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Identification of ductile damage parameters for pressure vessel steel

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HIGHLIGHTS

- We identified ductile fracture response of both austenitic and ferritic steels.
- We arranged set of experiments including rods, butterfly, small punch.
- We used responses for calibration of fracture locuses of both steels.
- We analyzed specimens with high stress concentration.
- Applied material models are not accurate enough in the latter case.

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ABSTRACT

The FEM simulations in the field of design and safety assessment represent very powerful tools, but they are strongly limited by available material models and material input data. If states near to ductile fracture are to be considered, more complex material description taking into account multiaxial loading conditions is necessary. As complex material models suitable to include these effects into practical simulations are still mostly phenomenological, experiments with samples of various geometries tested under various loading modes have to be used. On the basis of these tests a complex material behavior model covering elastic and plastic material behavior for various stress states can be obtained. This kind of the material behavior description allows a wide range of application from calculation of component limit loading conditions to material properties conversion for samples of different sizes e.g. This paper deals with ductile damage parameters determination for two typical Reactor Pressure Vessel (RPV) steels ferritic and austenitic. The ferritic steel is used for the RPV vessel and the austenitic one is used for internals. There are chosen appropriate samples geometries based on the preliminary FEM stress state analyses of samples at first. Subsequently, testing of proposed samples is performed and material parameters are evaluated. The obtained material plastic damage parameters are subsequently applied to FEM simulation of sharp notched samples and capabilities of applied models to describe material behavior for high stress concentrations is assessed on the basis comparison with real tests.

1. Introduction

Ductile damage is the process of metallic material damage under conditions of monotonic loading. Evolution of the damage follows plastic straining and ends by fracture of component. Problems of ductile damage play significant role in industry, for example in optimization of technological processes, evaluation of safety in automotive and aeronautic industry namely in cases of crashes or emergency states, analysis of steel civil structures etc. Many of the current calculations are still performed on the basis of standard tensile tests, if not only on database data or data from literature. Such a material description is not sufficient for an accurate design assessment and detailed material behavior description has to be used for reliable results of complex simulations. Standard tensile test is mainly based on uniaxial sample loading and small strains. The standard tensile test results are useful for elastic solutions or elastic-plastic solutions for a small plastic strain. If states near

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to ductile fracture are to be considered, more complex material description taking into account multiaxial loading conditions is necessary (Bai and Wierzbicki, 2008; Bai et al., 2010; Wierzbicki et al., 2005; Bao and Wierzbicki, 2004; Li and Wierzbicki, 2009). Complex material models of ductile fracture require calibration based on extensive experimental tests.

Phenomenological material models describing ductile damage in continuum mechanics mostly introduce extension of plasticity models. From the point of view of coupling plasticity and failure description, two types of material models can be distinguished. Uncoupled models separate plastic response from influence of ductile damage and failure. Coupled models modify plastic response in dependence on damage evolution. Even though coupled models have huge potential, their complexity and calibration costs result into small extension in practice. Easier calibration process is an essential advantage of uncoupled material models, for which the calibration of plastic response and calibration of ductile damage can be separated. The calibration is significantly easier when the uncoupled material model is used.

In the current paper uncoupled material models using Johnson–Cook, Rice–Tracey and Bai–Wierbitzski damage description are applied to two steels: ferritic one and austenitic one used in RPV applications. The ductile damage parameters are determined for considered materials on the basis of experimental results and simulations using finite element method (FEM). The damage parameters are subsequently applied to simulation of notched fracture mechanics samples designed as CT (Central Tension). The results of simulation of CT samples are compared with the experimental results.

2. Ductile damage model

Material model discussed in this paper is based on both classical incremental model of plastic response with isotropic hardening and phenomenological concept of damage in continuum mechanic. This model supposes isotropy, and for description of stress state uses Von Mises stress q, stress triaxiality η, and Lode parameter ξ. These quantities are defined using second J_2 and third J_3 invariant of deviatoric stress, Eq. (1).

$$J_2 = \frac{1}{2}(S_1^2 + S_2^2 + S_3^2) \qquad J_3 = S_1 S_2 S_3 \cdot \tag{1}$$

Principal deviatoric stresses S_1, S_2 and S_3 are the eigenvalues of the stress deviator, Eq. (2).

$$\mathbf{S} = \boldsymbol{\sigma} + p\mathbf{I}, \tag{2}$$

where

$$p = -\frac{1}{3}\mathrm{tr}(\boldsymbol{\sigma}) \tag{3}$$

is hydrostatic stress. Von Mises stress is defined in Eq. (4).

$$q = \sqrt{3J_2} \cdot \tag{4}$$

stress triaxiality, η, can be expressed by Eq. (5)

$$\eta = -\frac{p}{q} \cdot \tag{5}$$

Lode parameter can be expressed by following equation

$$\xi = \frac{27}{2}\frac{J_3}{q}, \tag{6}$$

and the normalized Lode angle can be expressed according to

$$\bar{\theta} = 1 - \frac{6\theta}{\pi} = 1 - \frac{2}{\pi}\arccos\xi \cdot \tag{7}$$

We have used standard constitutive model of elastic-plastic response with isotropic hardening to determine general stress state. Yield condition is based on Von Mises surface of plasticity

$$q(\boldsymbol{\sigma}) = \sigma_Y(\bar{\varepsilon}_{pl}) \cdot \tag{8}$$

Loading history dependence is introduced through yield stress σ_Y, that depends on the only history dependent state parameter–accumulated intensity of plastic strain

$$\bar{\varepsilon}_{pl} = \int_0^t \dot{\bar{\varepsilon}}_{pl}\mathrm{d}t, \tag{9}$$

where plastic strain intensity rate, $\dot{\bar{\varepsilon}}_{pl}$, is defined as

$$\dot{\bar{\varepsilon}}_{pl} = \sqrt{\frac{2}{3}\dot{\boldsymbol{\varepsilon}}_{pl} : \dot{\boldsymbol{\varepsilon}}_{pl}} \cdot \tag{10}$$

Relation of $\sigma_Y(\bar{\varepsilon}_{pl})$ is calibrated experimentally, using uniaxial tensile test. Failure criterion is based on phenomenological quantity ω called damage. It is defined as a non-decreasing scalar parameter

$$\omega = \int_0^t \frac{\dot{\bar{\varepsilon}}_{pl}\mathrm{d}t}{\bar{\varepsilon}_f(\eta, \quad \bar{\theta})} \cdot \tag{11}$$

Damage depends on loading history and can be understood as linear accumulation of incremental damage in process of monotonic loading. Fracture locus $\bar{\varepsilon}_f$ expresses damage rate from plastic strain rate as function of stress triaxiality and Lode parameter and it has to be calibrated experimentally. Ductile failure initiation of material point occurs as soon as critical damage value ω_{crit} is reached. Usually fracture locus is calibrated to reach material failure when damage equals unity, so $\omega_{crit} = 1$. In this case fracture locus has physical meaning of accumulated plastic strain at the instant of material point failure initiation at the end of hypothetical monotonic loading with both triaxiality and Lode parameter constant.

Since unlike austenitic steel, the ferritic one has exhibited no dependence on Lode angle, Johnson–Cook and Rice–Tracey material models were employed for description of ductile damage for ferritic steel while Bai–Wierzbicki and extended Bai–Wierzbicki models were used for the austenitic one. Johnson–Cook model in general form is given in Eq. (12)

$$\bar{\varepsilon}_f(\eta, \dot{\bar{\varepsilon}}_{pl}, \hat{T}) = [d_1 + d_2 e^{-d_3\eta}]\left[1 + d_4 \ln\left(\frac{\dot{\bar{\varepsilon}}_{pl}}{\dot{\varepsilon}_0}\right)\right](1 + d_5\hat{T}), \tag{12}$$

where d_1 to d_5 are failure parameters, $\dot{\varepsilon}_0$ is the reference strain rate, and dimensionless temperature

$$\hat{T} = \frac{T - T_0}{T_m - T_0}$$

for temperature T between transition temperature T_0, below which fracture locus shows no temperature dependence, and melting temperature T_m. For $T < T_0$, $\hat{T} = 0$ and for $T > T_m$, $\hat{T} = 1$. In this paper quasi-static loading at room temperature is supposed. Therefore only the first term (parameters d_1, d_2, d_3) of Johnson–Cook model is calibrated.

Johnson–Cook material model can be understood as phenomenological generalization of Rice–Tracey model, (Rice and Tracey, 1969) that was outlined on the basis of analytic solution of growing micro-cavities in basic material matrix within the frame of classical continuum mechanics. The Rice–Tracey model is defined by

$$\bar{\varepsilon}_f(\eta) = C_{RT} e^{-\frac{3}{2}\eta}, \tag{13}$$

where C_{RT} is failure parameter that has to be calibrated. Wierzbicki and Xue (2005) extended the dependence of the fracture strain on triaxiality by the third invariant of the stress deviator. This invariant was included in the form of the Lode parameter. Xue and Wierzbicki