Effect of temperature in ductile damage models

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Abstract:
This paper describes the effect of temperature in ductile damage models. This problem has been explored within the "Identification of ductile damage parameters for nuclear facilities" project. This article analyses calibration process for the uncoupled Bai-Wierzbicki and Johnson-Cook material models that is based on fifteen tested specimens corresponding with the literature and also analyses subsequent process to obtain the fracture locus for higher temperatures. This approach was verified through FE simulation comparing each specimen with experimental data. This verification was created for two materials used in nuclear facilities.

Key words: *Identification, calibration, FEM, Ductile damage, Phenomenological material modeling, Fracture locus*

1 Introduction

Because of the growing demands on safety, reliability and longer lifetime period of nuclear facilities components it is necessary to use material data in a numerical simulation where ductile damage is considered. Ductile damage is a process of metallic material damage in conditions of monotonic loading. Evolution of the damage follows plastic straining and ends by fracture of the component.

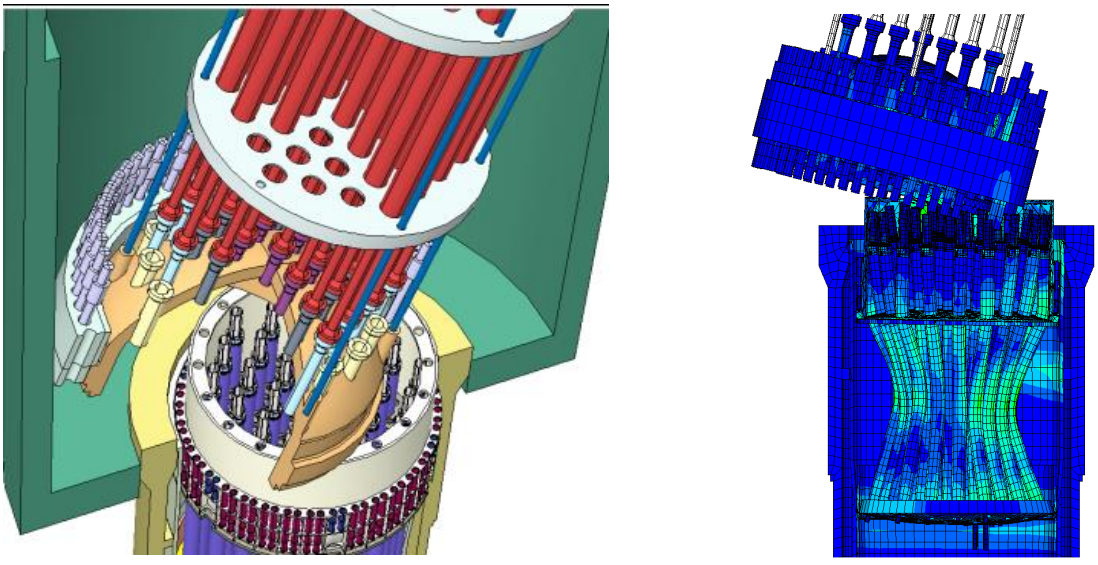

***Fig. 1** - Hypothetical fall of the top block in reactor shaft*

From the point of view of interpretation of real process inside material the ductile damage models can be classified as micro-structural material models or phenomenological material models describing ductile damage in continuum mechanics indroducing mostly extension of

the plasticity model. Micro-structural models attempt to describe the damage by natural way. The damage occurs on the basis of two different mechanisms. Initiation, growing and connection of micro-cavities dominate in area with tri-axial stress. The loaded cross section is reduced during damage process and finally leads to failure. The phenomenological models do not have actual physical meaning but they try to simulate the behaviour of a real material using empirically desinged relations. In the project called „Identification of ductile damage parameters for nuclear facilities“ the phenomenological material model was used. These models can be classified into two groups. The fist one is formed by uncoupled models where the plastic response and ductile damage are separated and the other one contains coupled models where the plastic response is modified according to the evolution of the damage. Even though coupled models have huge potencial, their complexity and calibration costs result into limited practical application. Easier calibration process is an obvious advantage of the uncoupled material model because the calibration of the plastic response and the calibration of ductile damage are separated.

2 Ductile damage model

The material model discussed in this papar is based on both classical incremental model of plastic response with isotropic hardening and phenomenological concept of damage in continuum mechanic. This model considers isotropy and uses these quantities: Von Mises stress q, stress triaxiality η and Lode parameter ξ that are defined by second and third invariant of the deviatoric stress.

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

Principal deviatoric stresses S_1 , S_2 and S_3 are principal values of the stress deviator

$$\boldsymbol{S} = \boldsymbol{\sigma} + p\boldsymbol{I}\ , \quad p = -\frac{1}{3} tr(\boldsymbol{\sigma}), \qquad 2$$

where p is hydrostatic stress. Von Mises stress is defined as

$$q = \sqrt{3J_2}. \qquad 3$$

Stress triaxiality η and Lode parameter ξ can be expressed as

$$\eta = -\frac{p}{q}\ , \qquad \xi = \frac{27}{2}\frac{J_3}{q} \qquad 4$$

The model of plastic response works with simple surface of plasticity that is based on Von Mises stress and is described as

$$q = \sigma_Y(\bar{\varepsilon}_{pl}), \qquad 5$$

where σ_Y is actual yield stress and $\bar{\varepsilon}_{pl}$ means accumulated intensity of plastic strain and the associated flow rule has only one history dependent state parameter and is described by relation

$$\bar{\varepsilon}_{pl} = \int_0^t \dot{\bar{\varepsilon}}_{pl}\, dt\,, \quad \dot{\bar{\varepsilon}}_{pl} = \sqrt{\frac{2}{3}\dot{\boldsymbol{\varepsilon}}_{\boldsymbol{pl}} : \dot{\boldsymbol{\varepsilon}}_{\boldsymbol{pl}}} \qquad 6$$

Dependence of $\sigma_Y(\bar{\varepsilon}_{pl})$ is calibrated experimentally. Failure criterion is based on phenomenological quantity damage ω that is defined as non-decreasing scalar parameter and has form

$$\omega = \int_0^t \frac{\dot{\bar{\varepsilon}}_{pl}}{\bar{\varepsilon}_f(\eta, \xi)}\, dt\,, \qquad 7$$

That depends on loading history and can be understood as linear accumulation of incremental damage in process of monotonic loading. Fracture locus $\bar{\varepsilon}_f(\eta, \xi)$ is a function of stress triaxiality and Lode parameter and it has to be calibrated experimentally. If the critical value of damage ω_{crit} is reached the ductile fracture of material occurs. The fracture locus has physical meaning of accumulated plastic strain at the instant of initiation of the ductile damage at the end of hypothetic monotonic loading with both triaxiality and Lode parameter constant. In such loading process the damage at the moment of failure reaches value ω_{crit}=1. Damage defined in equation 7 can be normalized. In this paper Johnson-Cook material model (Abaqus 6.12, 2012) was employed to describe the ductile damage

$$\bar{\varepsilon}_f(\eta, \dot{\bar{\varepsilon}}_{pl}, \hat{T}) = [d_1 + d_2 e^{-d_3 \eta}]\left[1 + d_4 ln\left(\frac{\dot{\bar{\varepsilon}}_{pl}}{\dot{\varepsilon}_0}\right)\right]\left[1 + d_5 \hat{T}\right], \qquad 8$$

where d_1, d_2, d_3, d_4 and d_5 are failure parameters, $\dot{\varepsilon}_0$ is the reference strain rate and $\hat{T}$ is dimensionless temperature. For the calibration process the quasi-static loading at reference temperature is supposed. Therefore only the first term (parameters d_1, d_2 and d_3) of Johnson-Cook model is calibrated.

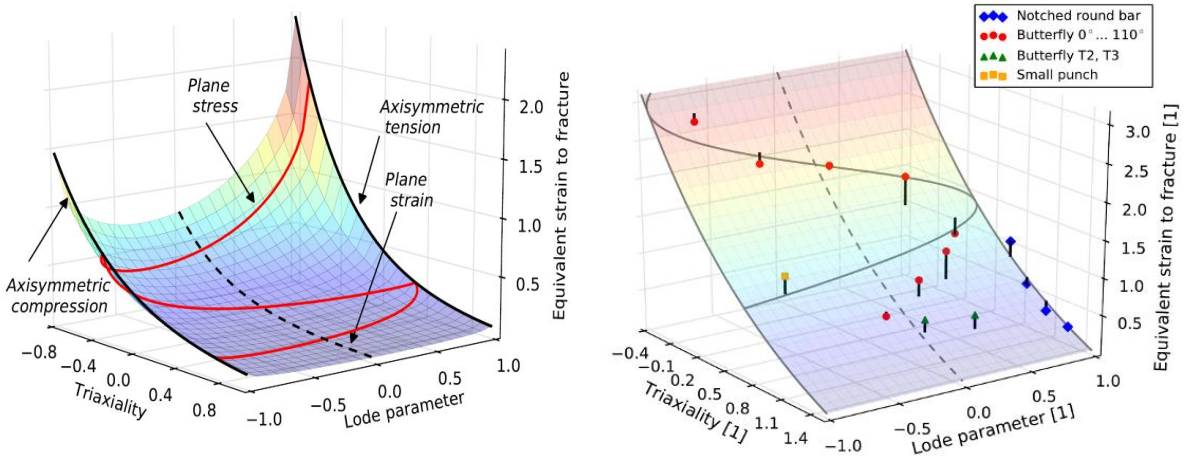

***Fig. 2** – Bai-Wierzbicki asymmetric fracture locus (left) and Johnson-Cook fracture locus (right)*

Bao and Wierzbicki (Bao 2005) determined fracture locus experimentally in wide range of stress triaxiality and showed that the dependence of fracture locus on stress triaxiality needs to be monotonic decreasing function. Xue and Wierzbicki (Wierzbicki 2005) expanded the

dependence of fracture strain on triaxiality by third invariant of the stress tensor. This invariant was included in the form of Lode parameter. Xue and Wierzbicki used ellipical function to describe the dependence of fracture locus on Lode parameter. They defined symmetric function (fracture locus) that shows the same dependence of axisymemtric tension and axisymmetric pressure. Further generalization is introduced in Bai-Wierzbicki model (Bai, 2007) that expects that the fracture locus is generally asymmetric. The fracture locus is defined in the form

$$\bar{\varepsilon}_f(\eta,\xi) = \left[\frac{1}{2}(D_1 e^{-D_2\eta} + D_5 e^{-D_6\eta}) - D_3 e^{-D_4\eta}\right]\xi^2 + \frac{1}{2}(D_1 e^{-D_2\eta} + D_5 e^{-D_6\eta})\xi + D_3 e^{-D_4\eta}, \quad 9$$

where d_1, d_2, d_3, d_4, d_5 and d_6 are failure parameters, η is triaxiality stress and ξ is Lode parameter. The failure parameters for the quasi-static loading at reference temperature have to be calibrated.

The artificial degradation function described by the parameter of degradation is implemented in the software Abaqus and is created in order to prevent stepped loss of stiffness in the whole element when the failure criterion is reached. The difference between damage and degradation process lies in its dependence on fracture strain. The degradation parameter is not counted as a material parameter so it does not enter into the calibration process. If the failure is indicated in the element of FE mesh the elastic modulus starts to decrease in form

$$E^* = (1 - D)E \quad 10$$

After the critical value $D = 1$ is reached in the element, it is removed from the FE mesh. In Abaqus there are more ways how the damage process could be controlled. In this paper the description based on Hillerborg's fracture energy was used.

$$G_f = \int q\,du_{pl} = \int Lq\,d\bar{\varepsilon}_{pl} \quad 11$$

The degradation process is described as

$$D = \int \frac{Lq}{G_f} d\bar{\varepsilon}_{pl}, \quad 12$$

where L is characteristic size of the element. In area of expected damage the mesh with the same element size should be used. Material parameter G_f must be recalculated for different mesh density. From equation 11 the fracture energy G_f can be expressed for any characteristic element length L differing from L_0 using values of G_{f0}

$$\frac{G_f}{L} = \frac{G_{f0}}{L_0} = \int q\,d\bar{\varepsilon}_{pl}\,, \quad G_f = \frac{G_{f0}}{L_0}L \quad 13$$

The dependency of degradation development on mesh density can be removed by setting $G_f \to 0$. The phase of degradation is minimized and the full degradation of element occurs immediately after the critical damage value ω_{crit} is reached. All samples simulated in this paper have the same size of element edge (0.2 mm) in expected damage initiation.

3 The method of calibration process

Several specimen types with different values of both stress triaxiality and Lode parameter at expected locations of ductile fracture should be used for successful calibration of material model. Because in most cases the course of quantities $(\eta, \xi, \bar{\varepsilon}_{pl})$ during loading process can not be described using analytic formulas, the specimens have to be analyzed via FE. The calculated course of these quantities serves as input into calibration process. Experimental determination of fracture strain $\bar{\varepsilon}_f$ is essential. Mostly the critical extension ΔL_f at the instant of failure is determined from experimental data. Fracture strain $\bar{\varepsilon}_f$ in expected location of failure is then calculated using FE simulation. The critical extension could be determined for example via direct surface observation and first individual cracks detection. This approach is limited onto specimens at which the fracture starts form surface. Usually, critical extension is identified on base of sudden decrease of loading force in force-displacement record. It is possible to use the method of digital image correlation for direct evaluation of fracture strain in case of failure on the surface.

Two approaches are commonly cited to be used in calibration process of uncoupled ductile damage model. The first one is based on averaged values of stress triaxiality and Lode parameter (Bai, 2007) etc. Averaged values of stress triaxiality η_{av}, resp. Lode parameter ξ_{av} are based on plastic strain weighted average

$$\eta_{av} = \frac{1}{\bar{\varepsilon}_f} \int_0^{\bar{\varepsilon}_f} \eta(\bar{\varepsilon}_{pl})\, d\bar{\varepsilon}_{pl}\,, \qquad \xi_{av} = \frac{1}{\bar{\varepsilon}_f} \int_0^{\bar{\varepsilon}_f} \xi(\bar{\varepsilon}_{pl})\, d\bar{\varepsilon}_{pl} \tag{14}$$

The point $(\eta_{av}, \xi_{av}, \bar{\varepsilon}_f)$ for each individual sample can be determined by this approach. Fracture locus $\bar{\varepsilon}_f(\eta, \xi)$ passes through this point. The main disadvantage of this approach is wide range of stress triaxiality and Lode parameter ξ for some specimen types resulting into non-negligible error caused by averaging of these quantities. The point of minimum of target functional F_{av}

$$F_{av} = \frac{1}{N} \sum_{i=1}^{N} \left| \bar{\varepsilon}_{f_i} - \bar{\varepsilon}_f(\eta_{av_i}, \xi_{av_i}) \right|^m \tag{15}$$

is searched employing suitable optimization tools. This functional, at which N means total number of calibrated specimens, m expresses the rate of weighting of individual deviations, expresses total error of $\bar{\varepsilon}_f(\eta, \xi)$ in points $(\eta_{av_i}, \xi_{av_i}, \bar{\varepsilon}_{f_i})$ corresponding with i-th specimen. More balanced deviation can be expected with growing value of m ($m = 2$ corresponds with least squares method). Bai and Wierzbicki use modified target function F_{av}^* in their work (Bai, 2007). Individual deviations are weighted by fracture strain.

$$F_{av}^* = \frac{1}{N} \sum_{i=1}^{N} \frac{1}{\bar{\varepsilon}_{f_i}} \left| \bar{\varepsilon}_{f_i} - \bar{\varepsilon}_f(\eta_{av_i}, \xi_{av_i}) \right| \tag{16}$$

Optional application of least squares enabling employment of linear regression is quality of this approach. Some material models can be modified so that the linear regression can be used partially.

The second approach defines target as deviation of damage ω_i integrated up to fracture strain ω_{crit} =1 for i-th specimen averaged over all specimens.

$$F_\omega = \frac{1}{N}\sum_{i=1}^{N}|1-\omega_i|^m, \omega_i = \int_0^{\bar{\varepsilon}_{f_i}} \frac{d\bar{\varepsilon}_{pl_i}}{\varepsilon_f(\eta_i,\xi_i)} \quad 17$$

This approach eliminates averaging in Equation 13 however calibration costs are higher in comparison with the first approach and moreover existence of global minimum uncertainty is higher in this case. For this reasons fine tuning of parameters that was found using averaging quantities is preferred application. This approach was used for example in (Vaziri, 2010).

4 Effect of temperature in ductile damage models

4.1 Method of plasticity calibration process for different temperatures

In this case the proportionality of the true stress - logarithmic strain dependence within the analyzed materials could be used with regard to the force response of individual specimens. If the measured force – extension dependence for fixed temperature is possible to approximate successfully by force – extension dependence for the same specimen for reference temperature in form

$$F(\Delta l)_T \approx \tau(T).F(\Delta l)_{T_0}, \quad 18$$

where $\tau(T)$ is convenient correct function that depends only on temperature, the correct plastic curve can be calculated as

$$\sigma_{y\,crit}^{True}\left(\varepsilon_{ln}^{pl}\right)_T = \tau(T).\sigma_{y\,crit}^{True}\left(\varepsilon_{ln}^{pl}\right)_{T_0} \quad 19$$

Correct temperature function can be designed for example in form that is contained in Johnson-Cook plastic model.

$$\tau(T) = 1-\left(\frac{T-T_0}{T_{melt}-T_0}\right)^m, \quad 20$$

where T_0 is the temperature of the reference specimen that the plastic part of tensile curve was identified for, T_{melt} is melting temperature of chosen material and m is material parameter that describes temperature softening. If the range of experimental measured data for different temperature is large, melting temperature T_{melt} can be understood as the next material parameter that lost direct physical meaning. Own calibration process is possible to divide into two steps. At first the value of correct function for individual temperatures has to be determined. For this case the low square method is considered to be a suitable tool. If this method is applied on this problem, the value of correct function can be calculated by form:

$$\tau(T_j) = \frac{\int_0^{\Delta l} F(\Delta l)_{T_j} d\Delta l}{\int_0^{\Delta l} F(\Delta l)_{T_0} d\Delta l} \quad 21$$

The second step is finding the parameters m a T_{melt}. This parameters could not be expressed explicitly therefore they have to be calculated numerically. The problem can be transformed into optimized solution and the parameters are searched in a way to minimize the target function.

$$F(m, T_{melt}) = \sum_j \left|\tau_j - \tau(T_j)\right| \qquad 22$$

In case the experimental dependence cannot be acceptably adjusted by suitable function in form

$$F(u)_T \approx \tau(T).F(u)_{T_0}, \qquad 23$$

it is necessary to calibrate true stress – logarithmic strain dependence for each temperature separately.

4.2 Method of ductile damage calibration process for different temperatures

Temperature dependence proceeds from calibrated ductile damage model without strain rate dependence and temperature dependence (the first bracket of the form 8 could serve as an example). For example the Johnson-Cook model can be used where D_5 is the searched parameter. For determination of the material parameter D_5 partially describing the dependance of ductile damage on temperature the same process used in the case of finding material parameters for description of plasticity could be employed. The content of brackets for the temperature loading in form $\left(1 + D_5 \frac{T-T_0}{T_{melt}-T_0}\right)$ is set for a concrete specimen and for defined temperature constant if the specimen with reference temperature T_0 is known. This parameter $\tau(T)$ scales fracture strain acquired for quasi-static loading, the form for new fracture strain than is

$$\bar{\varepsilon}_f^{pl} = (D_1 + D_2 e^{D_3\eta})(1 + D_5 T^*) = \bar{\varepsilon}_{fqv}^{pl}\left(1 + D_5\left(\frac{T - T_0}{T_{melt} - T_0}\right)^m\right) = \bar{\varepsilon}_{fqv}^{pl}.\tau(T) \qquad 24$$

Parameter $\tau(T)$ can be expressed as a ratio of limit displacements for the ductile damage occuring during quasi-static loading u_{fqv} and for loading at different temperature with displacement u_f. The form for this is

$$\tau(T) = \left(1 + D_5\left(\frac{T - T_0}{T_{melt} - T_0}\right)^m\right) = \frac{\bar{\varepsilon}_f^{pl}}{\bar{\varepsilon}_{fqv}^{pl}} = \frac{u_f}{u_{fqv}} \qquad 25$$

The last step is mathematical description for calculation of parameter D_5. This parameter is found using linear regres and following pseudoinversion.

$$\tau(T) = \left(1 + D_5\left(\frac{T - T_0}{T_{melt} - T_0}\right)^m\right) \quad \rightarrow \quad Z = 1 + D_4 T^* \qquad 26$$

$$D_5 = ((T^*)^T.T^*)^{-1}.(T^*)^T.(\tau(T) - 1) \qquad 27$$

In the Fig. 3 the reference fracture and scaled fracture locus are shown for ductile damage model of Bai-Wierzbicki.

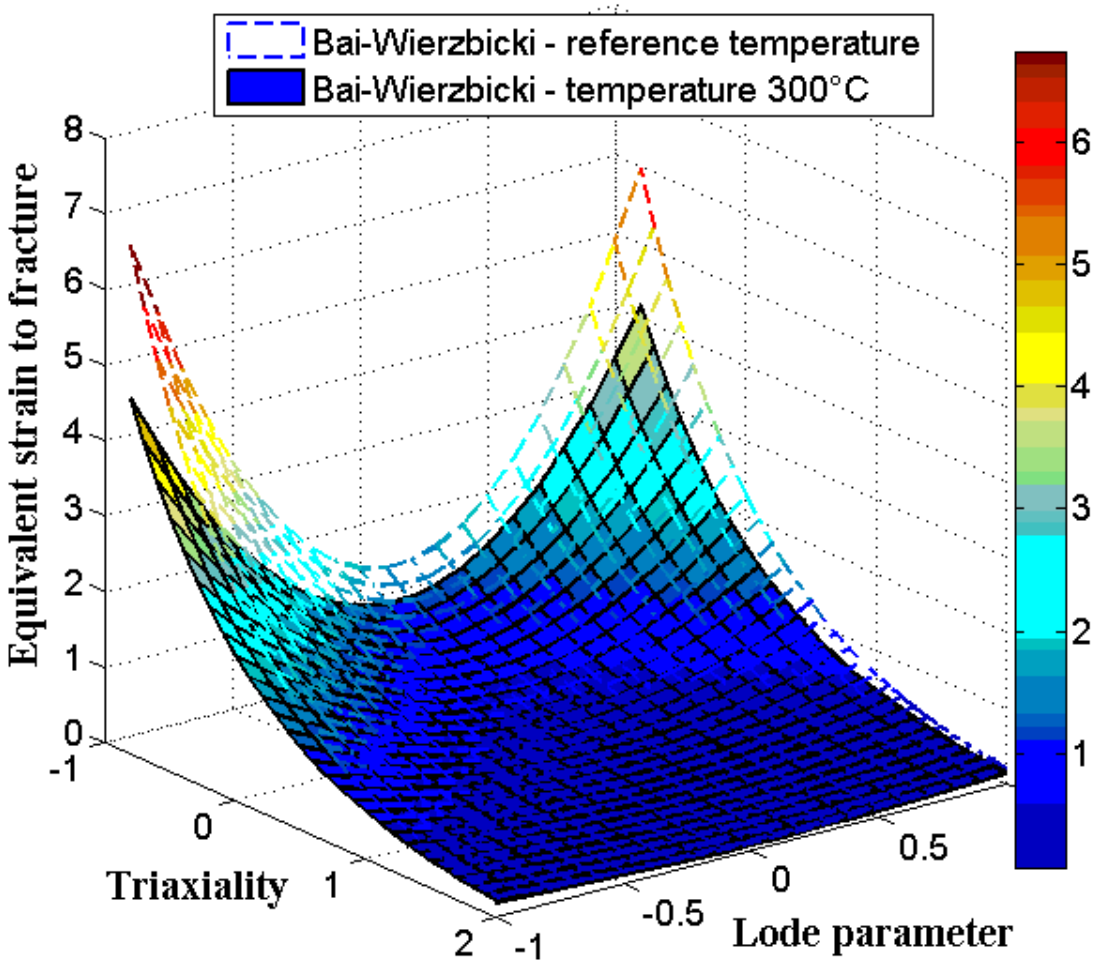

***Fig. 3** – Bai-Wierzbicki asymmetric fracture locus for reference temperature and temperature 300°C*

5 Specimens for calibration of fracture locus at reference temperature

Material calibration process of ductile damage models is based on the portfolio of fifteen calibration specimens. These specimens for quasi-static loading were designed to describe different stress in every single specimen and to calibrate fracture locus at reference temperature. The whole portfolio in dimension of stress triaxiality and Lode parameter is shown in Fig. 4. It is based on averaged values of η_{av} and ξ_{av} at instant of expected fracture.

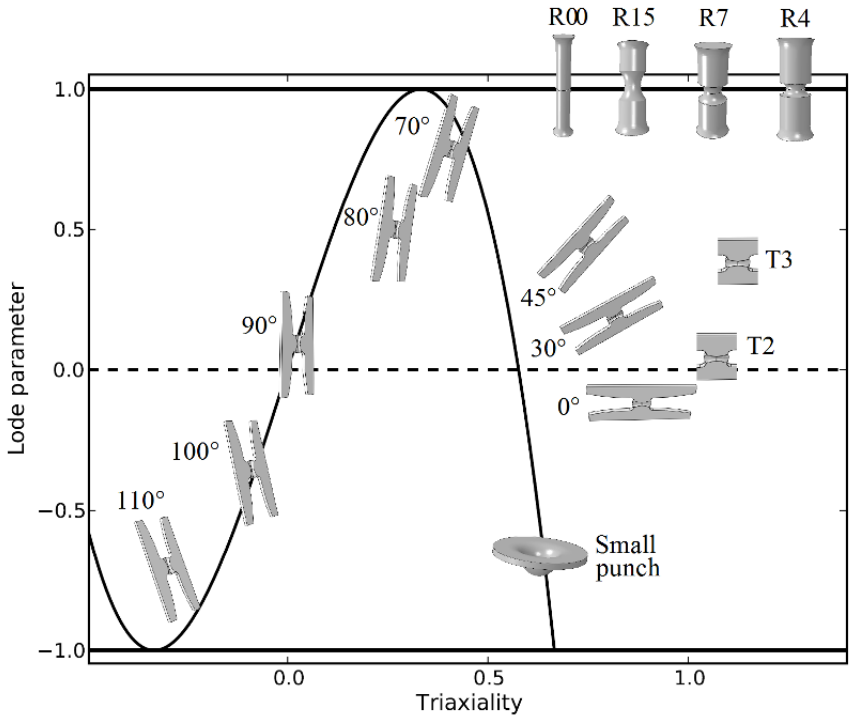

Fig. 4 *– The portfolio of calibration specimens*

Because it is technically very difficult to measure the whole portfolio of specimens in different temperature (some specimens need a special device for measuring), only round bars with different size of notches $(R_{\infty}, R1, R2, R4, R7, R15)$ were experimentaly measured for different temperatures. The specimen damage develops in conditions of constant value of Lode parameter $\xi = 1$. Each of these specimens was designed so that the fracture occurred first in the centre of the specimen. The bilinear axisymmetric elements CAX4 with uniform size 0.2 mm in damage area was used. All used specimens are shown in Fig. 5.

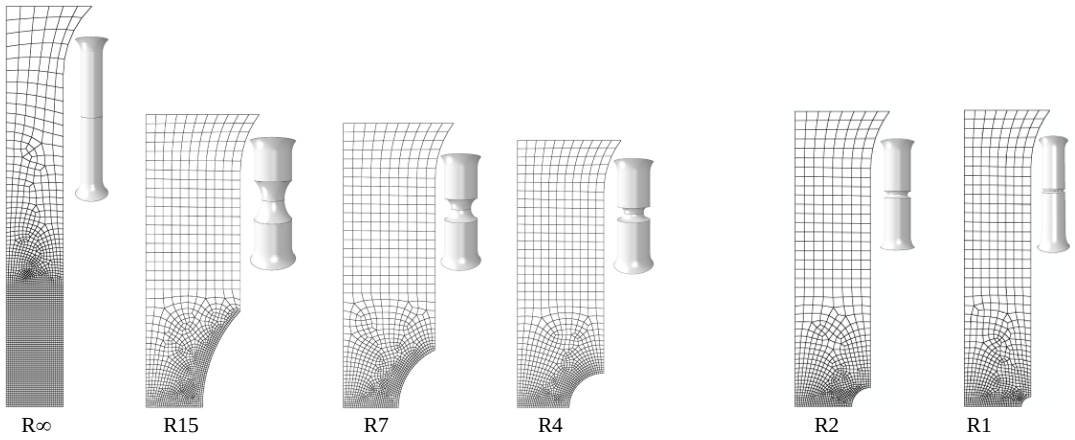

Fig. 5 *– Notched round bar specimens*

6 Results

FE simulation of each specimen was compared with the experimental data as shown in the figures below. Each figure is determined for four round bar specimens $(R_{\infty}, R1, R2, R4)$ at the same temperare.

6.1 Scaled plastic curve – Material 1

Plastic region of reference temperature was scaled for particular temperatures by parameter. In numerical simulations the plastic curve is described by a table and using only one parameter is sufficient for this sort of material, therefore it is not necessary to calibrate plastic curve for different temperature, while this method remains sufficiently accurate. The results of the first material for plastic region are shown in Fig. 6.

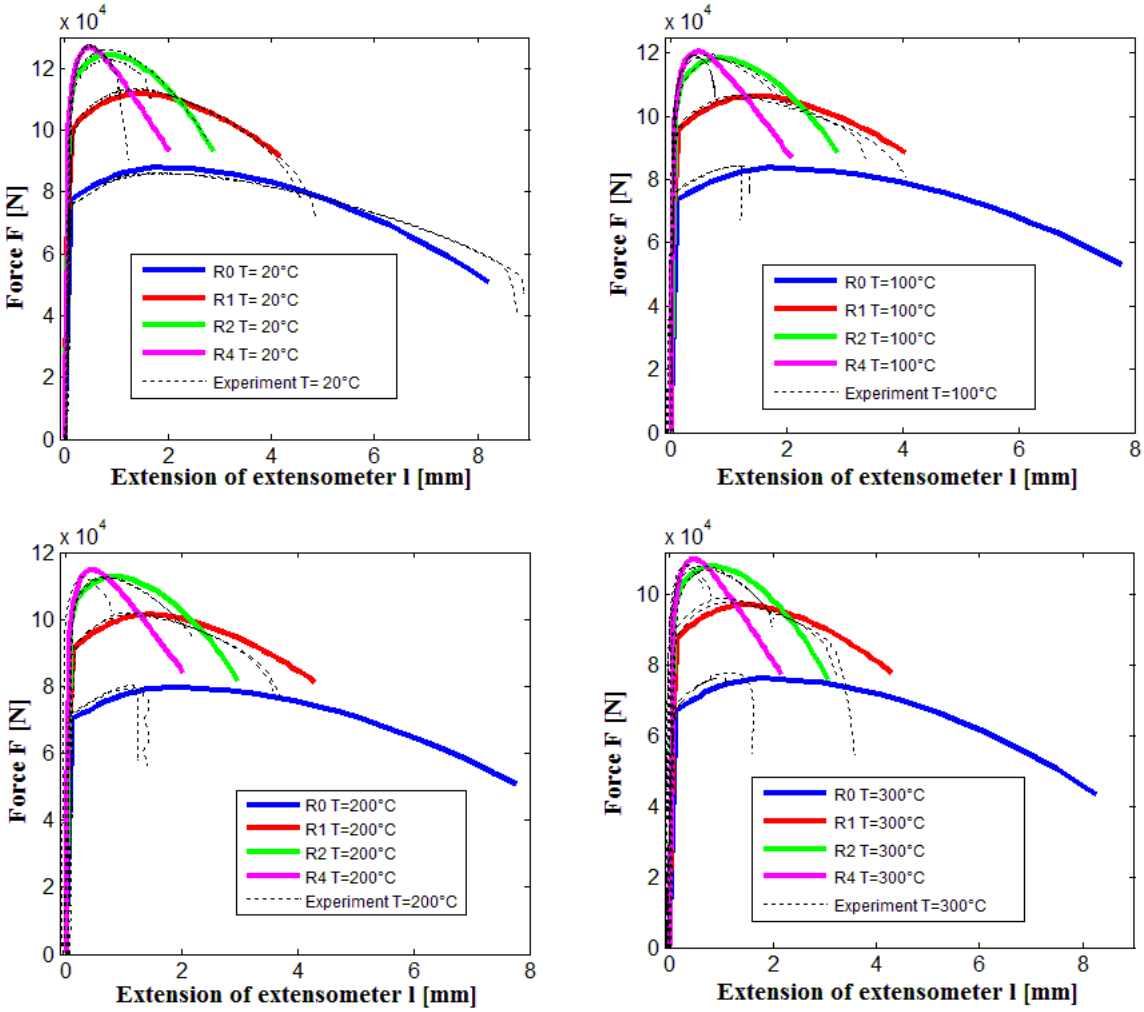

Fig. 6 *– Results of scaled plastic curve – Force and extension dependence for round bar specimen without notch and with notch R1 mm, R2 mm a R4 mm za různých teplot*

6.2 Scaled plastic curve – Material 2

Plastic region of reference temperature was scaled for particular temperatures by parameter. In numerical simulations the plastic curve is described by a table. The figure shows that parameter of scaling is not sufficiently accurate for this sort of material and it would be necessary to calibrate the plastic curve for each temperature. The results of scaled plastic responce are shown in

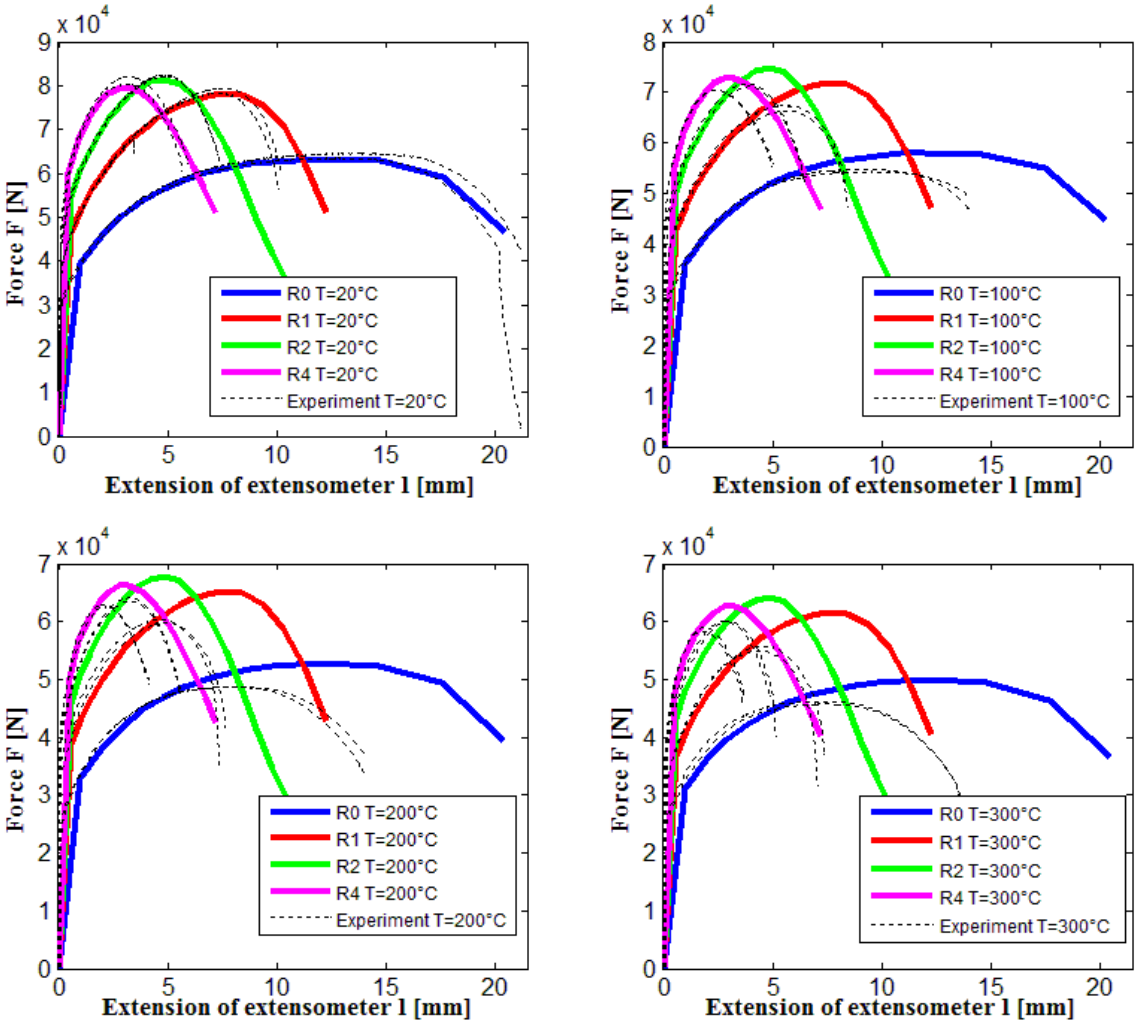

Fig. 7 *– Results of scaled plastic curve –Force and extension dependence for round bar specimen without notch and with notch R1 mm, R2 mm a R4 mm za různých teplot*

Because these results are not accurate for plastic response, each plastic curve has to be calibrated separately for individual temperature values and only after that the scaled ductile damage model of Bai-Wierzbicki could be used.

6.3 Scaled ductile damage model – Material 1

Ductile damage model is based on calibrated Johnson-Cook model for reference temperature 20°C. This model was scaled for particular temperatures by constant that has to be optimized and is based on Johnson-Cook.

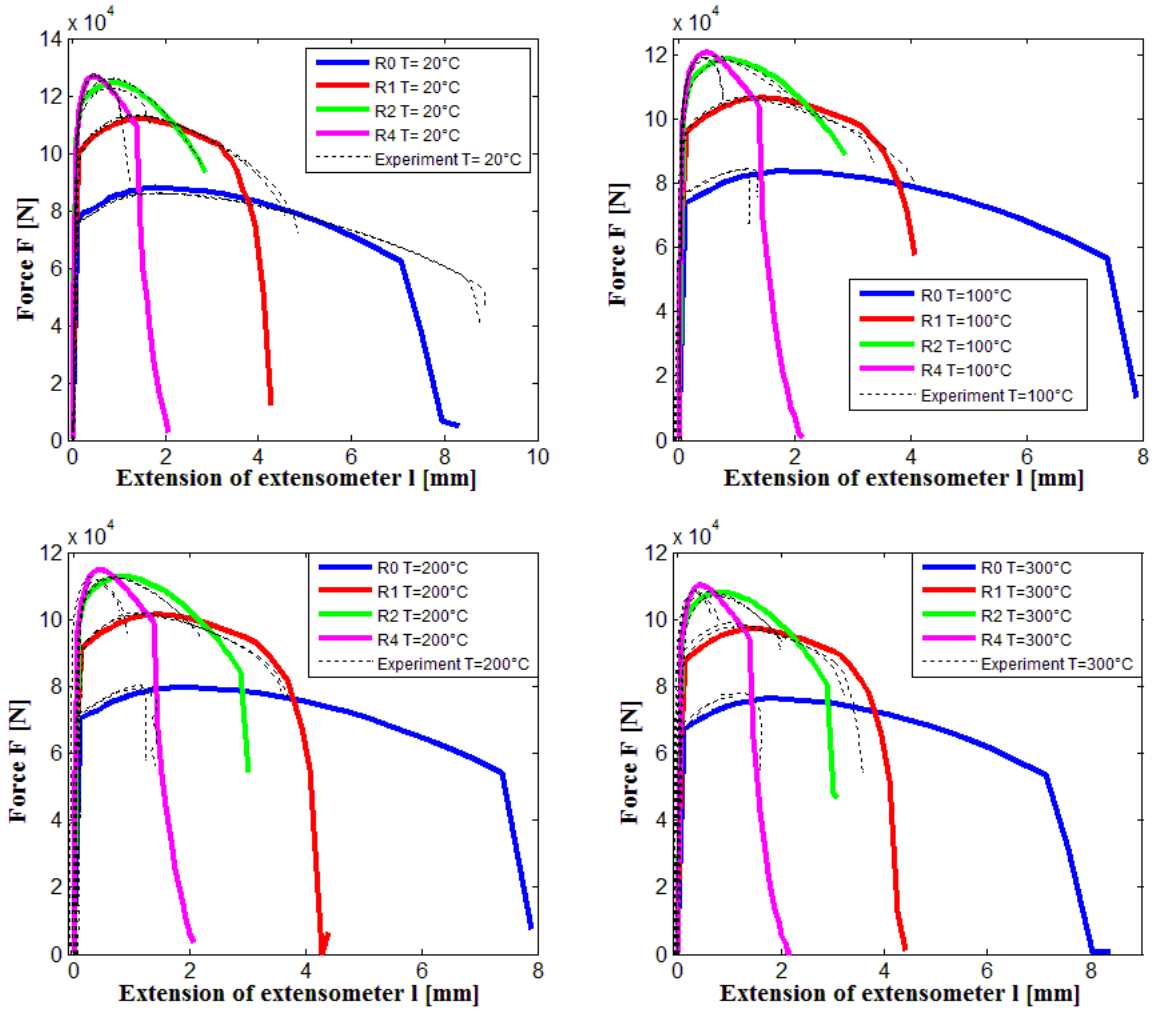

Fig. 8 *– Results of scaled ductile damage model – Force and extension dependence for round bar specimen without notch and with notch R1 mm, R2 mm a R4 mm for different temperature*

The results show that scaled fracture locus is sufficiently accurate for this type of material. Large range difference between numerical simulation and experiment is for smooth round bar specimen. The reason of this difference lies in experimental measuring. The smooth round bar specimen was measured with different method in reference temperature than in other temperatures. The problem of measuring was caused by friction in jaws of tearing machine. This problem was solved but new experimental data are yet to be measured for this specimen.

7 Conclusion

In this paper the calibration of uncoupled phenomenological ductile damage model in FE software Abaqus was discussed. The Johnson-Cook and Bai-Wierzbicki material models were used for description of the ductile fracture and were scaled for different temperatures. Fracture strain was calculated on base of the specimen extension at material failure and portfolio of quasi-static calibrating test was designed. Both material models successfully describe ductile damage of calibration specimens that are often presented in literature. The method of scaling fracture locus for different temperature is succesful and for plastic response depends on the type of material. The ductile damage models are sufficiently accurate and can be used in technical practise because of their easy description and searching only for one parameter.

List of symbols

J_2	Second invariant of deviatoric stress	[MPa^2]
J_3	Third invariant of deviatoric stress	[MPa^3]
S	Principal deviatoric stress	[MPa]
p	Hydrostatic stress	[MPa]
q	Von Mises stress	[MPa]
η	Triaxiality stress	[MPa]
η_{av}	Avaraged triaxiality stress	[MPa]
ξ	Lode parameter	[MPa]
ξ_{av}	Avaraged Lode parameter	[MPa]
$\bar{\varepsilon}_{pl}$	Accumulated intensity od plastic strain	[-]
$\bar{\varepsilon}_f$	Fracture strain	[-]
E	Elastic modulus	[MPa]
E^*	Decrease of elastic modulus	[MPa]
G_f	Hillerborg´s fracture energy	[J]
L	Characteristic size of element	[mm]
F	Minimum of target function	[-]
T_0	Reference temperature	[K]
T_{melt}	Melting temperature	[K]
T	Temperature	[K]

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