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MULTI-SURFACE DESCRIPTION OF TEMPERATURE AND STRAIN RATE-DEPENDENT DAMAGE INITIATION AND GROWTH IN DUCTILE VISCOPLASTIC MATERIALS

PATRICE LONGERE[1], ANNE-GAELLE GEFFROY[2], BRUNO LEBLE[2] AND ANDRE DRAGON[3]

[1] Université de Toulouse
Institut Supérieur de l'Aéronautique et de l'Espace - Institut Clément Ader (EA 814)
10 avenue E. Belin, BP 54032, 31055 Toulouse cedex 4, France
e-mail: patrice.longere@isae.fr

[2] DCNS NA Ingénierie
Rue de Choiseul, 56311 Lorient, France
email: anne-gaelle.geffroy@dcnsgroup.com, bruno.leble@dcnsgroup.com

[3] CNRS-Institut Pprime (UPR 3346)
Ecole Nationale Supérieure de Mécanique et d'Aérotechnique - Université de Poitiers
1 avenue C. Ader, BP 40109, 86961 Futuroscope - Chasseneuil du Poitou, France
e-mail: andre.dragon@ensma.fr

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Abstract. A phenomenological modelling approach has been developed, based on some salient physical effects regarding void growth vs. plastic straining, to describe the transition behaviour between dense metal plasticity and micro-porous metal plasticity. Considering that void germination requires a certain amount of plastic deformation, a 'primary' hole nucleation criterion has been proposed, as well as a statistical law governing the 'secondary' hole kinetics. In a consistent way, the hole nucleation criterion accounts for the accelerating effects of stress triaxiality and, conversely, the delaying effects of temperature and strain rate. In this work, a modification of the GTN model has also been proposed, overcoming its inability to predict damage growth and fracture for zero and low triaxiality, shear-dominated deformations. In this respect the kinematic mean stress related shift mechanism has been introduced and quantified in the expression of the GTN plastic potential, enabling thus the damage growth under shear and under small negative triaxialities. The 3D constitutive equations have been implemented as user material in the engineering finite element computation code Abaqus®. Numerical simulations have been conducted considering a single finite element under simple shear on one hand and a notched cylindrical sample under remote uniaxial tensile loading on the other hand. The numerical results show clearly the influence of the hole nucleation criterion related constants on the damage and further failure of the material.

1 INTRODUCTION

Several authors have attempted to describe the consequences of micro-voiding induced damage on the bulk material behaviour. These consequences are double: a progressive loss of the overall properties of the bulk material and the appearance, in addition to the isochoric plastic deformation due to dislocation glide in the matrix material, of an inelastic dilatancy due to void growth. In order to describe this second effect in the context of standard material, BERG [1] proposed a pressure dependent plastic potential, assuming that a critical mean triaxial stress (hydrostatic stress) is required to activate the void expansion. In this approach, only a loading path involving a mean triaxial stress greater than this critical mean triaxial stress produces void growth and related dilatancy. It is noteworthy that the latter may be accompanied by plastic deformation (under moderate mean triaxial stress) or not (under high mean triaxial stress). Based on a micromechanical analysis, GURSON [2] developed a plastic potential for slightly porous and perfectly plastic metals accounting explicitly for the concentration of voids and hydrostatic stress. TVERGAARD AND NEEDLEMAN [3] modified GURSON's model in order to take notably into account isotropic strain hardening and strain rate effects. The so-called GTN (for GURSON-TVERGAARD-NEEDLEMAN) model is widely used by the community of researchers dealing with ductile damage. Using the rate type formulation coupled with physical concepts, PERZYNA [4] developed an elliptic plastic potential taking into consideration cooperative effects of void growth, strain rate sensitivity and heating.

These approaches all suppose initially the presence of micro voids, or equivalently assume mostly that void expansion starting and plastic deformation occurrence are concomitant. It is clear that such a hypothesis is not supported physically, because cavity nucleation requires a certain amount of plastic deformation. Furthermore, by construction, the plastic potentials proposed by BERG, GURSON-TVERGAARD-NEEDLEMAN and PERZYNA are not able to describe dilatancy and related cavity growth under shear loading, implying that according to their approaches shear loading cannot lead sole to fracture.

This work aims at facing up to these deficiencies concerning notably the lack of a physically satisfying description of the transition from dense metal plasticity to micro-porous metal plasticity and the incapacity of describing void growth under shear.

The principle of the modelling approach involving non-concomitant damage incipience with respect to plastic straining is described in Sect.2. The constitutive equations for an elastic-viscoplastic material undergoing the combined effects of the two stage damage formation (void nucleation) mechanism, the mean stress kinematic shift related to ductile damage growth, isotropic hardening, thermal softening, are detailed in Sect.3. The complete model has been implemented as user material in the engineering finite element computation code Abaqus® and some numerical simulations have been conducted for a single representative volume element (RVE) and a laboratory sample submitted to a remote uniaxial tensile loading. The numerical results are shown in Sect.4.

2 BASIC CONCEPTS AND PRELIMINARY CONSIDERATIONS

2.1 Principle of the sound/damaged or dense/micro-porous behavior transition

In the present approach, based in part on the concepts suggested by DRAGON AND OHJI [5], the metallic material is initially supposed to be exempt of micro-voids. Subjected to a monotonic loading involving a positive or null stress triaxiality, it behaves elastic-(visco)plastically. As soon as the condition for the germination of a given volume fraction f_0 of micro voids, involving the equivalent plastic strain κ, plastic strain rate $\dot{\kappa}$, temperature T and stress triaxiality ST, is satisfied, the material behaviour becomes pressure dependent. Consecutive damage accompanying plastic yielding may be described then using e.g. GTN and PERZYNA micro-porous plasticity oriented models. The quantity f_0 represents thus a characteristic micro-porosity initiation bunch; its occurrence does not exclude further, secondary, delayed nucleation (see Sect.3.3). According to Fig.1, where the surfaces $\Phi_0 = 0$ and $\Phi_{I_0} = 0$ represent the limits of elastic domain of the sound material and of the hole non-nucleation domain respectively, this approach is able to reproduce qualitatively the accelerating effects of the stress triaxiality $\left(ST = -\sigma_{eq} / p_m\right)$ on the hole germination.

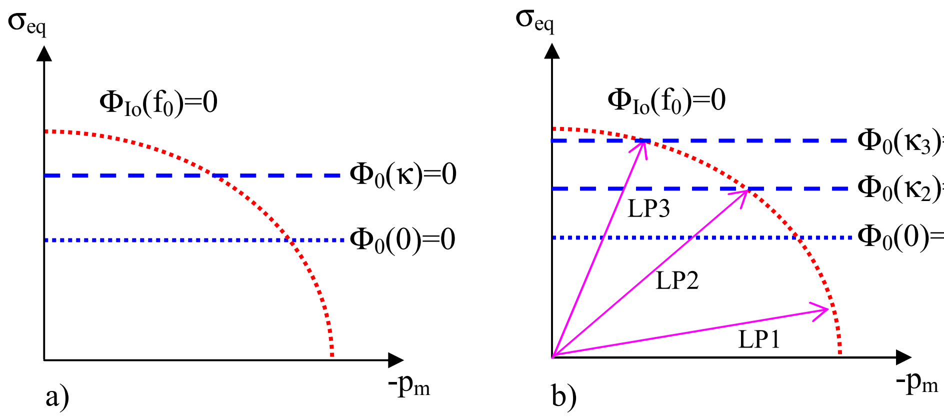


Figure 1: Principle of the approach. a) Initial and current elastic domains for the sound material and hole nucleation locus – b) Illustration of various loading paths; LP1: hole germination without plastic deformation; LP2: hole germination for a finite amount of plastic deformation (κ_2) linked to the current stress triaxiality (ST_2); LP3: hole germination for a finite amount of plastic deformation ($\kappa_3 > \kappa_2$) linked to the current stress triaxiality ($ST_3 < ST_2$) - σ_{eq} and p_m represent the equivalent stress and the pressure, respectively.

2.2 The hypothesis of a kinematical mean stress shift

It is known that viscoplastic deformation may cause brittle-like micro-damage in metals, as observed notably in creep where micro-voids and micro-cracks initiate along the grain boundaries. At an advanced stage of deformation, a material may thus contain defects potentially at the origin of brittle fracture (in the sense mentioned above) and defects potentially at the origin of ductile fracture (in the current sense). In such a material, there are thus two sources of damage induced softening, the latter being described via e.g. GTN and PERZYNA micro-porous plasticity oriented models. We are here describing the consequences of the former by introducing an effective micro porosity related softening mechanism, acting as a kinematic-like mean stress drop resulting in a shift of the yield locus centre towards negative stress triaxiality values. Though a like shift may require more sophisticated analysis involving e.g. damage/plasticity-induced anisotropy , here, however, the purpose is limited to a modification of the GTN model. From a practical viewpoint, let denote as $\Phi(\sigma_{eq}, p_m; f, ...) = 0$ the yield surface of a material with a micro-void concentration f. The translation of the yield function Φ of the amount $-p_r$ under the micro-crack induced softening effects leads to consider the new plastic potential and corresponding yield locus $\Phi_G(\sigma_{eq}, p_m, p_r; f, ...) = 0$, such that $\Phi_G(\sigma_{eq}, p_m, p_r; f, ...) = \Phi(\sigma_{eq}, p_m + p_r; f, ...)$. The principle is illustrated in Fig.2.

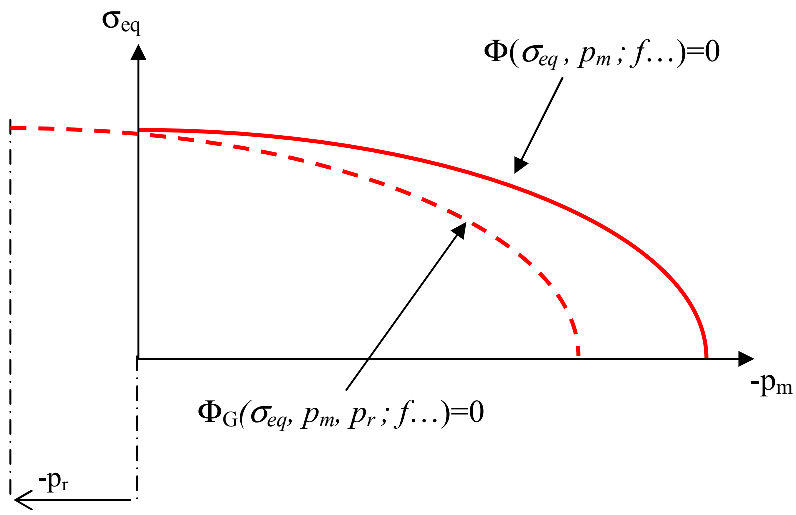


Figure 2: Shift of the micro-porous potential with kinematic mean stress

This paper aims at proposing a multi-surface approach based model accounting for the aforementioned effects.

3 CONSTITUTIVE EQUATIONS

The elastic/viscoplastic model involving two stage void nucleation mechanism and void growth related hardening/softening effects is detailed in the present section.

3.1 Constitutive equations of the sound material

The internal variable procedure has been followed to model the material behaviour. The instantaneous state of the material is described via the HELMHOLTZ free energy $\Psi(T; \underline{\underline{\varepsilon}}^e, \kappa)$, whose arguments are the absolute temperature T, the elastic strain tensor $\underline{\underline{\varepsilon}}^e$, and the isotropic strain hardening variable κ. Let us consider the following additive decomposition of Ψ :

$$\Psi(T; \underline{\underline{\varepsilon}}^e, \kappa) = \Psi_r(T; \underline{\underline{\varepsilon}}^e) + \Psi_T(T) + \Psi_s(T; \kappa) \quad (1)$$

where $\Psi_r\left(T;\underline{\underline{\varepsilon}}^e\right)$ is the recoverable part, $\Psi_T(T)$ the purely thermal part, and $\Psi_s(T;\kappa)$ the stored part. The rotational derivatives considered in the following are GREEN-NAGHDI derivatives, see GREEN AND NAGHDI [6]. Moreover, the tensor $\underline{\underline{\varepsilon}}^e$ represents here a spatial, generally small, elastic strain measure, namely $\underline{\underline{\varepsilon}}^e = \ln \underline{\underline{V}}^e$, $\underline{\underline{V}}^e$ representing pure elastic stretching resulting from the relevant multiplicative decomposition of the deformation gradient $\underline{\underline{F}}$ .The expressions of the various contributions in (1) are given by

$$\begin{cases} \rho\Psi_r\left(T;\underline{\underline{\varepsilon}}^e\right) = \dfrac{\lambda}{2}\left(Tr\underline{\underline{\varepsilon}}^e\right)^2 + \mu\underline{\underline{\varepsilon}}^e : \underline{\underline{\varepsilon}}^e - \alpha_T K Tr\underline{\underline{\varepsilon}}^e \Delta T \\ \rho\Psi_T(T) = -\dfrac{\rho C}{2T_0}\Delta T^2 \ ; \ \Delta T = T - T_0 \\ \rho\Psi_s(T;\kappa) = h(\kappa)g(T) \end{cases} \tag{2}$$

where λ and μ represent LAMÉ elastic coefficients, K is the bulk modulus $(K = \lambda + 2\mu/3)$. The quantities α_T, ρ and C represent the thermal dilatation coefficient, the mass density and the specific heat, respectively. In (2_3), $h(\kappa)$ represents the stored energy of cold work and $g(T)$ the thermal softening function. The set of thermodynamic forces connected to the state variables is given by

$$\begin{cases} \underline{\underline{\sigma}} = \rho\dfrac{\partial\Psi}{\partial\underline{\underline{\varepsilon}}^e} = \rho\dfrac{\partial\Psi_r}{\partial\underline{\underline{\varepsilon}}^e} = \left(\lambda Tr\underline{\underline{\varepsilon}}^e - \alpha_T K\Delta T\right)\underline{\underline{\delta}} + 2\mu\underline{\underline{\varepsilon}}^e \\ \rho s = -\rho\dfrac{\partial\Psi}{\partial T} = -\left(\rho\dfrac{\partial\Psi_r}{\partial T} + \rho\dfrac{\partial\Psi_T}{\partial T} + \rho\dfrac{\partial\Psi_s}{\partial T}\right) = \alpha_T K Tr\underline{\underline{\varepsilon}}^e + \dfrac{\rho C}{T_0}\Delta T - h(\kappa)g'(T) \\ r = \rho\dfrac{\partial\Psi}{\partial\kappa} = \rho\dfrac{\partial\Psi_s}{\partial\kappa} = h'(\kappa)g(T) \end{cases} \tag{3}$$

where $\underline{\underline{\sigma}}$ represent the CAUCHY stress tensor, s entropy and r isotropic hardening force. The viscoplastic yielding of the material under consideration is supposed to be well described by the plastic potential

$$\Phi_0 = \tilde{\sigma}_{eq}^2 - 1 = 0 \ ; \ \tilde{\sigma}_{eq} = \frac{\sigma_{eq}}{\overline{\sigma}_y} \ ; \ \overline{\sigma}_y = \sigma_y + \sigma_{vp} \tag{4}$$

where the quantities σ_y and σ_{vp} in (4_3) are the rate independent and rate dependent contributions to the yield stress $\overline{\sigma}_y$. The rate independent contribution σ_y in (4_3) incorporates the combined effects of strain hardening, via a VOCE type law, and thermal softening, via a power law:

$$\sigma_y = [R_0 + h'(\kappa)]g(T);\; h'(\kappa) = R_\infty [1-\exp(-k\kappa)]^\beta \;;\; g(T) = 1 - \left(\frac{T}{T_{melt}}\right)^m \tag{5}$$

where $(R_0, R_\infty, k, \beta)$ are isotropic hardening related constants and (T_{melt}, m) thermal softening related constants, with T_{melt} the melting point. With (5), the rate independent contribution σ_y in (4_3) and the isotropic hardening force r in (3_3) take thus the form

$$\sigma_y = \left\{R_0 + R_\infty [1-\exp(-k\kappa)]^\beta\right\}\left[1 - \left(\frac{T}{T_{melt}}\right)^m\right] \;;\; r = R_\infty [1-\exp(-k\kappa)]^\beta \left[1 - \left(\frac{T}{T_{melt}}\right)^m\right] \tag{6}$$

The tensile/compressive asymmetry in the plastic behaviour is here considered as a thermally activated mechanism involving the mean stress. The strain rate induced overstress σ_{vp} in (4_3) is consequently expressed by

$$\sigma_{vp} = Y\left[\dot{\kappa}\exp\left(\frac{V_a p_m}{k_B T}\right)\right]^{1/n} \tag{7}$$

where (Y,n) are viscosity related constants and (V_a, k_B) behaviour asymmetry related constants, with $V_a = V_h \beta^3$, V_h being a constant, β Burgers vector magnitude (β=2.5Å), k_B BOLTZMANN constant (k_B=1.3804.10^{-23}J/K). The sound material satisfies the conditions of standard materials in the irreversible thermodynamics sense. Applying the normality rule yields

$$\underline{\underline{d}}^p = \Lambda\frac{\partial\Phi_0}{\partial\underline{\underline{\sigma}}} = \dot{\varepsilon}_0^{pD}\underline{\underline{n}} \;;\; \dot{\varepsilon}_0^{pD} = \Lambda\frac{\partial\Phi_0}{\partial\sigma_{eq}} = 2\Lambda\frac{\tilde{\sigma}_{éq}}{\overline{\sigma}_y} \;;\; \dot{\kappa} = \sqrt{\frac{2}{3}\underline{\underline{d}}^p : \underline{\underline{d}}^p} = \dot{\varepsilon}_0^{pD} \;;\; \Lambda \geq 0 \tag{8}$$

where Λ represents the viscoplastic multiplier. Finally, heating during any adiabatic processes is supposed to proceed predominantly from dissipation, see LONGERE AND DRAGON [7] for further details, yielding

$$\rho C\dot{T} = \underline{\underline{\sigma}} : \underline{\underline{d}}^p - r\dot{\kappa} = (\sigma_{eq} - r)\dot{\kappa} \geq 0 \tag{9}$$

3.2 Constitutive equations of the damaged material

Considering slightly porous metals, we are assuming a weak damage-plasticity state coupling and strong damage-plasticity kinetic couplings, allowing us for assuming that the state potential (1)-(2) and the forces (3) still hold during the damage process considered herein. We are indeed focusing our attention on the damage-plasticity coupling intervening at the level of the yield condition, as it is mostly done when modelling micro-porous metal behaviour, see e.g. [2] – it must be noted that accounting for damage-plasticity state coupling does not imply significant changes in the present methodology. As an application of the hypothesis of a kinematical mean stress shift, see Sect.2.2, a modified version of the GTN model is proposed. Consider thus the following modified GTN potential:

$$\Phi_G = \tilde{\sigma}_{\acute{e}q}{}^2 + 2q_1 f \cosh\left[-\frac{3}{2}q_2(\tilde{p}_m + \tilde{p}_r)\right] - (1 + q_3 f^2) = 0 \ ; \ \tilde{p}_m = \frac{p_m}{\overline{\sigma}_y} \ ; \ \tilde{p}_r = \frac{p_r}{\overline{\sigma}_y} \tag{10}$$

where (q_1, q_2, q_3) are material constants. Let define now the 'cleavage strength' σ_{cleav} as being the critical value of the mean stress $\sigma_m = -p_m$ at the incipience of void growth under equi-triaxial stress. For the GTN model $(p_r = 0)$, the so-defined 'cleavage strength' σ_{cleav} is expressed by

$$\frac{\sigma_{cleav}}{\overline{\sigma}_y} = \frac{2}{3}\frac{1}{q_2} a\cosh\left[\frac{1 + q_3 f^2}{2q_1 f}\right] \tag{11}$$

Considering low values of f $(f << 1)$ yields

$$\sigma_{cleav} \approx -\frac{2}{3}\frac{1}{q_2}\overline{\sigma}_y \ln[q_1 f] \geq 0 \tag{12}$$

In a first approximation, we are considering the kinematic pressure p_r in a close form:

$$p_r = b\ln[q_1 f] \leq 0 \tag{13}$$

with b assumed as being a positive constant. After [1], the normality rule applies to the damaged material:

$$\underline{\underline{d}}^p = \Lambda\frac{\partial\Phi_G}{\partial\underline{\underline{\sigma}}} = \Lambda\left(\frac{\partial\Phi_G}{\partial\sigma_{eq}}\underline{\underline{n}} - \frac{1}{3}\frac{\partial\Phi_G}{\partial p_m}\underline{\underline{\delta}}\right) = \dot{\varepsilon}_G^{pD}\underline{\underline{n}} + \frac{1}{3}\dot{\varepsilon}_G^{pM}\underline{\underline{\delta}} \tag{14}$$

where the distortional and dilatational parts, namely $\dot{\varepsilon}_G^{pD}$ and $\dot{\varepsilon}_G^{pM}$, respectively, of the inelastic strain rate $\underline{\underline{d}}^p$ are given by

$$\dot{\varepsilon}_G^{pD} = \Lambda\frac{\partial\Phi_G}{\partial\sigma_{eq}} = 2\Lambda\frac{\tilde{\sigma}_{\acute{e}q}}{\overline{\sigma}_y} \ ; \ \dot{\varepsilon}_G^{pM} = -\Lambda\frac{\partial\Phi_G}{\partial p_m} = 3q_1 q_2 f\Lambda\frac{\sinh\left[-\frac{3}{2}q_2(\tilde{p}_m + \tilde{p}_r)\right]}{\overline{\sigma}_y} \tag{15}$$

The evolution law of the isotropic hardening variable κ is deduced from the equality of the macroscopic plastic work rate with the microscopic one, see [2]:

$$\dot{\kappa} = \frac{\sigma_{eq}\dot{\varepsilon}_G^{pD} - p_m\dot{\varepsilon}_G^{pM}}{(1 - f)\overline{\sigma}_y} \tag{16}$$

Adiabatic heating is accordingly evaluated from

$$\rho C\dot{T} = \underline{\underline{\sigma}} : \underline{\underline{d}}^p - r\dot{\kappa} = \sigma_{eq}\dot{\varepsilon}_G^{pD} - r\dot{\kappa} - p_m\dot{\varepsilon}_G^{pM} \geq 0 \tag{17}$$

The porosity rate $\dot{f}$ is decomposed into a contribution due to growth of existing defects and a contribution due to the formation of new defects, see (18_1). The former, namely $\dot{f}_g$, is deduced from the classical hypothesis of matrix incompressibility, see (18_2), whereas the latter, namely $\dot{f}_n$, is the subject of the following sub section:

$$\dot{f} = \dot{f}_g + \dot{f}_n \ ; \ \dot{f}_g = (1-f) Tr \underline{\underline{d}}^p = (1-f)\dot{\varepsilon}_G^{pM} \ ; \ f_g(0) = f_0 \tag{18}$$

3.3 Micro void nucleation criterion and kinetics law

The hole nucleation criterion describes the conditions for which a specific volume fraction of ‘primary’ voids f_0 instantaneously germinates. A ‘secondary’ void initiation kinetic law is also proposed.

‘Primary’ micro void nucleation criterion

To ensure the instantaneous transition between dense metal plasticity and micro-porous metal plasticity, the hole nucleation criterion Φ_{I_0} is proposed in a form close to the micro-porous metal potential (10):

$$\Phi_{I_0} = \hat{\sigma}^2 + 2q_1 f_0 \cosh\left(-\frac{3}{2} q_2 (\hat{p}_m + \hat{p}_r)\right) - \left(1 + q_3 f_0^2\right) = 0$$

$$\hat{\sigma} = \frac{\sigma_y}{\sigma_c} \ ; \ \hat{p}_m = \frac{p_m}{\sigma_c} \ ; \ \hat{p}_r = \frac{p_r}{\sigma_c} \tag{19}$$

Hole nucleation (19) is clearly controlled by the stress triaxiality. In order to describe the delaying effects of the strain rate and the temperature in the hole nucleation process, we are assuming the following expression for the critical stress σ_c :

$$\sigma_c = \sigma_I + \sigma_{vp} \ ; \ \sigma_I = \alpha(R_0 + R_\infty) \tag{20}$$

The expression of the equivalent plastic strain at ‘primary’ hole nucleation $\kappa_0 = \kappa(f_0)$ may be explicitly deduced from (13) and (19) as:

$$\kappa_0 = -\frac{1}{k} \ln\left(1 - \left\{\frac{\sigma_c}{R_\infty}\left[1 - \left(\frac{T}{T_{fusion}}\right)^m\right]^{-1}\left[\left(1 + q_3 f_0^2\right) - 2q_1 f_0 \cosh\left(-\frac{3}{2} q_2\left(\frac{p_m + p_r}{\sigma_c}\right)\right)\right]^{1/2} - \frac{R_0}{R_\infty}\right\}^{1/\beta}\right) \tag{21}$$

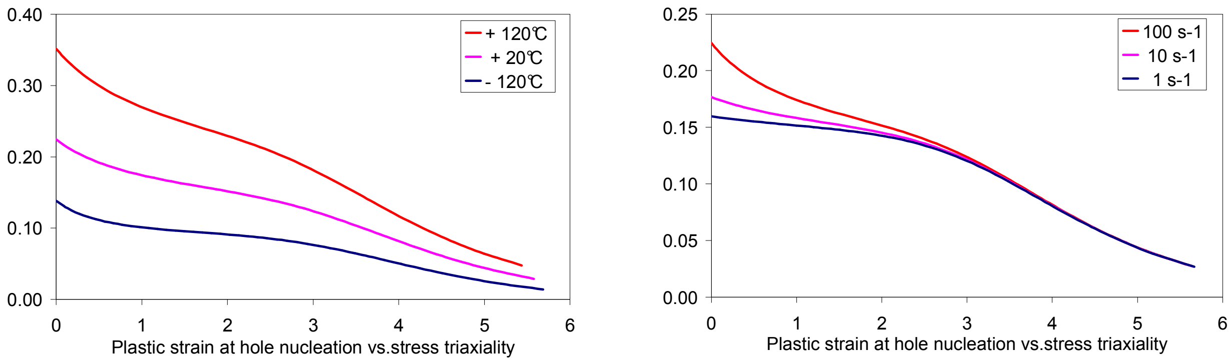
The plastic strain at ‘primary’ hole nucleation κ_0 in (21) is drawn in Fig.3 as a function of the stress triaxiality for various values of temperature and strain rate. The graphs in Fig.3 show clearly that the hole nucleation criterion given in (19) is able to reproduce, at least qualitatively, the accelerating effects of stress triaxiality and the delaying effects of temperature, see Fig.3.a, and strain rate, see Fig.3.b.

‘Secondary’ micro void formation kinetics law

In the present approach, the formation of ‘secondary’ voids, in addition to the ‘primary’ voids whose germination is controlled by the above criterion, is postulated. These ‘secondary’ voids include micro-voids of the same nature of the ‘primary’ voids but nucleating later, as well as nano-voids germinating between macro-voids and being consequently at the origin of the coalescence by localised shearing. In agreement with this definition, the 'secondary' void nucleation kinetics is supposed to be controlled by the rate of hardening, see (22_1) below. This hypothesis is consistent with the fact that hole germination requires a certain amount of plastic deformation. Based on the works by MOLINARI AND WRIGHT [8], the kinetic law for secondary nucleation is assumed to be well described by a WEIBULL type distribution function, see (22_2):

$$\dot{f}_n = B\langle\dot{\sigma}_y\rangle \; ; \; B = f_{\sup}\frac{p}{\sigma_c}\langle\Phi_{I_0}\rangle^{p-1}\exp\left(-\Phi_{I_0}^p\right) \; ; \; f_n(0)=0 \tag{22}$$

where p is a constant ($p=2$) and where $f_{\sup}$ represents the upper bound of the nucleated 'secondary' void volume fraction. $\langle . \rangle$ represents MCCAULAY brackets.



a) α=0.65 ; b=115MPa ; $f_0=10^{-4}$; $\dot{\kappa}=10^2 s^{-1}$

b) α=0.65 ; b=115MPa ; $f_0=10^{-4}$; T_0=20°C

Figure 3: Influence of temperature a) and strain rate b) on the plastic strain at hole nucleation

4 NUMERICAL SIMULATIONS AND CONFRONTATION WITH EXPERIMENTS

The model detailed in Sect.3 was implemented as user material (Vumat) in the engineering finite element computation code Abaqus®. The numerical integration is conducted in the GREEN-NAGHDI rotating frame using the classical return mapping procedure combined with the NEWTON-RAPHSON solving algorithm, see ARAVAS [9]. The thermal dilatation is supposed to be negligible in the present approach. Adiabatic conditions are furthermore assumed to be valid for plastic equivalent strain rate $\dot{\kappa}$ greater than $1s^{-1}$. In addition, failure is supposed to occur as soon as the porosity reaches the critical value f_r, leading numerically to the erosion of the concerned finite element. Some numerical simulations employing Abaqus® were conducted considering a cube under shear as well as notched structures under tension.

4.1 Case of a cube under simple shear

We are here considering a RVE submitted to a simple shearing in order to verify the ability of the model of Sect.3 to describe the consequences on the material behaviour of cavity growth under shear loading. From the numerical viewpoint, the RVE is represented by a single finite element C3D8R. The upper side is submitted to a tangential displacement at a constant velocity of 2.3m/s (leading to a plastic equivalent strain rate slightly greater than 1s^{-}

[1]) while the lower side is constrained. The set of material constants is reported in Table 1. Shear stress-shear strain and porosity-shear strain curves are shown in Fig.4 for various values b entering the expression of the kinematic pressure softening p_r, see (13).

Table 1: Micro-porous model related constants for the numerical simulation of a cube under simple shear

q_1	q_2	q_3	f_0	α	b (MPa)	f_{sup}	f_r
1	1	1	10^{-3}	0.65	/	16.10^{-2}	1.

The graphs in Fig.4.a show the combined softening effects of adiabatic heating and cavity growth induced damage on the material behaviour – for the ETVP (ElasticThermoViscoPlastic/sound material) model adiabatic heating is solely responsible for the softening behaviour at large deformation. It is furthermore clearly visible that the loss of shear resistance of the damaged material is more significant for large values of b. As shown in Fig.4.b this softening effect is induced by cavity nucleation and growth. Cavity growth under shear loading has been made possible thanks to the introduction of the kinematic pressure softening mechanism governed by p_r in the modified GTN model, see (10).

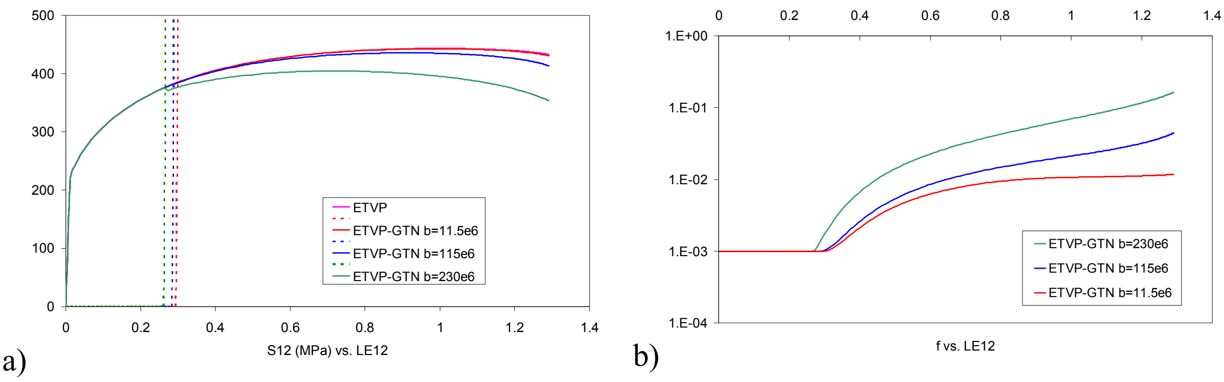


Figure 4: Influence of kinematic pressure. Simple shear on a cube.
a) Shear stress vs. shear strain. b) Volume fraction of holes vs. shear strain

4.2 Case of notched cylindrical samples under tension: simulation vs. experiment

This case deals with the tension of notched cylindrical specimens. The configurations with a notch radius value of 1.2mm (ST=1.15) and 6.2mm (ST=0.55) are considered here. Quasi-static tests were performed at room temperature and at 5mm/min on these samples. Concerning numerical simulations, the spatial discretisation consisted in meshing one eighth of the samples using solid finite elements with reduced integration C3D8R, as shown in Fig.5. The vertical translation of the lower face nodes is constrained while a vertical velocity is imposed to the upper face nodes. The sample material behaviour is described via the model detailed in Sect.3 (Vumat). The material constant values are reported in Table 2. The time integration scheme is explicit.

Experimental and numerical results are superposed in Fig.6 in the form of axial load-extensometer displacement curves. These curves allow for studying the influence of various model constants on the onset of void growth induced damage and subsequent drop in load, namely that of e.g. the ratio α in (20), see Fig.6. The influence of the secondary nucleation upper bound f_{sup}, and of the failure porosity f_r was also studied but is not shown here.

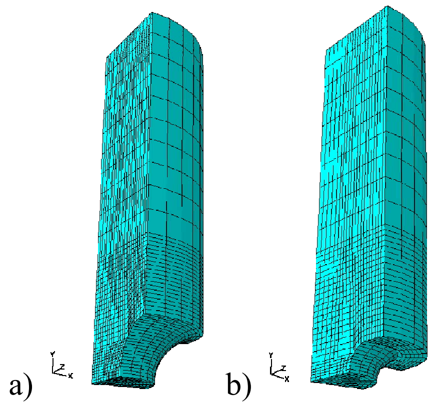
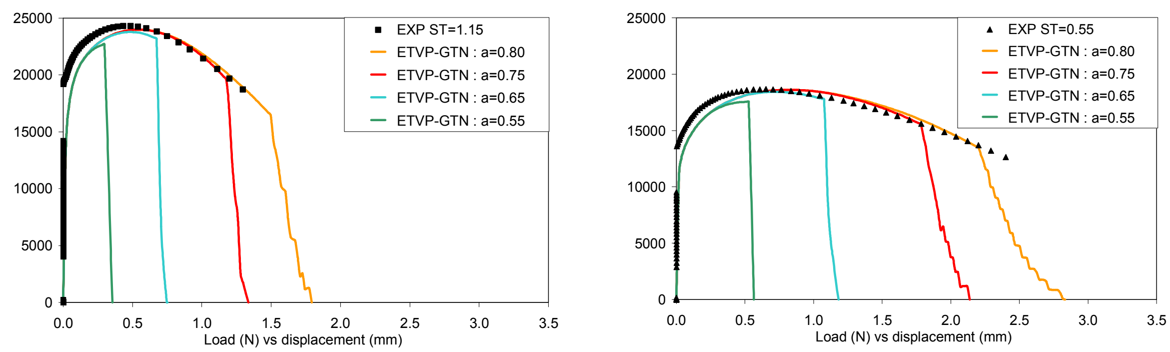


Figure 5: Meshing of the notched cylindrical samples; a) ST=0.55 b) ST=1.15

Table 2: Microporous model related constants for the numerical simulation of notched samples under tension

q_1	q_2	q_3	f_0	α	b (MPa)	f_{sup}	f_r
1	1	1	10^{-3}	/	115	0.03	0.2

Fig.6 clearly shows the effect of the critical stress σ_c via the ratio α, see (20), on the overall response of the notched round sample. Acting on the 'primary' void germination occurrence and on the 'secondary' void nucleation rate, see (20) and (22_2), the ratio α is consequently a key parameter in the model detailed in Sect.3. For early damage conditions, the ratio α value must be low, and for late damage conditions, the ratio α value must be large. Note that a large value of α provokes a progressive drop in load, contrarily to the brutal drop in load observed for a low value of α. According to Fig.6, the set α=0.75 - f_{sup}=0.03 - f_r=0.2 may be considered as satisfying for the material at stake.



a) ST=1.15 b) ST=0.55

Figure 6: Comparison model-experiment. Influence of the ratio α. Tensile test on notched cylindrical samples ; α= / - f_{sup}=0.03 - f_r=0.2

5 CONCLUSIONS

An elastic/viscoplastic model involving two stage void nucleation mechanism and void growth related hardening/softening effects has been put forward for a class of structural steels subjected to rapid loading conditions.

A phenomenological modelling approach has been developed, based on some salient physical effects regarding void growth vs. plastic straining, to describe the transition behaviour between dense metal plasticity and micro-porous metal plasticity. Considering that void germination requires a certain amount of plastic deformation, a 'primary' hole nucleation criterion has been proposed, as well as a statistical law governing the 'secondary' hole kinetics. In a consistent way, the hole nucleation criterion accounts for the accelerating effects of stress triaxiality and, conversely, the delaying effects of temperature and strain rate. In this work, a modification of the GTN model has also been proposed, overcoming its inability to predict damage growth and fracture for zero and low triaxiality, shear-dominated deformations. In this respect the kinematic mean stress related shift mechanism has been introduced and quantified in the expression of the GTN plastic potential, enabling thus the damage growth under shear and under small negative triaxialities. The 3D constitutive equations have been implemented as user material in the engineering finite element computation code Abaqus®. Numerical simulations have been conducted considering a single finite element under simple shear on one hand and a notched cylindrical sample under remote uniaxial tensile loading on the other hand. The numerical results show clearly the influence of the hole nucleation criterion related constants on the damage and further failure of the material.

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