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LETTERS IN FRACTURE AND MICROMECHANICS

ULTIMATE BENDING STRENGTH EVALUATION OF U-NOTCHED DUCTILE STEEL SAMPLES UNDER LARGE-SCALE YIELDING CONDITIONS

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Abstract. The load-bearing capacity of several U-notched specimens, reported in literature, made of very ductile steel and loaded under symmetric three-point bending were theoretically estimated by using the Equivalent Material Concept (EMC), proposed originally by the author, combined with two well-known brittle fracture models namely the mean stress (MS) and the point stress (PS) criteria. The results revealed that the MS-EMC model with an accuracy of more than 90% was more efficient than the PS-EMC model with about 72% accuracy. By using the MS-EMC model, one can predict well the onset of mode I cracking in U-notched ductile components under large-scale yielding conditions without requiring conducting elastic-plastic analyzes or using ductile fracture criteria.

Keywords: Equivalent Material Concept (EMC), U-notch, Crack initiation, Mode I loading, Point stress (PS) criterion, Mean stress (MS) criterion.

1. Introduction. Depending on the brittleness and the ductility of a material, different failure modes can be recognized. For brittle materials, the applied monotonic load increases from zero to final fracture without exhibiting plastic deformations. In other words, brittle materials experience sudden fracture. For such materials, the ultimate load is usually considered as a governing parameter in mechanical design. However, for ductile materials, two main failure modes exist. The first one is yielding and the second one is the onset of cracking. Although yielding is the most common criterion in static and monotonic design, the prediction of the onset of crack initiation in ductile components is very important for the structure of some engineering products such as short-life aerial vehicles etc., because the load-bearing capacity of ductile materials is normally achieved when crack initiates. Most of the notch fracture mechanics (NFM) investigations have been already focused on the V and U-shaped notches because of their widespread applications. In this context, the great attention has been paid by the researchers to the brittle fracture as a catastrophic failure mode. For example, one can refer to Lazzarin and Zambardi (2001), Berto and Lazzarin (2009), Lazzarin et al. (2009), Ayatollahi et al. (2011), Gomez et al. (2009a, 2009b, 2008, 2000), Gomez and Elices (2003a, 2003b, 2004), Ayatollahi and Torabi (2009, 2010a, 2010b, 2010c, 2010d, 2011a, 2011b). Unlike for brittle materials, the number of fracture mechanics based analyzes reported in literature dealing with failure in notched ductile components (crack initiation and propagation) is very limited. For example, J-integral has been evaluated in the past under elastic-plastic conditions by Berto et al. (2007) as a governing parameter in fracture assessment of U and V-notched components made of ductile materials obeying a power-hardening law. J-integral has also been utilized by Smith et al. (1998) to investigate brittle mode I, brittle-ductile mixed mode and ductile mode II fracture in cracked test samples made of rotor steel. However, a valuable research in this subject has been done by Susmel and Taylor (2008a) during which the load-bearing capacity of several specimens made of a type of ductile steel and containing notches of different features (e.g. U, V and O-notches) has been estimated by using the theory of critical distances (TCD) under pure mode I loading conditions. Their tested material has been a commercial cold-rolled carbon steel exhibiting very ductile behavior. The notched specimens showed large plastic deformations around the notches after fracture. They predicted the maximum load that each notched specimen can sustain by performing linear elastic and elastic-plastic stress analyzes in conjunction with the use of the theory of critical distances (TCD) with a maximum discrepancy of about 15%. They have clearly stated in their paper that the good accuracy of TCD in the presence of large plastic deformations around notches is questionable. Although the experimental results reported in Susmel and Taylor (2008a) have been in a good agreement with the results of TCD, its application in engineering design together with linear elastic analysis cannot be prescribed from the viewpoint of fracture mechanics principles. Since elastic-plastic analyzes in the engineering design process are rather time-consuming and relatively complicated compared to elastic ones, the author has recently proposed a novel failure model to be simply used in estimating the crack emanation from V-notches in ductile materials under mode I loading conditions (see Torabi (2012)). In the author's most recent work, the tensile load-bearing capacity of V-notched specimens made of the same ductile commercial steel presented in Susmel and Taylor (2008a) has been predicted by using the combined mean stress (MS) and the equivalent material concept (EMC), i.e. the MS-EMC model. A very good agreement has been shown to exist between the experimental and the theoretical results (see Torabi (2012)). As a more applied work, Torabi (2013) has also made use of the MS-EMC model to successfully estimate the tensile load-bearing capacity of ductile steel bolts containing V-shaped threads.

In the present research, the possibility of using the two well-known brittle fracture criteria, namely the mean stress (MS) and the point stress (PS) criteria, combined with the novel equivalent material concept (EMC) in predicting the onset of mode I crack initiation in several U-notched specimens (reported in Susmel and Taylor (2008a)) under large-scale yielding conditions was examined. It was found that the PS-EMC model with a discrepancy of more than 28% could not predict the experimental results satisfactorily. Moreover, found in this work was that, like for V-notches, the MS-EMC criterion with an accuracy more than 90% could be a successful fracture mechanics based failure model to be used in advanced mechanical design related to U-notched ductile components.

2. Experimental results. A few experimental results have been reported in Susmel and Taylor (2008a) regarding mode I fracture tests on U-notched ductile specimens. Such rectangular specimens containing an edge U-notch could be divided into two sets; each tested under symmetric three-point bending. Fig. 1 displays the tested specimens (Susmel and Taylor (2008a)). The distances between the two supports have been 100 mm and 75 mm for the specimens of the sets (a) and (b), respectively. The test samples have been made from a type of commercial cold-rolled low-carbon steel, namely En3B having very ductile behavior. The mechanical properties of the steel are presented in Table 1 (Susmel and Taylor (2008a)). The value of K_{Ic} has been determined experimentally by means of testing the C(T) specimens of 85 mm thick in accordance with the ASTM E399 (Susmel and Taylor (2008a)). A total number of six specimens have been tested; three for each of the sets (a) and (b) (see Fig. 1).

The parameters E, σ_Y, σ_u, ε_f, K_{Ic}, K and n denote the elastic modulus, the yield strength, the ultimate tensile strength, the strain to rupture, the plane-strain fracture toughness, the strain hardening coefficient and the strain hardening exponent, respectively.

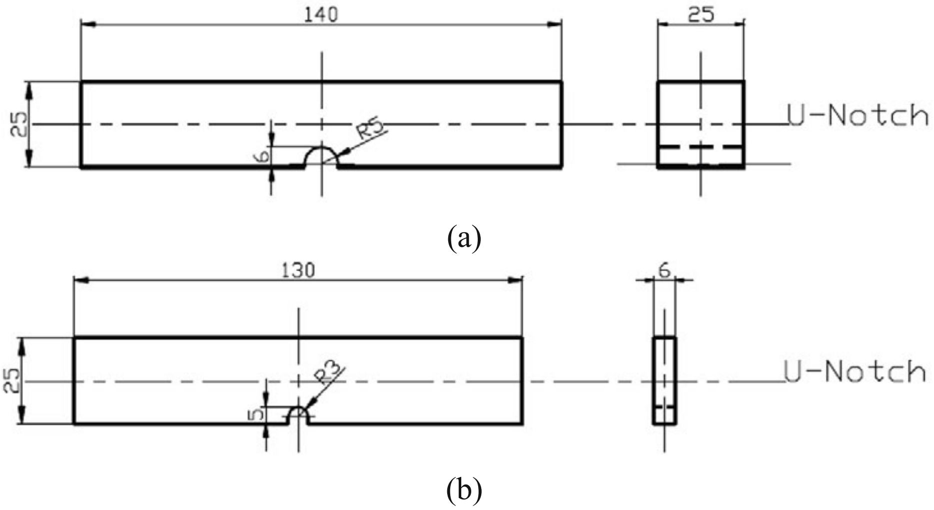

Figure 1. The tested U-notched specimens reported in Susmel and Taylor (2008a).

Table 1. The mechanical properties of the En3B steel (Susmel and Taylor (2008a)).

σ_u *(MPa)*	σ_Y *(MPa)*	*E (GPa)*	$K_{Ic}\,(MPa\sqrt{m})$	*K (MPa)*	*n*	ε_f
638.5	606.2	197.4	97.4	882.7	0.06	0.56

The experimental values of the maximum loads that the specimens could sustain (i.e. the load at the onset of crack initiation from the U-notch tip) are presented in Table 2 (Susmel and Taylor (2008a)).

Table 2. The load-bearing capacity of the U-notched specimens (Susmel and Taylor (2008a)).

Specimen	Load-bearing capacity for the set (a) (*kN*)	Load-bearing capacity for the set (b) (*kN*)
1	96.1	33.6
2	93.7	32.3
3	95.7	33.1
Average	95.2	33.0

3. The equivalent material concept (EMC)

The equivalent material concept (EMC) has been elaborated in Torabi (2012, 2013). Nevertheless, it is briefly described herein for convenience. Using the EMC, one can imaginarily consider in failure studies a virtual brittle material exhibiting linear elastic behavior instead of ductile material with elastic-plastic behavior. Thus, brittle fracture criteria may be utilized to study the failure in ductile materials. According to EMC, the strain energy density (SED) for ductile material is assumed to be equal to that for a virtual brittle material with the same elastic modulus. For a ductile material with significant plastic deformations and with exhibiting power-law strain-hardening relationship in the plastic zone, one can write the true stress-strain relationship as

$$\sigma = K{\varepsilon_P}^n \tag{1}$$

In Eq. 1, σ and ε_p are the stress and the plastic strain, respectively. The total SED can be written in a general form of elastic-plasticity as

$$(\mathrm{SED})_{\mathrm{tot.}} = (\mathrm{SED})_{\mathrm{e}} + (\mathrm{SED})_{\mathrm{p}} = \frac{1}{2}\sigma_{\mathrm{Y}}\varepsilon_{\mathrm{Y}} + \int_{\varepsilon_p^Y}^{\varepsilon_{\mathrm{p}}} \sigma \, d\varepsilon_{\mathrm{p}} \tag{2}$$

Substituting $\varepsilon_Y = \dfrac{\sigma_Y}{E}$ and Eq.1 into Eq. 2 gives

$$(\mathrm{SED})_{\mathrm{tot.}} = \frac{{\sigma_{\mathrm{Y}}}^2}{2\mathrm{E}} + \int_{\varepsilon_p^Y}^{\varepsilon_{\mathrm{p}}} K{\varepsilon_P}^n d\varepsilon_{\mathrm{p}} \tag{3}$$

Thus

$$(\mathrm{SED})_{\mathrm{tot.}} = \frac{{\sigma_{\mathrm{Y}}}^2}{2\mathrm{E}} + \frac{K}{n+1}((\varepsilon_P)^{n+1} - (\varepsilon_p^Y)^{n+1}) \tag{4}$$

If ε_p^Y is considered to be equal to 0.002 (obtained from 0.2% offset yield strength), then

$$(\mathrm{SED})_{\mathrm{tot.}} = \frac{{\sigma_{\mathrm{Y}}}^2}{2\mathrm{E}} + \frac{K}{n+1}({\varepsilon_P}^{n+1} - (0.002)^{n+1}) \tag{5}$$

In order to calculate the total SED corresponding to the onset of crack initiation (i.e. the area under the true σ-ε curve from beginning of loading to the maximum load), one can replace ε_p in Eq.5 with $\varepsilon_{u,\mathrm{true}}$; i.e. the true strain at maximum load; which could be obtained by recording the length of the gage section of the standard tensile test specimen at maximum load (one can utilize simply $\varepsilon_{u,true} = \ln(l_u / l_0) = \ln(1 + \varepsilon_u)$ where l_0, l_u and ε_u are the initial length and the length of the gage section at maximum load, and the engineering strain at maximum load, respectively.).

$$(\mathrm{SED})_{\mathrm{tot.}} = \frac{{\sigma_{\mathrm{Y}}}^2}{2\mathrm{E}} + \frac{K}{n+1}({\varepsilon_{u,true}}^{n+1} - (0.002)^{n+1}) \tag{6}$$

The equivalent material considered in EMC is a virtual brittle material with the same values of the elastic modulus E and the plane-strain fracture toughness K_{Ic}, but unknown value of ultimate tensile strength. Fig. 2 shows schematically a sample uni-axial stress-strain curve for the virtual brittle material. In Fig. 2, the parameter ε_f^* and σ_f^* are the strain at crack initiation (i.e. the final fracture due to the brittleness) and the ultimate tensile strength, respectively. The SED for this material at crack initiation is therefore

$$(\mathrm{SED})_{\mathrm{EMC}} = \frac{{\sigma_f^*}^2}{2\mathrm{E}} \tag{7}$$

On the basis of EMC, SED values for both the real ductile and the virtual brittle materials should be equal. Hence, Eqs. 6 and 7 are equal. Thus

$$\frac{{\sigma_f^*}^2}{2\mathrm{E}} = \frac{{\sigma_{\mathrm{Y}}}^2}{2\mathrm{E}} + \frac{K}{n+1}({\varepsilon_{u,true}}^{n+1} - (0.002)^{n+1}) \tag{8}$$

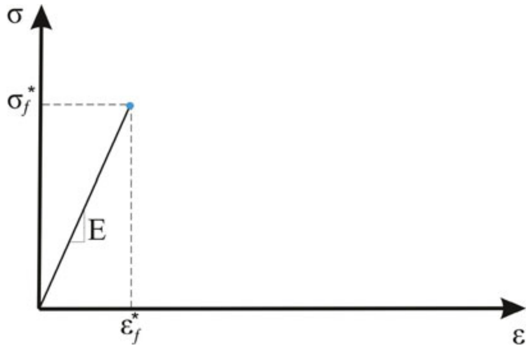

Figure 2. A sample uni-axial stress-strain curve for the virtual brittle material.

Ultimately, σ_f^* is

$$\sigma_f^* = \sqrt{\sigma_{\mathrm{Y}}{}^2 + \frac{2EK}{n+1}(\varepsilon_{u,true}{}^{n+1} - (0.002)^{n+1})} \tag{9}$$

The parameter σ_f^* presented in Eq. 9 may be utilized together with K_{Ic} in any brittle fracture models to estimate the onset of crack initiation in notched ductile components in the presence of large-scale yielding conditions around the notch.

4. The mean stress (MS) criterion

One of the most well-known brittle fracture criteria in notched domains under pure mode I loading conditions is the mean stress (MS) criterion. This fracture model has been successfully employed in the past by Ayatollahi and Torabi (2010 a,b) in rounded-tip V-notches. According to the MS criterion, fracture takes place in any notched component when the mean value of the tensile stress over a specified critical distance ahead of the notch tip attains a critical value. A closed-form expression has been suggested in literature by Ayatollahi and Torabi (2010a) for mode I notch fracture toughness of rounded-tip V-notches $K_{Ic}^{V,\rho}$ as

$$K_{Ic}^{V,\rho} = \frac{\sqrt{2\pi}\,(\sigma_{\theta\theta})_c\, d_c}{\frac{1}{\lambda_1}\left[d_c^{*\lambda_1} - r_0^{\lambda_1}\right] + \frac{n_{\theta\theta}(0)}{\mu_1 r_0^{\mu_1-\lambda_1}}\left[d_c^{*\mu_1} - r_0^{\mu_1}\right]} \tag{10}$$

If the notch angle is zero, then the rounded-tip V-notch becomes a U-notch. Therefore, Eq. 10 can be simply used also for U-notches by substituting the corresponding values of the parameters in Eq. 10 (in this case, $K_{Ic}^{V,\rho}$ can be denoted by K_{Ic}^{U}). In order to compute the theoretical value of the critical load associated with any value of K_{Ic}^{U}, a finite element (FE) model should first be created for the notched component. Then, a unit load is applied to the model and the mode I notch stress intensity factor K_I^U is computed by using Eq. 11 (Torabi and Jafarinezhad (2012))

$$K_I^U = \frac{\sqrt{\pi\rho}}{2}\sigma_{\theta\theta}\left(\frac{\rho}{2}, 0\right) \tag{11}$$

In Eq. 11, the parameters ρ and $\sigma_{\theta\theta}(\frac{\rho}{2},0)$ are the notch tip radius and the elastic tangential stress at the U-notch tip, respectively. Finally, the load is gradually enhanced till K_I^U attains K_{Ic}^U. The obtained load is, in fact, the mode I load-bearing capacity of the U-notched brittle component.

5. The point stress (PS) criterion

According to the point stress (PS) criterion, brittle fracture happens when the value of tensile stress at a specified critical distance from the notch tip reaches to the material tensile strength. Similar to the MS criterion, a closed-form expression of the mode I notch fracture toughness has also been suggested by Ayatollahi and Torabi (2010a) for the PS criterion. The expression is:

$$K_{Ic}^{V,\rho} = \frac{\sqrt{2\pi}\,(\sigma_{\theta\theta})_c\,(r_0+r_c)^{1-\lambda_1}}{(1+(1+\frac{r_c}{r_0})^{\mu_1-\lambda_1}\,n_{\theta\theta}(0))} \tag{12}$$

Note that Eq. 12 has been originally suggested for rounded-tip V-notches. Again, one can consider the notch angle equal to zero and use Eq. 12 for U-notches (in this case, $K_{Ic}^{V,\rho}$ is denoted by K_{Ic}^U). The procedure of converting the value of K_{Ic}^U to the corresponding value of the applied load is completely the same with that prescribed in the previous section.

As mentioned earlier, the main target of this work is to use the pre-existing brittle fracture criteria, namely the MS and the PS criteria, to predict the onset of crack initiation from U-notches in ductile components in the presence of large-scale yielding conditions around the notch. This goal may be reached by using the EMC that equates the ductile material with a virtual brittle one having the tensile strength of σ_f^* (see Eq. 9). In fact, in the MS-EMC and the PS-EMC models, the value of σ_f^* is substituted into Eqs. 10 and 12 instead of $(\sigma_{\theta\theta})_c$ in order to estimate the mode I notch fracture toughness of the U-notched ductile component. The load-bearing capacity of ductile U-notched component may then be obtained by following the *K-to-Load* conversion procedure described in section 3.

6. Results and discussion

The theoretical result of the MS-EMC and the PS-EMC models in predicting the load-bearing capacity of the tested U-notched samples are presented in Tables 3 and 4 together with the mean values of the experimental results for the sets (a) and (b), respectively. Also, presented in Tables 3 and 4 are the discrepancies between the mean experimental and the theoretical results. Tables 3 and 4 clearly demonstrate that the MS-EMC model with about 9% discrepancy is a suitable failure criterion for predicting pure mode I crack initiation from U-notches under large-scale yielding conditions. Moreover, the PS-EMC model having an accuracy less than 72% is not a satisfactory criterion. As can be seen in Eqs. 10 and 12, the parameter K_{Ic} is essential to compute the mode I notch fracture toughness $K_{Ic}^{V,\rho}$ (K_{Ic} is hidden in the critical distances d_c and r_c). This parameter is essential in not only the MS-EMC and the PS-EMC criteria but also in almost all of the failure criteria in the field of brittle fracture. If K_{Ic} is not valid for a ductile material, both the criteria will no longer be valid to be employed. An important finding in the present research was that the strain to rupture value (i.e. ε_f) for those ductile materials having valid K_{Ic} is not an important parameter to decide that whether or not one can utilize brittle fracture criteria, of course in combination with equating models like EMC that takes in to account the effects of ductility in investigating the fracture phenomenon. In other words, brittle fracture models may also be used for not only brittle materials but also for ductile materials with valid K_{Ic}. As a conclusion from the above statements, it can be said that the MS criterion in conjunction with the equivalent material concept (i.e. MS-EMC) can be utilized in both small-scale and large-scale yielding conditions.

Table 3. The results of the MS-EMC and the PS-EMC criteria together with the mean value of the experimental results for the set (a) including the discrepancies.

	Load-bearing capacity *(kN)*	Discrepancy (%)
PS-EMC	66.6	30
MS-EMC	86.6	9
Mean experimental	95.2	------

Table 4. The results of the MS-EMC and the PS-EMC criteria together with the mean value of the experimental results for the set (b) including the discrepancies.

	Load-bearing capacity *(kN)*	Discrepancy (%)
PS-EMC	24.7	25
MS-EMC	30.2	8.3
Mean experimental	33.0	------

As we aware, the elastic-plastic analysis is usually very time-consuming and relatively complicated in comparison with the linear elastic analysis. Therefore, linear elastic analyzes are generally preferred in real engineering applications. Consequently, it can be stated from the viewpoint of engineering design that the accurate MS-EMC failure model is very simple and convenient to use, because the corresponding analysis is completely linear elastic. Despite accuracy, the major advantage of the MS-EMC criterion is that it provides a short and justifiable path to estimate the tensile load-bearing capacity of U-notched elements experiencing large-scale yielding around the notch border without requiring performing elastic-plastic stress analysis.

7. Conclusions

A combination of the mean stress (MS) and the point stress (PS) brittle fracture criteria with the novel equivalent material concept (EMC) was employed to predict the tensile load-bearing capacity of a few U-notched specimens made of a type of ductile steel. It was found that like for V-notches, the MS-EMC criterion was efficient also for U-notches. Moreover, the PS-EMC model was demonstrated to be inefficient when applied to mode I fracture of U-notched components in the presence of large-scale yielding conditions. The MS-EMC criterion is valid for those metallic materials having valid K_{Ic} values. The MS-EMC model may be valid for ductile materials with both small and large-scale yielding. This fact was demonstrated in this work for U-notched samples with significant yielding area around the notches. The applicability of the MS-EMC criterion in practical engineering applications is very convenient since it does not require time-consuming elastic-plastic analysis.

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