be efficiently used to estimate the crack shape propagation;
• The effect of residual stresses was studied by the introduction of crack closure on the numerical model. The residual stresses affect the stress ratio and consequently it directly affects the crack closure level as well. For U=0.48 at the free surfaces, the results show that the crack shape is more distant from the quarter-elliptical crack shape and an approach based on this presupposition is inadequate. The tunneling effect rises significantly as well as the delay at the free surfaces, especially at the front surface.
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REFERENCES


Byrne J, Burgess A (2002). Surface improvement for structural integrity and life extension. 8as Jornadas de Fractura, UTAD, Vila Real de Trás os Montes, Portugal, Ed. by SPM (Portuguese Society of Materials)


