THE DEPENDENCY OF THE LOCAL APPROACH TO FRACTURE ON THE CALIBRATION OF MATERIAL PARAMETERS

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ABSTRACT

Finite element (FE) simulations that employed the local approach to fracture are increasingly used in design against fracture of ductile materials and have now been included in some standards. However, a critical aspect of this approach is an accurate assessment of the local parameters by using experimental calibration procedures. Detail examination of these procedures has indicated that the simulation results are dependent on assumed initial conditions. For example, calibration of the Rice & Tracey model have been shown dependency of the void ratio (R/R_0) on the mesh size used in the FE analysis and calibration of the Gurson-Tvergaard-Needleman model parameters have shown that the void volume fraction values increase with increased initial void value.

INTRODUCTION

The estimate of the damage tolerance of an engineering structure, using fracture mechanic (FM) methods, such as K_I, CTOD and J integral is well established. However, the FM approach has inherent limitations, mainly since FM criteria do not take into account micromechanical aspects, local inhomogeneities and often the stress triaxiality. For example, Linear Elastic FM is usually limited to a well defined geometry due to the dependency of K_I on the geometry [1].

An alternative method, the so-called ‘local approach’, have been the subject of much research in recent years. It has been shown that the local approach provides a complimentary approach to the conventional fracture mechanics methods and may be used when continuity of crack growth modelling is required [2].

The local approach to fracture makes much use of non-linear FE, particularly for ductile materials. A critical aspect of this approach is an accurate assessment of the local conditions by using experimental calibration procedures. In this short paper, several material parameters were critically evaluated and compared with tests of mild steel and the sensitivity of the fracture simulations to assumptions regarding initial conditions were investigated.

EXPERIMENTAL PROGRAMME

The material used is a medium carbon steel EN8 (BS080M40) with the following chemical composition (wt/%): C 0.432, Si 0.0295, Mn 0.0745, S 0.018, P 0.006, Cr 0.029, Mo 0.005, Ni 0.033. The material Young's modulus and Poisson ratio are 199.031 GPa and 0.3 respectively.

Tests were performed on a servo-hydraulics testing machine (INSTRON 8800) and include uniaxial response of solid hourglass specimens. For the calibration of the local parameters notched round bars with three different radii (R=2 ,4 and 10 mm) have been used. In these tests the diametral change of the notch net section was measured using a transverse extensometer Epsilon Model 3575-100-ST.

THE RICE AND TRACEY VOID GROWTH MODEL

Early micromechanical studies focused on the growth of a single void in infinite elastic-plastic solid [3]. The McClintock model assumed that deformation localisation, which starts from the discontinuous sites of the materials, is developed within a narrow shear band due to the progressive material softening by the increasing porosity at the discontinuity. This process is proceeded through the analysis to describe a growth of circular holes under triaxial transverse stress. It was assumed that the relative void expansion per unit applied strain increment increased exponentially with the transverse stress. Rice and Tracey [4] developed a relation between void growth and stress triaxility to a mathematical model. The general rule for growth rate of voids was written as:

$$\frac{dR}{R} = \alpha \exp(\frac{3}{2}\xi) d\varepsilon_{eq}{}^{p} \tag{1}$$

Where α is a material constant, taken to be 0.283 in the original model, $\varepsilon_{eq}{}^{p}$ is the equivalent plastic strain, ξ is the triaxiality ratio: $\xi=\sigma_m/\sigma_0$ (σ_m is the hydrostatic stress and σ_0 is the yield stress). To take into account the strain hardening of the material, Beremin [5] suggested to replace the yield stress by the actual flow stress (σ_{eq}). This allows a straightforward integration of Eqn. 1:

$$\ln\frac{R}{R_0} = \int \alpha \exp\left(\frac{3\sigma_m}{2\sigma_{eq}}\right) d\varepsilon_{eq}{}^{p} \tag{2}$$

This relation was found to be generally valid by experimental data. Recently Huang [6] re-evaluated the theoretical deviation of the constant α, and found that a higher value of 0.427 was more realistic for most alloys.

However, since the Rice and Tracey model is based on a single void, it does not take into account the interface between voids, nor does it predict ultimate failure. A separate failure criterion must be applied to characterize microvoid coalescence. For example, the fracture might be assumed to occur when the void ratio (R/R_0) reaches a critical value $(R/R_0)_c$.

Calibration of the $(R/R_0)_c$ parameter was carried out according to ESIS suggested procedure (2) by post processing the FE results. One quarter of the notch specimen was modelled in the FE due to symmetry. An axisymmetric 4 nodes elements with four Gauss points has been used in a commercial FE code (ABAQUS/Standard) employing an elastic-plastic isotropic analysis.

The calculation of the critical value $\ln(R/R_0)_c$ was based on the evolution of plastic strain and stress triaxiality within elements at the critical location. For the notched tensile specimens this is at the net section centre, where ductile cracks may initiate. The cavity growth ratio (R/R_0) was calculated by using the following explicit integration procedure:

$$\ln\left(\frac{R}{R_0}\right)_{n+1} = \ln\left(\frac{R}{R_0}\right)_{n} + 0.283 \Delta\varepsilon_{eq}{}^{p} \exp\left(\frac{3\sigma_m}{2\sigma_{eq}}\right) \tag{3}$$

where $\ln(R/R_0)_{n+1}$ is the cavity growth rate at increment number n+1, $\ln(R/R_0)_n$ at increment number n. At the start of the analysis, $R=R_0$ and $\ln(R/R_0)$ =1.0.

The true strain was calculated as:

$$\varepsilon = 2 * \ln\left[\frac{R}{R - U_r(node\, at\, the\, tip)}\right] \tag{4}$$

Where R is the initial length of the minimum section and U_r is the radial displacement of the node at the tip. "Mean ductility" at fracture (ε_F), is [2]:

$$\varepsilon_F = 2 * \ln\left(\frac{\Phi_0}{\Phi_F}\right) \tag{5}$$

Where Φ_0 is the initial value of the diameter at the minimum section and the diameter at fracture Φ_F can be determined from the experimental load-diameter change curve (P-Δd).

The "mean ductility" values at fracture for the three different radii are shown in Table 1.

TABLE 1
"Mean ductility" at fracture (ε_F) for different notch radii.

Notch radius (mm)	R=10	R=4	R=2
"Mean ductility" at fracture	0.1751	0.1132	0.1108

The sensitivity of the RT model to the FE calibration mesh size was investigated by changing the number of the elements along the notch net section at a range of between 5 and 200 elements. The critical values of void ratio $(R/R0)_c$ versus element mesh size is shown in Figure 1. The critical void ratio value increases with the decreasing element size. However, mesh size effect is getting smaller for smaller element size, and, approximately constant for mesh size that is less than 0.2 mm.

THE GUROSN-TVERGAARD-NEEDLEMAN MODEL

Gurson originally assumed plastic yielding and damage of porous ductile material described by yield surface and a spherical void model [7]:

$$\Phi = \frac{3}{2}\frac{s_{ij}s_{ij}}{\sigma_{ys}^2} + 2f\cosh\left(\frac{3}{2}\frac{\sigma_m}{\sigma_{ys}}\right) - (1+f^2) \tag{6}$$

Where f is the void volume fraction and s_{ij} is the deviatoric stress. When f=0, Eqn. 6 reduces to the classical Von Mises yield surface with isotropic hardening. In Gurson's model, the rate of growth of the void volume fraction is written in the form:

$$\dot{f} = \dot{f}_{growth} + \dot{f}_{nucleation} \tag{7}$$

Where: $\dot{f}_{growth} = (1-f)G^{ij}\dot{\eta}^{P}_{ij}$, $\dot{f}_{nucleation} = A\dot{\varepsilon}^{pl}_{m}$ and $A = \frac{fn}{S_n\sqrt{2\pi}}\exp\left[-\frac{1}{2}\left(\frac{\varepsilon_m^{pl}-\varepsilon_n}{S_n}\right)\right]$

suggested by Chu and Needleman [8] for a normal distribution of nucleation.

Further micromechanical studies for materials containing periodic distribution of cylindrical or spherical voids carried out by Tvergaard [9] consider the influence of neighbouring voids to a central pair of voids, three parameters, q_1, q_2 and q_3, have been introduced in Eqn. 6:

$$\Phi = \frac{3}{2}\frac{s_{ij}s_{ij}}{\sigma_{ys}^2} + 2q_1 f \cosh\left(\frac{3}{2}\frac{q_2\sigma_m}{\sigma_{ys}}\right) - (1 + q_3 f^2) \tag{8}$$

Tvergaard and Needleman [10] further modified the Gurson model by replacing f with an effective void volume fraction, $f^*(f)$:

$$f^*(f) = \begin{cases} f & \text{for } f \le f_c \\ f_c - \dfrac{f_u - f_c}{f_f - f_c}(f - f_c) & \text{for } f > f_c \end{cases} \tag{9}$$

Where f_c, f_u, f_f are fitting material parameters. This most recent modification introduced an abrupt failure point, which approximately matched experiments.

Analysis of GTN Parameters

The GTN model described above includes a total number of nine parameters. Three parameters (ε_n, f_n, s_n) are used to model void nucleation, three parameters (f_0, f_c, f_f) describe the evolution of void growth up to coalescence and final failure, and three (q_1, q_2, q_3) characterise the yield behaviour of the materials. Commonly, it is assumed that q_1=1 and $q_3=q_2^2$, so that the number of parameter reduces to seven [11]

Since the model is based on the micromechanisms and microstructure of materials, some of the parameters, for example f_n, can be predicted from metallurgical observations. However, since no clear quantitative relation exists, the assumption could effect the fracture simulations. In the following the choice of the volume fraction of void nucleating (f_n) has been examined.

By trial and error simulations, using initial f_n value of 0.01 and ε_n and s_n values of 0.3 and 0.1 respectively, the following set of parameters correlated best the 4mm notched bar experimental results: f_0=0.002, f_c=0.006, f_f=0.15. These values were then used to carry out direct simulations of the GTN model (Eqns. 6-9) in ABAQUS/Explicit, and indirect, post-processing analysis of simulations using Gurson model (Eqn. 6) in ABAQUS/Standard. The effect on the load-displacement curve of keeping all parameters fixed but changing f_n, using the direct GTN model simulations, is shown in Figure 2. It is demonstrated that f_n has a significant effect on the GTN void volume fraction values at high radial strains.

Since the void coalescence and final fracture of GTN model (Eqn. 9) is not included in the original Gurson model, a post-processing procedure has been developed as follows. A typical relation between the void volume fraction and the true strain was obtained as shown in Figure 3a. The ratio of the increased void volume fraction over increased true strain, or the gradient ($\Delta f/\Delta\varepsilon$), was calculated and is shown in Figure 3b as a function of f. The gradient ($\Delta f/\Delta\varepsilon$) was than used to modify Eqn. 9 when f_c is reached to get the evolution void volume fraction (f^*), using the following equation:

$$f_i = f_{(i-1)} + \left(\frac{\Delta f}{\Delta \varepsilon}\right)_{f_{(i-1)}} \times \Delta\varepsilon \tag{10}$$

$$f^*_i = \begin{cases} f_c + \dfrac{f_u - f_c}{f_f - f_c}(f_i - f_c) & f_c < f_i^* \le f_f \\ f_f & f^*_i > f_f \end{cases} \tag{11}$$

In Figure 4 the void volume fracture at nucleation and fracture was obtained using the following three different methods. 1 – directly simulated in the FE model, 2 – using the void gradient method (Eqns. 10 & 11) to update the nucleation void volume and, 3 – Post processing the Gurson simulations by using Eqn. 9. It is seen that the calculated f^* obtained from post processing Gurson model with the gradient method fairly well agreed with the direct simulations of the GTN model in ABAQUS/Explicit. Particularly, the estimated final void fracture is very similar.

CONCLUDING COMMENTS

(1) The critical value in the RT model, $(R/R_0)_c$, appears to be mesh dependent if the mesh size is not reduced to less than 0.2 mm.
(2) GTN model predictions of radial displacement appeared to be sensitive to the assumed volume fraction of nucleating particles (f_n), and it is suggested that the estimation of this value is not well established.
(3) A simplified method of post-processing of the simulated Gurson model void volume fraction (f) to estimate the effective f^* prior to failure and after critical value (f_c) was reached appears to agree fairly well with a direct simulations of the GTN model.

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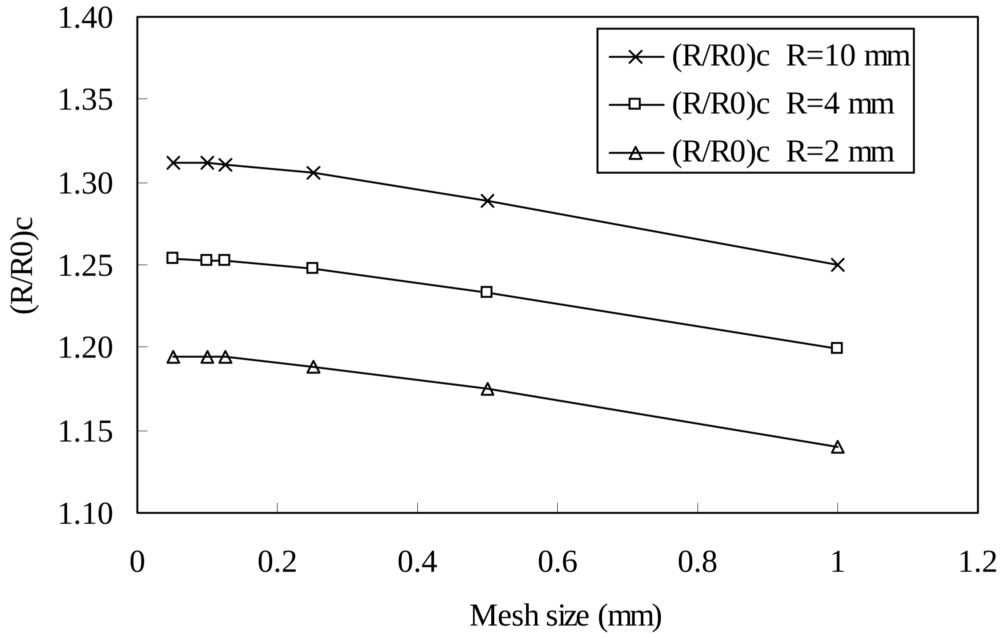

Figure 1 - Critical Void Ratio $(R/R_0)_c$ as a function of mesh size.

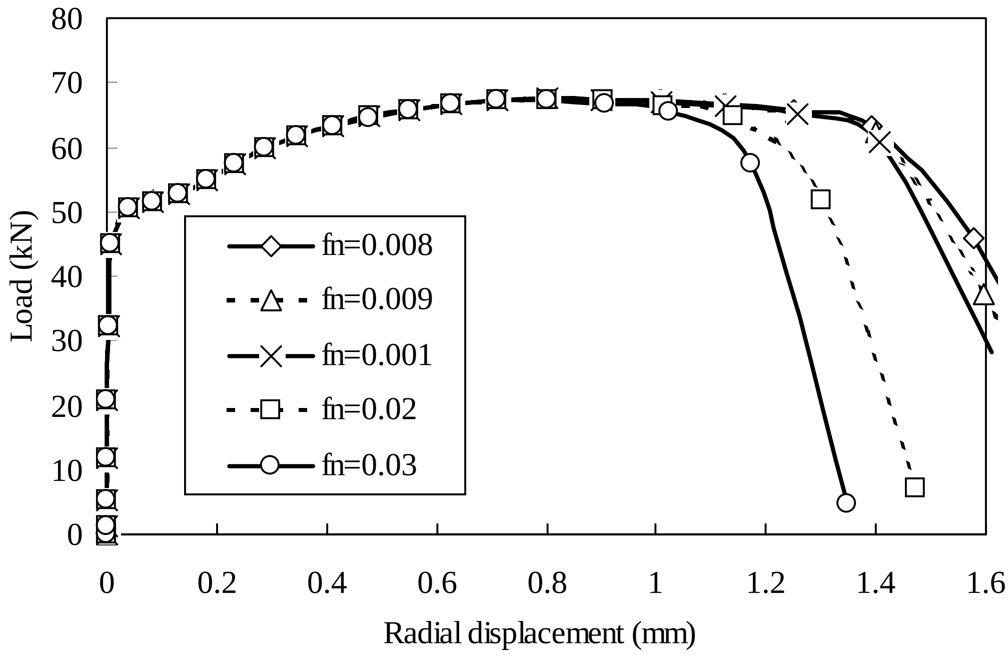

Figure 2 - Sensitivity of the GTN model to the void nucleation particles (f_n).

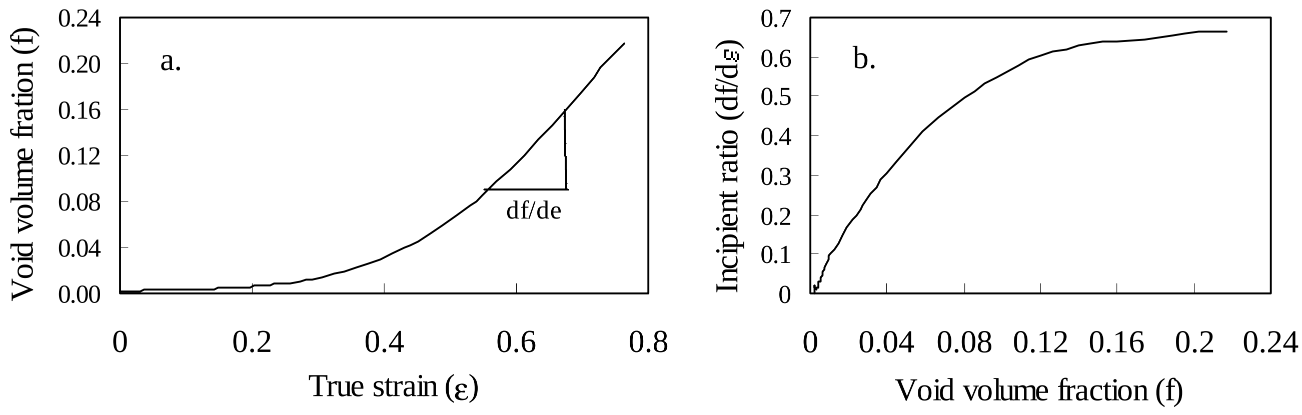

Figure 3 - Calculation of the incipient ratio gradient df/dε.

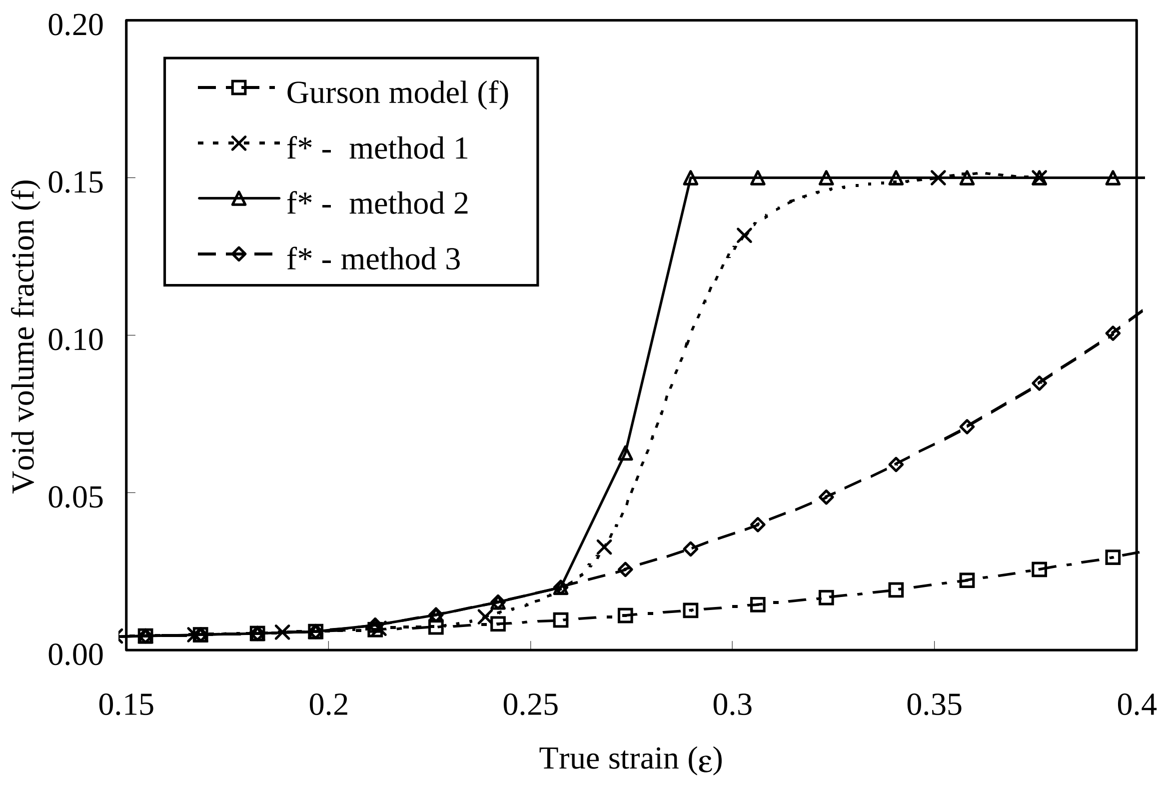

Figure 4 - Sensitivity of the GTN model to the simulation method of the effective void volume fraction , f*.