Analysis of hot forging of porous metals

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Abstract

Thermomechanical elastoplastic problems in hot forging of the porous metals have been analysed using the thermoelastoplastic finite element method. This finite element program has been formulated using the yield condition advanced by Lee and Kim and developed using the thermoelastoplastic time integration procedure. A hardening law of non-porous metals as a function of temperature, plastic strain and strain rate is proposed. The thermal and thermoelastic properties of porous metals, which are dependent on the relative density and the corresponding properties of the non-porous base metal, are used in finite element calculations. Thermomechanical response and densification behaviour of the porous metals during hot forging have been calculated at various initial relative densities, strain rates and temperatures. The calculated results are in good agreement with experimental data.

Keywords: Hot forging; Elastoplastic properties; Thermomechanics

1. Introduction

Understanding the deformation behaviour and the density distribution of porous metals during forming is very important in achieving good quality powder metalurgy parts. In the hot forming of sintered porous metals, the thermoelastoplastic deformation behaviour is influenced by the internal pores and temperature gradients in the porous workpiece. Since the mechanical properties of the porous metal are dependent on temperature as well as porosity, the analysis of the deformation during hot forming should be coupled with the heat transfer analysis.

Many experimental studies on forging of porous metal have been undertaken. Kuhn and Downey [1] and Mori et al. [2] investigated the deformation behaviours of porous iron and copper during cold forging. In hot forging of porous iron preforms, Fischmeister et al. [3] investigated densification behaviour, density distribution and macroscopic response. Im and Kobayashi [4] applied the rigid-plastic finite element technique using the yield condition of Shima and Oyane [5] to the analysis of plane strain compression of Fischmeister et al.'s [3] porous iron preforms. Kuhn and Ferguson [6] took into account the heat loss of porous specimens by radiation and convection to the environment during hot powder forging based on newtonian cooling, and they determined the localized temperature profiles in the tool and preform assuming them to be semi-infinite bodies in perfect contact. The thermoviscoplastic response and the densification behaviour of porous alloy steel preforms under hot upsetting conditions were investigated at various temperatures and strain rates by Kim and Cho [7]. Cho et al. [8] carried out and analysed the hot upsetting and ring compression of the porous alloy steel preforms to understand the thermoplastic response and densification behaviour of sintered porous alloy steel preforms. They proposed a yield function which has three experimental parameters to provide better theoretical predictions.

Lee and Kim [9] reviewed yield criteria for porous metals and found that most of them [5,10-13] unreasonably suggested zero yield stress only at zero relative density. Lee and Kim [9] modified a yield equation suggested by Doraivelu et al. [10] so that it incorporated only one experimental parameter estimated from yield stress vs. initial relative density data. This equation was derived using Kuhn's relationship [14] between Poisson's ratio in plastic deformation and the relative density of porous metals. The modified yield condition for porous metals is given by
where \( \eta = Y_y / Y_y^0 = [(R - R_c)/(1 - R_c)]^2 \)

\[
J' = (\sigma_{11} - \sigma_{22})^2 + (\sigma_{22} - \sigma_{33})^2 + (\sigma_{33} - \sigma_{11})^2
+ (\nu_{23} - \nu_{12})^2 + \nu_{23}^2 + \nu_{31}^2
\]

\( J_2 \) and \( J_1 \) are the second deviatoric stress invariant and
the first stress invariant respectively. \( Y_y \) is the yield stress of the porous metal with the relative density \( R \), and \( Y_y^0 \) is the yield stress at \( R = 1 \). For work-hardening
metals, \( Y_y \) and \( Y_y^0 \) are the flow stresses of the porous
and non-porous metals respectively. \( R_c \) is an experimental parameter and may be interpreted as a critical
relative density in which the yield stress of porous metal
becomes zero; that is, \( Y_y = 0 \) at \( R = R_c \). For non-
porous metals, \( R = 1 \) and \( Y_y = Y_y^0 \), and Eq. (1) be-
comes the von Mises yield criterion.

Eq. (1) accurately represents the densification of
porous iron specimens with various initial porosities
under hydrostatic pressure [15]. Han et al. [16] formu-
lated the elastoplastic finite element method of defor-
mation of porous metals using Eq. (1), and accurately
predicted the deformation behaviour of sintered porous
metals in simple upsetting [16], indenting [16] and ring
compression [17]. Also, Han et al. [18] calculated the
forging limit curves of porous sintered metals using Eq.
(1) and the Lee–Kuhn [19] initial imperfection model.

In the present work, thermoelastoplastic finite ele-
ment analyses of the thermomechanical response and
densification behaviour in axisymmetric hot upsetting
and ring compression of the sintered porous alloy pre-
forms have been carried out. In the formulation of the
finite element method employing Eq. (1), the tempera-
ture and relative density dependence of the constitutive
equation has been taken into account, but creep defor-
mation is neglected. For fully dense alloy steel, a new
strain-hardening law that is dependent on temperature
and strain rate is proposed. To analyse the densification
behaviour and thermoelastoplastic response during hot
forging of porous alloy steel preforms having cylindri-
cal and ring shapes, a coupled deformation and heat
transfer finite element analysis was performed.

\section{The constitutive equation in thermoelastoplastic
finite element formulation of porous metal}

For the yield condition of Eq. (1), under isotropic
hardening, the yield function \( F \) at time \( t \) can be ex-
pressed as

\[
F = (2 + R^2) J_2^p / 3 + (1 - R^2) J_1^p / 9 - \eta Y_y^0 / 3
\]

(2)

where \( \eta \) and \( \eta \) are the state variables which are
dependent on the plastic strains \( \varepsilon_{pl} \), and \( \eta \) is the state
variable which is dependent on the plastic strains \( \varepsilon_{pl} \)
and temperature. The yield function \( F \) is used to
calculate the plastic strain increments \( \varepsilon_{pl} \), and
the stress matrix increments \( d\sigma \) as follows:

\[
d\varepsilon_{pl}^R = \frac{\partial F}{\partial \sigma_{ij}}
\]

\[
d\sigma = C^p (\varepsilon - \varepsilon_{pl}^T) + (\varepsilon_{pl}^T (\sigma - \sigma_{w} ))
\]

\[
d\sigma = C^p d\sigma_{pl}^T + \varepsilon_{pl}^T (\sigma - \sigma_{w} )
\]

\[
d\sigma_{pl}^T = \frac{\partial F}{\partial \sigma_{ij}}
\]

The scalar values \( \varepsilon_{pl}^T \) and \( \sigma_{w} \) are defined by

\[
\varepsilon_{pl}^T = [\varepsilon_{pl}^T, \varepsilon_{pl}^T, \varepsilon_{pl}^T]
\]

\[
\sigma_{w} = [\sigma_{w}, \sigma_{w}, \sigma_{w}]
\]

\[
\varepsilon_{pl}^T = \frac{\partial F}{\partial \sigma_{ij}}
\]

where \( \varepsilon_{pl}^T \) and \( \sigma_{w} \) are transposes of \( \varepsilon_{pl} \) and \( \sigma_{w} \)
respectively and \( C^p \) is the inverse matrix of \( C^p \).

In the above, \( \varepsilon_{pl} \) and \( \varepsilon_{pl}^T \) are the total and thermal
strain increments in matrix notation, \( \varepsilon_{pl} \) is a positive scalar
and \( T \) is the temperature. \( C^p \) and \( C^p \) are the
relative density and temperature dependent elastic
stress–strain matrix and elastoplastic stress–strain matrix
respectively. The scalar value \( \nu \) and the vector values \( \nu \), \( \nu \) and \( \nu \) are defined by

\[
\nu = \frac{\partial F}{\partial T}
\]

\[
\nu = \frac{\partial F}{\partial \sigma_{ij}}
\]

\[
\nu = \frac{\partial F}{\partial \sigma_{ij}}
\]

where \( \nu \), \( \nu \) and \( \nu \) are transposes of \( \nu \), \( \nu \) and \( \nu \)
respectively and \( C^p \) is the inverse matrix of \( C^p \).

In the above, thermal strain increments \( d\varepsilon_{pl}^T \) are defined
by

\[
d\varepsilon_{pl}^T = \left[ \begin{array}{c}
d\varepsilon_{pl} \\
T - T_{ref}
\end{array} \right] dT
\]

(12)

where \( \varepsilon_{pl} \) is the effective plastic strain increment of the
matrix. Since the yield stress is a function of the plastic
work per unit volume, we can evaluate \( \nu \), \( \nu \) and \( \nu \)
using the following equations:
\[ \dot{r} = \frac{2}{3} \dot{\eta} Y_0 \frac{d}{dT} \]  
\[ \dot{p}_v = \frac{2}{3} \dot{\eta} E_0 E_T \sigma_y \]  
\[ \dot{g}_v = \frac{2}{3} \dot{\eta} R^2 \sigma_y + \frac{2}{3} (1 - R^2) J \dot{\gamma} \]  
where \( E_0, E_T \) and \( \sigma_y \) are the elastic modulus and tangential modulus of the non-porous base metal and the deviatoric stresses respectively.

3. Updating relative density during deformation

The constancy of mass during deformation of porous metals gives the following relation:

\[ R = \frac{V}{V_0} \]  
where \( R_0 \) and \( R \) are the relative densities before and after deformation, and \( V_0 \) and \( V \) are the volumes before and after deformation. For axisymmetric cases, the volume of an element with area \( A \) is given by

\[ V = \int_A 2\pi x \, dA \]  
where \( x \) is the distance between the centroid of the element and the axisymmetric line for axisymmetric condition. The first step to calculate the volume \( V \) is to relate the actual global coordinates \( x, y \) to a natural coordinate system \((-1 < \xi, \eta < 1, -1 < \zeta < 1)\). The integration of a scalar function \( f(\xi, \eta, \zeta) \) in natural coordinates can be obtained by applying the integration formula successively, namely

\[ \int_{-1}^{1} \int_{-1}^{1} f(\xi, \eta, \zeta) \, d\xi \, d\eta = \sum_{i=1}^{m} \sum_{j=1}^{n} w_i w_j f(\xi_i, \eta_j) \]  
where \( w_i \) and \( w_j \) are the weight factors and \( m \) and \( n \) are the orders of integration.

For a scalar function \( f(x, y) = 2\pi x \) in the axisymmetric case defined over an isoparametric element, the volume can be evaluated as

\[ V = \int_{-1}^{1} \int_{-1}^{1} f(\xi, \eta) \, d\xi \, d\eta \]  

\[ = \sum_{i=1}^{m} \sum_{j=1}^{n} w_i w_j f(\xi_i, \eta_j) \]  
where \( |J(\xi, \eta)| \) is the determinant of Jacobian matrix and given by

\[ |J(\xi_i, \eta_j)| = \frac{\partial x}{\partial \xi} \frac{\partial y}{\partial \eta} - \frac{\partial x}{\partial \eta} \frac{\partial y}{\partial \xi} \]  

The updated relative density \( R \) can be evaluated using Eqs. (16), (19) and (20).

4. Effective plastic strain rate and flow stress in non-porous base metal

During hot deformation of non-porous metal, the relation between effective plastic strain rate \( \dot{\varepsilon}^p \) and temperature dependence of the balanced stress \( \sigma^b \), which is determined by a balance between the accumulation (due to strain hardening) and elimination (due to recovery) of dislocations in non-porous metal, suggested by Garofalo [20], is given by

\[ \dot{\varepsilon}^p = A \exp(-\frac{Q}{R_T T}) \frac{\sinh(\beta \varepsilon)}{1 + \beta} \]  
where \( A, \beta \) and \( m \) are constants, and \( Q \) and \( R_T \) are the activation energy for deformation and the gas constant respectively.

In order to obtain an expression for the flow curves at various temperatures and strain rates, we propose the following relation:

\[ \dot{\varepsilon}^p = A \exp(-\frac{Q}{R_T T}) \frac{\sinh(\beta \varepsilon)}{1 + \beta} \]  

\[ Y_0 = K(\sigma^b + \varepsilon)^n \]  
where \( K \) is the strength coefficient, \( n \) is the strain-hardening exponent and \( \beta \) and \( \varepsilon \) are constants.

The relationship between the apparent effective plastic strain and strain rate of the porous metal and those of the non-porous base metal are given by

\[ \dot{\varepsilon}^p - \dot{\varepsilon}^p = \frac{1}{R} \frac{\sinh(\beta \varepsilon)}{1 + \beta} \frac{\sinh(\beta \varepsilon)}{1 + \beta} \]  

where \( \dot{\varepsilon}^p \) is the volumetric plastic strain rate.

The flow stress \( Y_0 \) of the non-porous base metal at a given temperature can be calculated using Eqs. (22)-(25). The absolute temperature of the porous metal can be calculated through the analysis of heat transfer.

5. Heat transfer in porous metal

For a porous metal the energy balance equation is expressed by

\[ \nabla \cdot (k_R \nabla T) - \rho_R c_R \dot{T} + \omega Y_R \dot{\varepsilon}^p = 0 \]  
where \( k_R, c_R \) and \( \rho_R \) are the thermal conductivity, specific heat and mass density respectively of the porous metal. The heat generation efficiency \( \omega \) represents the fraction of plastic deformation energy transformed into heat and is assumed to be 0.9.
A change in contact condition during deformation brings about a change in the heat transfer boundary condition. The heat transfer boundaries may be classified into non-contact and contact. Fig. 1 shows a schematic diagram of these two boundaries. The non-contact boundaries exist in the air which is assumed to be kept at a constant temperature. In the contact boundaries, two parts are in contact and the contact heat transfer coefficient is used to describe the heat transfer through the contact regions between two parts. The heat transfer boundary conditions are expressed as

\[ q_n = h_{\text{air}}(T_{\text{air}} - T_{\text{surf}}) \]  
for non-contact \hspace{1cm} (27)

\[ q_c = h_{\text{con}}(T_{\text{con}} - T_{\text{surf}}) \]  
for contact \hspace{1cm} (28)

where \( h_{\text{air}} \) is the heat transfer coefficient from the heat transfer boundary to air, \( T_{\text{air}} \) is the temperature of the air, \( T_{\text{surf}} \) is the temperature at the heat transfer boundary, \( h_{\text{con}} \) is the contact heat transfer coefficient and \( T_{\text{con}} \) is the temperature of the counterpart for heat flow.

Generally, the nodes at the contact boundary do not coincide with the counterpart nodes. Therefore, the interpolation of the counterpart nodal temperature with a shape function has been used in this study. If the boundary is partially contacted, the heat flow is calculated by using the contact area fraction.

6. Thermal and thermoelastic properties of porous metal

In this work, the thermal and thermoelastic responses of the porous metal are assumed to be isotropic, and the

\text{self-consistent} estimates of thermal and thermoelastic properties derived by Budiansky [21] were employed to incorporate the dependence on the relative density and the corresponding properties of the non-porous base metal. Budiansky's method estimates the properties of composite material which consists of a random mixture of \( N \) isotropic constituents. The porous metal is assumed to be a random mixture of void and non-porous base metal, and the thermal and thermoelastic properties of the porous metal are evaluated using Budiansky's estimates.

Fig. 2. Measured uniaxial stress-strain data (symbols) [8] of non-porous base alloy steel cylinders at various temperatures and strain rates of (a) \( 10^{-2} \text{s}^{-1} \) and (b) \( 10^{-1} \text{s}^{-1} \) together with calculated values.
Table 1
Elastic properties [22] of alloy steel

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Room temperature</th>
<th>300</th>
<th>600</th>
<th>700</th>
<th>800</th>
<th>900</th>
<th>1000</th>
<th>1100</th>
<th>1200</th>
</tr>
</thead>
<tbody>
<tr>
<td>Poisson's ratio $\nu$</td>
<td>0.275</td>
<td>0.280</td>
<td>0.310</td>
<td>0.320</td>
<td>0.375</td>
<td>0.450</td>
<td>0.370</td>
<td>0.370</td>
<td>0.370</td>
</tr>
<tr>
<td>Young's modulus $E$ (GPa)</td>
<td>171</td>
<td>163</td>
<td>144</td>
<td>131</td>
<td>107</td>
<td>93</td>
<td>99</td>
<td>93</td>
<td>87</td>
</tr>
</tbody>
</table>

6.1. Elastic constants and coefficients of thermal expansion

Since the bulk modulus and shear modulus of the void in porous metal is zero, it follows from Budiansky's estimates that bulk modulus $B_R$ and shear modulus $G_R$ of the porous metal are given by

$$ \frac{1 - R}{1 - \alpha} + \frac{R}{1 - a + aB_b/B_R} = 1 $$

and

$$ \frac{1 - R}{1 - \beta} + \frac{R}{1 - b + bG_b/G_R} = 1 $$

where $B_b$ and $G_b$ are the bulk and shear moduli of non-porous base metal, and

$$ a = \frac{11 + \nu_R}{3(1 - \nu_R)} \quad \text{and} \quad b = \frac{24 - 5\nu_R}{15(1 - \nu_R)} $$

where $\nu_R$ is Possion's ratio of the porous metal given by

$$ \nu_R = \frac{3B_R - 2G_R}{6B_R + 2G_R} $$

The coefficient $\alpha_R$ of thermal expansion of the porous metal is given by the following relation:

$$ \alpha_R = R(B_b/B_R)(1 - a + aB_b/B_R)^{-1}\alpha_b $$

where $\alpha_b$ is the coefficient of thermal expansion of the non-porous base metal.

6.2. Thermal properties

The thermal conductivity $k_R$ of the porous metal is expressed as

$$ \frac{1 - R}{2/3 \cdot k_v/3k_R} + \frac{R}{2/3 \cdot k_b/3k_R} = 1 $$

where $k_v$ and $k_b$ are the thermal conductivities of the void and non-porous base metal respectively.

The heat capacity $\rho_R c_R$ per unit volume of the porous metal is given by

$$ \rho_R c_R = \rho_b c_b R $$

where $c_b$ and $\rho_b$ are the specific heat and mass density of the non-porous base metal.

Fig. 3. Normalized initial yield stresses of porous alloy steel preforms as a function of relative density [8]; the best fitting of the data yields $R_C = 0.648$.

Fig. 4. Initial mesh for finite element analysis of hot upsetting of porous alloy steel preforms at a constant temperature.

Table 2
Parameters in Eqs. (22) and (23) of alloy steel

<table>
<thead>
<tr>
<th>$\beta$ (MPa$^{-1}$)</th>
<th>$\epsilon_0$</th>
<th>$n$</th>
<th>$A$ (s$^{-1}$)</th>
<th>$Q$ (kJ mol)</th>
<th>$m$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.013 59</td>
<td>0.004 134</td>
<td>0.1568</td>
<td>$1.331 \times 10^{10}$</td>
<td>308.3</td>
<td>0.3</td>
</tr>
</tbody>
</table>
7. Applications

7.1. Axisymmetric hot upsetting

Cho et al. [8] carried out hot upsetting of 11.28 mm diameter and 14 mm high porous alloy steel cylinders at various temperatures in the range 800–1200 °C and at axial velocities of 0.12 and 1.2 mm s⁻¹. The elastic properties [22] of the non-porous alloy steel (matrix) are given in Table 1.

Fig. 2 shows the calculated and measured uniaxial stress–strain data [8] for a non-porous alloy steel at various temperatures and strain rates. The experimental data were best fitted by Eqs. (22) and (23) using a non-linear fitting method, giving the parameters in Table 2. The deformation behaviour of the matrix is well described by Eqs. (22) and (23) in the temperature and strain rate ranges of interest. The activation energy for deformation of the non-porous alloy steel evaluated at temperatures from 0.5T_m to 0.8T_m was found to be 308 kJ mol⁻¹, which is close to the value for bulk diffusion of γ-Fe, 284 kJ mol⁻¹ [23]. Therefore, the rate-controlling restoration process of deformation of the non-porous alloy steel seems to be bulk diffusion, as expected.

Fig. 3 shows the normalized initial yield stress of the porous alloy steel preforms as a function of relative density, from which the value of R_C in Eq. (1) is found to be 0.648. Fig. 4 shows the initial mesh for the elastoplastic finite element calculation of the hot upsetting of porous alloy steel cylinders.

Fig. 5 shows the experimental load–displacement data [8] of the porous alloy steel preforms with initial relative densities of 0.846 and 0.878 at various temperatures and axial velocities together with the values calculated by the finite element method under frictionless conditions. The calculated results are in very good agreement with experimental data.

Fig. 6 shows Cho et al.'s measured average relative densities of the porous alloy steel preforms in the temperature range 800–1200 °C as a function of displacement, together with those at 1000 °C obtained by
Fig. 6. Experimental average relative density of porous alloy steel preforms with initial relative densities of (a) 0.846 and (b) 0.878 as a function of displacement [8] are compared with values calculated assuming a friction coefficient of 0.3.

The finite element method. The average relative densities \( R_{\text{avg}} \) at each step are computed from

\[
R_{\text{avg}} = \frac{\sum_i^N R_i V_i}{\sum_i^N V_i}
\]

where \( R_i \) and \( V_i \) are the relative density and volume respectively of the \( i \)th element, and \( N \) is the total number of elements. At the early stage of upsetting, the calculated average relative density increases slowly with increasing friction owing to the barrelling effect. The effect of friction coefficient on the average relative density of upset porous metals was discussed in a previous paper [16]. The result calculated at a friction coefficient of 0.3 accurately fits the experimental results.

The data in Figs. 5 and 6 indicate that Eq. (1) gives rise to very good results over the temperature and strain rate ranges studied.

7.2. Axisymmetric hot ring compression

Cho et al. [8] carried out hot compression of porous alloy steel rings with an initial relative density of 0.866 at 1000 °C and at an axial velocity of 1 mm s\(^{-1}\). The specimens had dimensions of 11.93 mm in outer diameter, 5.2 mm in inner diameter and 10 mm in height. Fig. 7 shows the initial mesh and boundary conditions for the coupled analysis of deformation and heat for ring compression, which takes the heat flow during deformation into account. Initial temperatures of the die and specimen were assumed to be 25 °C and 1000 °C respectively. The thermal and mechanical properties [4,24,25] for the finite element computation are given in Table 3.

Fig. 8 shows the shapes of the deformed porous specimens calculated under a friction coefficient of 0.1 assuming an isothermal workpiece at 1000 °C and taking heat transfer into account, together with the experimental shape [8] of a 25% reduced specimen. The results calculated taking heat transfer into account are in better agreement with the experimental shape.

Fig. 9 shows the temperature distribution in the 25% reduced porous ring calculated under a friction coefficient of 0.1. It can be seen that the isotherms in the specimens are almost linear and the temperature in the central region of the porous specimen is higher than that in the specimen–die contact region, which in turn gives rise to the lower flow stress in the central region than in the contact region. Therefore, the outward
Table 3
Thermal and mechanical properties \cite{4,24,25} of alloy steel

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>( \alpha_0 ) (K(^{-1})) (Reference 20 °C)</th>
<th>( h_0 ) (J s(^{-1}) m(^{-2}) K(^{-1}))</th>
<th>( h_\infty ) (J s(^{-1}) m(^{-2}) K(^{-1}))</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Room temperature</td>
<td>300</td>
<td>600</td>
</tr>
<tr>
<td></td>
<td></td>
<td>1062</td>
<td>1062</td>
</tr>
<tr>
<td></td>
<td>486</td>
<td>7620</td>
<td>7540</td>
</tr>
<tr>
<td>300</td>
<td></td>
<td>7780</td>
<td>7660</td>
</tr>
<tr>
<td>600</td>
<td></td>
<td>7620</td>
<td>7540</td>
</tr>
<tr>
<td>900</td>
<td></td>
<td>7580</td>
<td>7500</td>
</tr>
<tr>
<td>1200</td>
<td></td>
<td>7500</td>
<td>7460</td>
</tr>
<tr>
<td>1.98 \times 10^{-5}</td>
<td>5500</td>
<td></td>
<td></td>
</tr>
<tr>
<td>( \rho ) (kg m(^{-3}))</td>
<td>7900</td>
<td></td>
<td></td>
</tr>
<tr>
<td>( \rho ) (J s(^{-1}) m(^{-1}) K(^{-1}))</td>
<td>36</td>
<td></td>
<td></td>
</tr>
<tr>
<td>( k ) (J s(^{-1}) m(^{-1}) K(^{-1}))</td>
<td>0.001</td>
<td></td>
<td></td>
</tr>
<tr>
<td>( k_\infty ) (J s(^{-1}) m(^{-1}) K(^{-1}))</td>
<td>0.026</td>
<td></td>
<td></td>
</tr>
<tr>
<td>( k_\infty ) (J s(^{-1}) m(^{-1}) K(^{-1}))</td>
<td>0.047</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Fig. 8. Comparison between experimental \cite{8} and calculated shapes of 25% reduced porous alloy steel specimens with an initial relative density of 0.866. The specimens were reduced at 1000 °C and an axial velocity of 1 mm s\(^{-1}\). The finite element results were calculated assuming an isothermal workpiece at 1000 °C and taking heat transfer into account as indicated.

barrelling calculated taking the heat transfer in the contact into account is larger than that calculated assuming an isothermal workpiece as shown in Fig. 8.

Fig. 8. Comparison between experimental \cite{8} and calculated shapes of 25% reduced porous alloy steel specimens with an initial relative density of 0.866. The specimens were reduced at 1000 °C and an axial velocity of 1 mm s\(^{-1}\). The finite element results were calculated assuming an isothermal workpiece at 1000 °C and taking heat transfer into account as indicated.

Densification of porous metals during hot forging at various temperatures and strain rates has been analysed using the thermoelastoplastic finite element method in

8. Conclusions

Fig. 10 shows the measured density distributions in the 25% reduced porous alloy steel ring preform together with the result calculated assuming an isothermal workpiece and taking heat transfer into account. Again, the value obtained taking heat transfer into account gives the better results.

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8. Conclusions

Densification of porous metals during hot forging at various temperatures and strain rates has been analysed using the thermoelastoplastic finite element method in
which the yield criterion advanced by Lee and Kim [9]
is incorporated. In this formulation of the finite element
method, the temperature and relative density depen-
dence of the constitutive equation is taken into account,
butf creep deformation is neglected. For a non-porous
alloy steel (matrix), a temperature- and strain-rate-de-
pendent strain hardening law has been proposed. De-
formation behaviour of the non-porous alloy steel is
well described by the strain hardening law. The coupled
analysis of deformation and heat transfer simulates well
the dimensional changes, densification, load and tem-
perature distribution during hot forging of the porous
alloy steel at various initial temperatures and strain
rates.

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