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On the modelling of the interaction of materials softening and ductile damage during hot working of Alloy 80A

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Abstract

Ductile fracture at high temperatures was investigated by means of continuum damage mechanics. An enhanced model of the effective stresses considering crack closure effects by splitting the Cauchy stress tensor into a compressive and tensile part is combined with a damage evolution law that has been derived from the potential of dissipation and depends on the damage strain energy release rate. Additionally, a semi-empirical grain structure model had been coupled to describe the materials softening by dynamic recrystallisation. It can be demonstrated that with the onset of recrystallisation the accumulated deformation, i.e. the effective plastic strain, is reduced by recrystallisation and hence the initiation of damage is retarded. EBSD analyses of hot deformed samples were performed in order to validate the model and to investigate the interaction of crack initiation as well as of crack progress with dynamic recrystallisation.

Keywords: Damage; Dynamic recrystallisation; Crack initiation; Crack growth; EBSD

1. Introduction

Severely deformable materials mostly exhibit very narrow deformation windows in terms of temperature and strain rate, which generally can be defined by processing maps [1]. During hot rolling or open die forging for example, thermo-mechanical processing is performed in order to stay within this deformation window over the length and cross section of the work piece as well as to spare costly intermediate heat treatments. For a process optimisation and the tuning of the specified microstructure, grain structure models can be used that consider recrystallisation and grain growth [2]. Additionally, the materials formability is a function of local temperature, strain and the stress state. The resulting damage can be calculated e.g. with the model of effective stresses [3], considering crack closure effects by splitting the Cauchy stress tensor in a compressive and tensile part [4]. Therefore, the materials damage state in industrial processes can be simulated by implementing damage models into finite element (FEM) software [5]. If the local hot forming conditions are such that dynamic recrystallisation is initiated, deformation energy is reduced by the formation and growth of stress free grains. Hence, the damage rate and thus the probability of crack initiation are reduced.

For the validation of the model hot compression tests at different temperatures were performed. The critical strains at the appearance of first macro surface cracks were determined by a nesting procedure. The specimens were analysed by light microscopy and the EBSD method in order to describe the microstructure in the surrounding of the cracks as well as to investigate the interaction of crack formation, crack growth and dynamic recrystallisation.

2. Grain structure and damage model

The softening process in materials with a relative low stacking fault energy, e.g. steels in the austenitic regime, copper or nickel-based superalloys, is mainly governed by recrystallisation. After reaching a critical strain during deformation, the nucleation and growth of recrystallisation grains start. These new stress-free grains also experience deformation during an ongoing hot forming and thus can again lead to a further recrystallisation cycle [6]. The strain ε_p at maximum flow stress σ_p is given by (see e.g. [7]) $\varepsilon_p = A d_0^l Z^m$, with A, l, m as constants, d_0 as the initial mean grain size and Z as the Zener–Hollomon parameter. The critical strain for the initiation of dynamic recrystallisation can be set

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into relation to the peak strain by $\varepsilon_c = k_c\varepsilon_p$, where the constant k_c has typical values of 0.60–0.86 (see e.g. [7]). Inserting the time for 50% dynamic recrystallisation $t_{0.5}$ into the Avrami type equation for the recrystallised fraction $X = 1 - \exp\lfloor -\ln(2)(t/t_{0.5})^k \rfloor$ delivers the dynamically recrystallised (DRX) fraction X_{dyn}:

$$X_{\mathrm{dyn}} = 1 - \exp\left\{-0.693\left[\frac{tZ^b}{B^k}\exp\left(-\frac{Q_{\mathrm{def}}}{RT}\right)\right]^k\right\}, \quad (1)$$

with Q_{def} as the activation energy for deformation and b, B as well as k as material parameters. For the investigated Alloy 80A, two temperature regimes with the critical temperature of 1020 °C have to be stated. The separation in two temperature regimes is necessary in order to account for the precipitation of carbides and the γ'-phase $Ni_3(Al,Ti)$ in the lower temperature regime [6].

Since the introduction of the finite elements method in the hot bulk forming also damage criteria were defined. In the model of effective stresses the stressed material is divided into representative volume elements. With the occurrence of a damage D it is assumed that by formation of pores only the fraction $(1 - D)$ of the section of a volume element carries the applied loads. All by ductile failure affected parameters are accordingly treated as effective values. For the effective stress tensor $\tilde{\bar{\sigma}}$ thus follows $\tilde{\bar{\sigma}} = \bar{\sigma}/(1 - D)$, where $\bar{\sigma}$ is the Cauchy stress tensor. This is valid for tensile and also for compressive stresses, if the microcracks and microcavities remain open. For certain materials and certain conditions of loading, the defects may remain open. This is often the case for very brittle materials. If the defects close completely in compression, the area which effectively carries the load equals the initial undamaged area. To define an effective area in compression, a crack closure parameter h was defined [3] that depends a priori upon the material and the loading. The law of evolution of damage derives from the potential of dissipation Ψ, which is a scalar convex function of the state variables in case of isotropic plasticity and isotropic damage:

$$\dot{D} = -\frac{\partial\Psi}{\partial Y} = \left(\frac{Y}{S_0}\right)^{s_0}\dot{\varepsilon}_{\mathrm{eq}}, \quad (2)$$

where S_0 and s_0 are material and temperature dependent and $\dot{\varepsilon}_{eq}$ is the equivalent true strain rate. The damage strain energy release rate Y corresponds to the variation of internal energy density due to damage growth at constant stress and is given by [4,5]:

$$Y = \frac{1+\nu}{2E}\left[\frac{\langle\bar{\sigma}\rangle : \langle\bar{\sigma}\rangle}{(1-D)^2} + \frac{h\langle-\bar{\sigma}\rangle : \langle-\bar{\sigma}\rangle}{(1-hD)^2}\right] - \frac{\nu}{E}\left[\left(\frac{\mathrm{tr}(\langle\bar{\sigma}\rangle)}{(1-D)}\right)^2 + h\left(\frac{\mathrm{tr}(\langle-\bar{\sigma}\rangle)}{(1-hD)}\right)^2\right], \quad (3)$$

where E is the elastic modulus, υ is the Poisson's ratio, $\mathrm{tr}(\bar{x})$ denotes the trace of a tensor and $\langle x\rangle$ is the Macauley bracket. For the implementation of the crack closure parameter h in Eq. (3), we have to distinguish between tensile and compressive stresses in a multi-axially stressed state, thus to split the stress tensor in a positive and a negative part, related to the signs of the principal stresses σ_i. For the prediction of the material parameter, tensile tests of Alloy 80A were carried out for different temperatures in the range of 900–1050 °C (for details see [5]).

3. Numerical model

Both models, described above, were implemented into the FE program DEFORM 2D® with Lagrange code. The flow potential after von Mises was modified in order to describe the damaged material behaviour, i.e. to reduce the flow stress $k_f = \sigma_{eq}/(1 - D)$, where σ_{eq} is the equivalent von Mises stress. The evolution of damage as a function of the dynamically recrystallised fraction was calculated by

$$D_i = D_{i-1} + \frac{\dot{D}\Delta t}{D_c}(1 - X_{\mathrm{dyn}}), \quad (4)$$

where i demarks the time step, Δt the time increment and D_c is the rupture criterion. Therefore, rupture is assumed if D_i equals 1. If we reach a fully recrystallised structure, i.e. $X_{dyn} = 1$, the progress of materials damage stops.

An axisymmetric non-isothermal model was built up representing one half of the testing facility and considering heat transfer. The tools, tool holders and ground plates were modelled as rigid surfaces. Due to earlier measurements tools and work piece were set to the same values of temperature. A surface-to-surface contact with friction was introduced to model the interaction between the tools and the specimen. Downward velocity for upper tool, tool holder and ground plate were defined analogous to measured values while the bottom ones were fixed. To all parts of the model a boundary condition considering heat exchange with the environment was applied. The friction coefficient between the tools and the specimen (0.7 shear) was verified by the comparison of the experimental and the numerical results in terms of the force–displacement response and the barrelling geometry obtained from the deformed samples.

4. Experiments and simulations

To evaluate the effectiveness and applicability of fracture criteria at elevated temperatures, compression tests with different starting temperatures (900, 950, 1000 °C) for Alloy 80A samples of 16 mm diameter and 24 mm height were performed (see details in Table 1). Because of the friction between the specimen and the tools a barrelling effect occurs during the experiments on the surface near the horizontal symmetry section of the sample. The resulting circumferential stresses cause specimens to fracture. The fracture initiation site and resulting height at fracture initiation were determined by a nesting procedure. Cracks

Table 1
Processing parameters of compression tests for L306 samples

	Final height [mm]	Maximum force[kN]	Die speed [mm/s]	True strain	Average true strain rate [1/s]
900 °C, 1.5:1	19.7	131.2	5.2	0.2	0.24
950 °C, 1.5:1	17.3	73.6	6.3	0.33	0.31
1000 °C, 1.5:1	13.7	75.9	6.7	0.56	0.39