EFFECTS OF RESIDUAL STRESS IN CREEP CRACK GROWTH ANALYSIS OF COLD BENT TUBES UNDER INTERNAL PRESSURE

O. Kwon*, B. Pathiraj**, K.M. Nikbin+

Abstract

In this paper, the effects of residual stresses are considered in crack growth of cold bent and longitudinally cracked carbon-manganese tubes and pipes tested under pressure at 360 °C, using the fracture mechanics parameters reference stress, σ_{ref} , K and C^* . Residual stress measurements using the X-Ray diffraction technique and a successive layer removal method have been performed through the thickness of the pipe extrados. These data have been used in finite element analyses to model effects of the secondary stresses acting on the crack tip. Creep crack growth rates versus C^* at 360 °C, in cold bent tubes, were shown to be faster by a factor of ~50 at constant C^* compared to cracking in fracture mechanics CT test specimens. This difference is due to geometry as well as the method of C^* analysis. In cold-bend tubes after 1000 hours thermal soaking at 650 °C, there was an increase in incubation time and steady state cracking rate reduced by a factor of 10. Furthermore, it was found that residual stress measurements performed after a thermal treatment at 360°C for 50 hours indicated a reduction of 40-50%. However, the calculations of C^* using a combination of primary plus secondary stresses showed only an increase by a factor of ~2 which did not fully explain the difference in cracking rate in the tube specimens.

Keywords: residual stress, creep crack growth, initiation, piping, internal pressure

Introduction

In life assessment, the effect of a pre-existing stress field on the behaviour of a crack at high temperatures needs to be addressed. In the power generation industry, the R5 and A16 [1-2] design codes use linear and non-linear fracture mechanics parameters such as K and C^* to predict initiation and crack growth of components at elevated temperatures. It is clear that in these assessment methods a correct evaluation of the relevant fracture mechanics parameters, on which the lifetime predictions are dependent, will largely determine the accuracy of the life predictions. Methods for calculating the parameters vary thus giving differences in final predictions. The calculations are further complicated by the presence of residual stresses resulting from fabrication processes. Residual stresses in engineering components can significantly affect their load carrying capacity and resistance to fracture. They arise from regions of plastically strained material, which are constrained

+Department of Mechanical Engineering, Imperial College, London, UK , (corresponding author)

* Materials Performance Technologies Ltd., PO Box 2225, Auckland

**Metsearch, B.V., Enschede, Netherlands

by surrounding elastically deformed material. Residual stresses are most commonly encountered in the vicinity of weldments where they are notorious for promoting cracking. Also where a component has been plastically loaded or cold bent, residual stresses will be induced. They can also be introduced through thermal and mechanical overloading conditions during in-service operation [3].

In fabrication processes including cold bending of tubes, the tube experiences inhomogeneous plastic deformation that leads to development of residual stresses. The magnitude of the residual stresses developed often extends up to the yield strength of the material. When combined with the operational pressure, they give rise to a complex stress state acting on in-service components. The state of stress, which results from a combination of the residual and applied stresses, can affect the fracture mechanics parameters K and C^* when the stresses act in the same direction normal to the crack. Incubation and propagation times for cracking under static and cyclic creep loading are likely to be affected by these residual stresses. Residual tensile stresses, which augment applied tensile stresses in a component, will have a detrimental effect on structural integrity, and are likely to accelerate the growth of crack-like defects.

In this paper, experimental crack growth data of cold bent tubes containing residual stress due to cold bending are analysed using the numerically calculated fracture mechanics parameters K and C^* . The residual stress profiles were experimentally obtained and used as input in finite element (FE) non-linear stress analysis to derive the relevant K and C^* parameters. Comparison of results is then made between laboratory sized fracture mechanics test specimen data and those of tubular specimens.

Materials and Tests

Only a summary of test methods will be presented in this paper. The details have been previously reported by [4] collaborating under an EU sponsored Brite/Euram project [see acknowledgements]. The chemical composition, dimensions of the tube and the creep material properties are shown in Tables 1 and 2 and fig. (1a). Table 2b shows the material constants used in the creep power law based on an average creep rate. This information was used in the Finite Element calculations using the $\frac{1}{2}$ section 2D model shown in fig. (1b). A 2D model was sufficient to model the 3D crack as the actual crack dimensions $a/2c$ ($2c$ is the axial length of the crack on the surface) found in the tubes were ≥ 0.1 .

Static pressure tests were conducted on Carbon-Manganese steel tubular components at 360 °C for test durations ranging between 1000 and 5000 hours. The data were analysed using the fracture mechanics parameters K and C^* and compared with the data from compact tension (CT) and three point bend specimens (3PB) tested following the standard [5]. The tubular components were internally pressurised using a pneumatic system according to [4]. A shallow axial crack was introduced on the outer surface of each of the pipe and tube specimens as shown in fig. (1a) using the spark erosion machining technique. An initial crack depth of $a_o = 1\text{mm}$ was produced keeping an $a_o/2c$ ratio of 0.1. In order to monitor the crack depth growth during testing the AC potential drop technique was employed.

Measurement of Residual Stresses

Non-destructive measurements of residual stress and strains were made using the X-Ray diffraction technique [6]. Data from a number of specimens were used to address the overall scatter of the results. Surface residual stress measurements were carried out according to the X-ray multi-exposure $\sin^2\Psi$ method [6]. A Rigaku X-ray stress analyser (MSF-2M) was used for the measurements. For each stress computation, strains acting along 7 to 8 Ψ -directions were measured. The irradiated area was restricted to 10 mm X 1 mm, with the aid of a special divergent slit.

TABLE 1 - Nominal Chemical Analyses of C-Mn steel (Weight %)

C	B	Si	Mn	P	S	Cr	Mo	Ni	Cu	Al	N
0.17	0.24	0.87	0.015	0.015	0.09	0.05	0.2	0.15	0.005	0.017	0.0101

TABLE 2a - Nominal dimensions of the tube specimens, 2b) Material creep properties data (360 °C, $\dot{\epsilon}_o = 1/h$) using eqn. 1

2a)	Component geometry (mm)				2b)	n	σ_o (MPa)	E (GPa)	ν
Specimen	R_o	R_i	W	a_o					
Tube/Bend	31	24	7.13	1.0		10.7	1080	195	0.3

Residual stress measurements were made through the thickness of a number of pipes and cold bent tubes in their extrados region, using a successive layer removal technique. The results, which are presented in fig. 2, also include pipe material produced from the same material but with double the dimensions of the tube in order to compare thickness effects. At each step, a layer of material typically about 150 μm in thickness, was removed electrochemically and the X-ray measurements were carried out. This process was repeated in several steps covering the entire thickness. Results of the last few measurements, made at depths close to the inner surface, are not reliable as there was puncturing of the last remaining material, while electro-polishing. No account has been taken in fig. 2 of the effects of layer redistribution on the actual residual stresses. Therefore the results for the longer crack lengths, $a/W > 0.5$ are progressively inaccurate. However as stable cracking in actual tests occurred in the region of $a/W < 0.5$ this had little effect on the analysis.

Fig. 2, which shows the measured residual stresses (hoop stress) for the tubes and pipes, suggests that there is a thickness effect. The depth is therefore normalised as a percentage of the total thickness, to enable easy comparison of the results. The figure indicates that there is generally a linear variation of the residual stress distributions. The maximum and minimum stress values were found to be of magnitude 100 MPa, coinciding on the outer and inner surfaces, respectively. The scatter range is ± 50 MPa. Residual stress measurements made after a thermal treatment at 360°C indicated that even at this relatively low temperature, surface stresses in some areas could be reduced by as much as 40 - 50% in 50 hours. However, it is noteworthy to remark that a total relaxation was not apparent even after heat-treating for 1500 hours.

Finite Element Analysis

Using [7] suitable 2-dimensional meshes of second-order elements (eight-node quadrilaterals) were produced to represent a pipe geometry. Owing to symmetry, only one half of the pipe was modelled as shown in fig. (1b). The mesh consisted of 356 biquadrilateral elements with 128 being focused in the semicircular patch around the crack tip. In order to effectively model the crack tip singularity, 1/4-point collapsed quadrilateral elements were used around the crack tip. This provides the correct elastic strain singularity of order $1/\sqrt{r}$ at the crack tip. The mesh could be modified by shifting the crack tip element along the crack line to model different crack lengths for analysis. Normalising crack lengths a/W of 0.08, 0.25, and 0.39 were used to correspond with the crack range from the experimental observations.

Representative boundary conditions were chosen as schematically shown in fig. 1b) with the point X (shown on the figure) fixed. The strain energy release rate G and the $C(t)$ integral were calculated using ABAQUS and values of K were evaluated from G using the relationship $G=(K^2/E')$ where $E'=E/(1-\nu^2)$ is Young's modulus in plane strain. Steady state C^* values were calculated assuming a steady state creep law,

$$(\dot{\epsilon}/\dot{\epsilon}_0) = (\sigma/\sigma_0)^n \quad (1)$$

where $\dot{\epsilon}$ and σ are the creep strain rates and applied stress and $\dot{\epsilon}_0, \sigma_0$ and n are material constants. ABAQUS uses the domain integral method to evaluate C^* . In this case, the integration takes place over a finite domain surrounding the crack front by using an area integral in two dimensions and a volume integral in three dimensions. The modelling of the residual stress by applying a stress distribution on the crack surface assumes that the residual stress is acting as a primary stress. No account is taken of the redistribution of the residual stress with either crack length or time. This method will therefore give conservative values for C^* .

The creep data shown in Table (2b) were used as material input data for the elastic-creep finite element analysis. The creep index for the material varied between $n=5, 19$. A weighted average n value of 10.7 was used for the FE modelling. Numerical problems which are sometimes encountered in the ABAQUS programme, when analysing creeping materials at very low strain rates and creep indices $n \gg 10$ [8], were therefore bypassed.

Two load cases were run for three different crack lengths. The first modelled the primary case (P) using an applied internal pressure of 70 MPa. In the second case, the combined primary and secondary loading (P+S) due to the residual stresses was modelled. For the addition of the secondary residual stresses, the best fits from X-ray experimental measurements across the thickness of the pipe and the tubes are used.

Fig. 2 shows the distribution of the residual stresses (hoop stress) through the wall thickness of the cold bent tubes and pipes measured using the x-ray diffraction technique. This figure was simplified as shown in fig. (3a) giving a linear distribution ranging

between ± 100 MPa. The tensile residual stresses acting at the depth of the notch were then applied on the crack surface of the model as shown in fig. (3b) combined with the applied internal pressure of 70 MPa. The distribution was applied for all time steps taking no account of relaxation. This therefore gave the worst-case results. The fracture mechanics parameters K and C^* were evaluated for the external axial cracked pipe subjected to an internal pressure only and combined stresses. The differences in the K and C^* values are compared in Table 3 for three different crack lengths. The combined Primary plus Secondary stresses (P+S) in the range of crack length calculated, increase K by a factor of 1.4 and C^* values by a factor of about ~ 2 .

Crack growth analysis

Several parameters such as stress intensity factor, K [9], reference stress, σ_{ref} [10] and C^* [11] have been used to correlate creep crack growth rate data of cracked components at elevated temperatures. The correlations of steady state crack growth rate with K , reference stress and C^* can be represented by straight lines of different slopes on log/log plots and expressed by power laws of the form

$$\dot{a} = AK^m \quad (2)$$

$$\dot{a} = H(\sigma_{ref})^p \quad (3)$$

$$\dot{a} = D_o C^{*\varphi} \quad (4)$$

where A , H , D_o , m , p and φ and are material constants. A steady state relationship between crack growth rate and the parameters in eqn. 2 and 3, physically imply a progressively accelerating creep crack growth rate. The elastic stress intensity factor K and the C^* parameter have generally been proposed for creep-brittle and creep-ductile materials, respectively. However it is necessary to verify the suitability of any of these parameters with respect to crack growth prediction in creep-brittle materials, since no guidelines for the choice of correlating parameters have been clearly defined.

TABLE 3 - Ratios of the FE evaluated K and C^* values using primary (P) load due to internal pressure and a combination of the primary (P) and the secondary load (S) due to the residual stress giving (P+S)/P

Crack a/W	$K(\text{MPa}\sqrt{\text{m}})$ Ratio (P+S)/P	$C^*(\text{J/m}^2\text{h})$ Ratio (P+S)/P
.08	1.4	1.81
.25	1.4	2.85
.39	1.3	2.6

Estimates of C^* can be obtained by experimental, numerical and limit analysis methods. The experimental procedure is applicable to laboratory specimens as specified in ASTM E1457 [5] and the other two methods are needed when C^* is calculated for components. The creep power law shown in eqn. 1 is used to define the creep properties of the material here.

Experimentally C^* is calculated (ASTM E1457 [5]) from the general relationship,

$$C^* = (P\dot{\Delta}_c / WB_n)F \quad (5)$$

where $\dot{\Delta}_c$ is the load-line creep displacement rate, F is a non-dimensional factor which can be obtained from limit analysis techniques [12], B_n is the net thickness of the specimen with side-grooves and W is the width. In general, eqn. (5) is used to estimate the values of C^* for tests in the laboratory.

Another method, which has been widely adopted in life assessment codes, is one based on reference stress concepts [13]. Reference stress procedures are employed to evaluate C^* for feature and actual component tests where the load-line deformation rate is not available. By determining;

$$C^* = \sigma_{ref} \dot{\epsilon}_{ref} \left(\frac{K}{\sigma_{ref}} \right)^2 \quad (6)$$

where $\dot{\epsilon}_{ref}$ is the creep strain rate at the reference stress, σ_{ref} , and K is the stress intensity factor. Usually it is most convenient to employ limit analysis to obtain σ_{ref} from

$$\sigma_{ref} = \sigma_y \frac{P}{P_{lc}} \quad (7)$$

where P_{lc} is the collapse load of a cracked body and σ_y is the yield stress. The value of P_{lc} will depend on the collapse mechanism assumed and whether plane stress or plane strain conditions apply. σ_{ref} can be derived from either limit load solutions [14] or directly from numerical calculations using elastic/plastic runs in ABAQUS.

In this paper, eqn. (5) has been used to evaluate C^* for the CT and the 3PB test data and eqn. (6) has been used to calculate C^* and analyse the component data using the K and the σ_{ref} parameters derived from FE computations.

Discussion

In order to compare characterization of creep crack growth by the various fracture mechanics parameters discussed, the data on cold bent tubes with different soaking times were represented in terms of the stress intensity factor K , reference stress σ_{ref} and the creep fracture parameter C^* in figs. 4 to 7.

Fig. 4 shows the correlation of crack growth as a function of the stress intensity factor. There appears to be an incubation period followed by a steady cracking rate. The creep crack growth rate data for the tubes show a clear trend and move nearer to the scatter band for the CT specimens. For longer soak times (at 650 °C), the rate of cracking drops by about factor of 10 suggesting a substantial decrease in residual stresses caused by cold-bending. The figure suggests that K could be used as a correlating parameter for creep

crack growth in this material. This could be due to the adoption of 360 °C as test temperature, giving rise to a more creep-brittle behaviour. However, from calculation of the plastic zone it was found that the extent of initial plasticity on loading contributes to initial crack tip blunting, thus reducing the crack tip singularity.

The reference stress σ_{ref} was used to correlate the creep crack growth behaviour of the components as shown in fig. 5. As expected, the data do not correlate for the cold bent tubes and also the data are not easily comparable to the CT and the 3PB specimen data. However, the trend with soak times is clearly identifiable.

Fig. 6 illustrates the correlation of CCG rates with the C^* parameter for the cold bent tubes with respect to the conventional laboratory specimen CT and 3PB data. C^* for the laboratory geometries were calculated using eqn. 5. C^* parameters for the tubes were evaluated from the reference stress method using the relationship of the uniaxial creep data shown in Table 1a. The NSW model [11] has been developed to predict the plane stress and plane strain crack growth rate. The model can be simplified to the following expression, which has shown good agreement with a broad range of experimental data:

$$\dot{a} = \frac{3C^{*0.85}}{\epsilon_f^*} \quad (8)$$

with crack growth rate $\dot{a}$ in mm/h and C^* in MPa m/h. In eqn. (8) ϵ_f^* is the creep ductility appropriate to the state of stress at the crack tip. It is usually taken to be the uniaxial ductility (ϵ_f) for plane stress conditions and $\epsilon_f/50$ for plane strain conditions. The value $\epsilon_f/50$ can confidently be reduced to $\epsilon_f/30$ when batch specific materials are analysed. Because of the large variation in n for this material at 360 °C the approximate predictions in eqn. (8) used the uniaxial ductility of $\epsilon_f=0.5$ and two different power indices $\phi=0.85, 0.95$, determined from the creep index relationship $\phi=n/n+1$. Both predictions are included in figs. 6 and 7. It was found that the plane strain estimate predicts the crack growth behaviour for the CT, and 3PB specimens are in close agreement with the plane stress predictions.

The effect of stress relaxation caused by the thermal soaking on cracking rate is well illustrated in fig. 6. It is clearly seen that the creep cracking rate is decreased with increase in soaking time which seems to be related to the reduction in the effects of cold working. When the cold bent tube was thermally soaked for a sufficient time to relax the residual stresses significantly, the CCG data of the cold bent tubes were found to be similar to those of the straight tubes and pipes. The initial ‘tail’ for the three tests which determine the length of crack incubation prior steady growth also show similar trends due to soak times showing longer ‘tails’ for the specimen with a 936 h soak time.

In order to assess the influence of the residual stress distribution on creep crack growth, the combined primary and secondary load is used to derive a value for C^* using eqn. (6) and the variation in K shown in table 3. In fig. 6, C^* is calculated only from the primary load due to internal pressure. Fig. 7 modifies the correlation shown in fig. 6 for the short soak times. As observed in table 3, there was an approximate 40% increase in K values when the tensile residual stresses were applied to the crack surface of the tubular component FE model. The effects of residual stress on the reference stress were not

considered in the present calculations. The factor of 1.4 on K leads to an approximate factor of 2 increase in C^* using eqn. (6), corresponding with the increase in the numerically derived values of C^* shown in table 3, suggesting that not taking into account the effect of residual stress in the reference stress in eqn. (6) has made little difference. Further detailed sensitivity analysis of the FE would be required to validate this statement. In addition the use of actual material creep properties of the un-soaked condition in eqn. (1) would also make a difference.

Therefore in fig. 7 the modified C^* values for the two cold bent tubes of short soaking times are compared to C^* calculated in fig. 6. Considering fig. 7 further, it is still clear that factor of 2 increase in the value of C^* combining the primary plus secondary loads is still too small to correlate the behaviour of short soak time tests to the test with long term soak time (936 h – shown in fig. 6) where it is assumed to have little residual stresses remaining.

Absolute crack growth rates of the tube tests are faster by a factor of approximately 50 compared to the CT or 3PB specimen results at constant C^* . Comparison with K and σ_{ref} in figs. 4 and 5 give worse results. It should be noted that C^* was derived for the tube using eqn. (6) and for the CT and 3PB specimens using from eqn. (5). After 1000 hours thermal soaking, the cracking rate in the cold bent tubes reduced by a factor of 10. However this was still not comparable with the fracture mechanics specimens. Also fig. 7 only indicates a small change due to the tensile residual stress on the value of C^* . This suggests that the major difference in the cracking behaviour between CT, 3PB and tube is due to a combination of geometry effects and methods for deriving C^* .

Conclusions

The effects of residual stresses have been considered in crack growth of cold bent and longitudinally cracked carbon-manganese tubes and pipes tested under pressure at 360 °C, using the fracture mechanics parameters reference stress σ_{ref} , K and C^* . Residual stress measurements using the X-Ray diffraction technique have been performed through the thickness of the tube extrados in order to obtain the residual stress distribution across the thickness of the tubes. These results were used in FE calculations for evaluating K and C^* values numerically and evaluating C^* using the reference stress method. After 1000 hours thermal soaking, the cracking rate reduced by a factor of 10. The initial ‘tail’ indicating incubation prior to steady growth also show similar trends due to soak times showing longer ‘tails’ for the specimen with a longer soak time. Furthermore, it was found that residual stress measurements made after thermal treatment at 360°C for 50 hours indicated a reduction of 40-50%. However, the calculations of C^* , derived both numerically and via the reference stress method, using combined primary plus secondary stresses showed an increase only of about a factor of ~2. This did not completely explain the difference in cracking rate between the cold-bend and the soaked condition.

Comparisons have also been made with laboratory CT and 3PB specimens. Creep crack growth rate at 360 °C, in cold bent tubes, were shown to be faster by a factor of ~50 compared to cracking in fracture mechanics CT and 3PB specimens. As the method of C^* determinations is different in the two cases no direct conclusions can be reached. However the results suggest that geometry and methods of determining C^* should be considered further.

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Figure (1a) - Schematic drawing of a tube with an axial crack and Fig. (1b) the boundary condition used in the 2D mesh of the pipe with an external crack (assuming a large $a/2c$ ratio of > 0.1)

Figure 2 - Measured residual hoop stress distribution through the thickness (W) of pipes and tubes in the extrados region (The pipe dimensions were X2 the tube dimension)

Figure 3 - a) Representative residual stress distribution (simplified from experimental results (Fig. 2) across the thickness of the pipe b) Schematic figure of applied residual stress on crack face of the internally pressurised pipe model.

Figure 4 - Correlation of crack growth rate with K for the axially cracked cold bent tubes compared with data band of CT and 3PB specimens tested at 360 °C

Figure 5- Correlation of crack growth rate with σ_{ref} for the axially cracked straight tubes compared with data band of CT and 3PB specimens tested at 360 °C

Figure 6 - Correlation of C^* with crack growth rate for the axially cracked cold bent tubes compared with data band of CT and 3PB specimens tested at 360 °C

Figure 7 - Relation of crack growth rate for the short soak time tubes, with the modified C^* (using eqn. (6) and the change in K due to residual stress shown table 3). Comparison is also shown with the CT and 3PB data (with the C^* evaluated from eqn. (5))