A Void Growth Model Considering the Bauschinger Effect and Its Application to Spall Fracture

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A void growth model considering the Bauschinger effect (BE) is proposed for ductile materials sustaining impact loading. Numerical simulations of two high-velocity impact problems are carried out by our newly developed Eulerian programs. The proposed model is tested by a plate impact problem and a qualitative agreement with the experimental data when the BE is considered. The proposed model reveals that the BE has an obvious effect on the spall process.

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The spall fracture of ductile material is considered as a sequence of nucleation, growth, coalescence of microscopic voids.[1] Most publications about the spall model are dedicated to improve the void growth model. Johnson[2] first introduced the model, developed by Carroll and Holt,[3] to describe the void growth process. He extended the previous work by adding the strain rate contribution to the yield stress of the matrix material and proved that the void growth can be modeled as a fully plastic process. Perzyna[4] improved Johnson’s work by including a linear strain hardening term in the yield stress expression. Eftis et al.[5] modified Perzyna’s model by considering a nonlinear isotropic hardening law that allows for saturation of the hardening with increase of strain. Cortes[6] analyzed in detail the influence of material viscosity, strain hardening and thermal softening on the tensile fracture behavior and concluded that the thermal softening has a negligible influence on the yield stress. Eftis et al.[6] summarized the previous works and proposed a constitutive-microdamage model which is fairly comprehensible.

Note that the spall is caused by tension after compression, the Bauschinger effect (BE) may exist during the spall process. Thakur et al.[7] developed a testing method to investigate the BE under dynamic loading. They observed that metals do not exhibit a BE in the quasi-static state may show a BE at very high strain rate. Hermann et al.[8] added the BE to the constitutive model to simulate the plate impact experiment and obtained improved match between simulation and experiment. However, no literature about the spall model considering the BE has been published yet.

In this Letter, a void growth model taking into account the BE is proposed. In order to describe the BE, a linear kinematic model is added to the Tresca yield condition. The proposed model is introduced into our newly developed Eulerian codes.[9] Numerical simulations of two high-velocity impact problems with spall fractures are carried out and compared to the experimental data.

![Fig. 1. Illustration of the void growth model: (a) cubic element with a random distribution of voids, (b) spherical void surrounded by a matrix material.](image)

The derivation begins with the consideration of a representative volume element containing a random distribution of voids, as illustrated in Fig.1(a). All the voids are assumed to be spherical. Imagine a uniform mean stress \( \sigma \) acting over the surface of this element. Since the cross-sectional area occupied by the voids does not support the stress, we have

\[
A \sigma = A_s \sigma_s,
\]

where \( A \) is the total area, containing the voids, of the cross section, \( \sigma \) is the mean stress acting on \( A \). \( A_s \) is the solid material part on plane \( A \) and \( \sigma_s \) is the mean stress on \( A_s \). For a random distribution of voids shapes and sizes, the following relation is acknowledged

\[
A/A_s = V/V_s,
\]

where \( V \) is the whole volume of the element and \( V_s \) is the volume of the solid material. Then we have

\[
\sigma_s = (V/V_s) \sigma = \alpha \sigma,
\]

where \( \alpha \) is defined as distortion ratio. Around each of the voids in the distended material, there is a mean

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stress \( \sigma_x \). If this stress is big enough in tension, the voids will grow by plastic deformation of the surrounding material. Another variable, VVF \( \xi \), is also frequently used in the published literatures. According to its definition, the following relation is obtained

\[
\xi = \frac{V - V_s}{V} = 1 - \frac{1}{\alpha}.
\]

Considering a spherical void of radius \( a \) in a sphere of radius \( b \) where the outer boundary is subjected to the mean stress \( \sigma_m \), as shown in Fig. 1(b), we express the equation of motion for the problem in the spherical coordinates as

\[
\frac{\partial \sigma_r}{\partial r} + \frac{2}{r} (\sigma_r - \sigma_\theta) = \rho \ddot{r},
\]

where \( \sigma_r \) is the radial stress, \( \sigma_\theta \) is the tangential stress, \( \rho \) is the density of the solid material and \( \ddot{r} \) is the radial acceleration. The boundary conditions are

\[
\sigma_r(a, t) = 0, \sigma_r(b, t) = \sigma_s = \alpha \sigma, \tag{6}
\]

where \( \alpha = b^2/(b^2 - a^2) \). The inertial term \( \rho \ddot{r} \) in Eq. (5) is usually neglected.\(^{[1]}\) Integrating Eq. (5) from \( a \) to \( b \), we have

\[
\alpha \sigma + \int_a^b \frac{2}{r} (\sigma_r - \sigma_\theta) \, dr = 0. \tag{7}
\]

According to the viscoplastic constitutive theory, the yield surface of the material is given by

\[
f = \| \dot{\sigma} - \dot{X} \| - R - \kappa = 0, \tag{8}
\]

where \( \dot{X} \) is the back-stress for kinematic hardening, \( R \) is the increment of the yield surface size for isotropic hardening and \( \kappa \) is the initial yield surface size. The linear kinematic hardening model proposed by Prager and described in Ref. [8] is adopted here to account for the BE. The evolution of the back-stress \( \dot{X} \) is collinear with the evolution of the plastic strain

\[
\dot{X} = \frac{2}{3} h \dot{\varepsilon}^p, \tag{9}
\]

where \( h \) is the linear hardening modulus determined by the slope of the \( \sigma - \varepsilon^p \) curve. The Tresca yield condition is adopted,

\[
\frac{\sigma_\theta - \sigma_r}{2} = \tau_s, \tag{10}
\]

where \( \tau_s \) is the shear yield stress. For most materials, \( \sigma_s = \sqrt{3} \tau_s \), where \( \tau_s \) is the tensile yield stress. The tensile yield stress is calculated by the simplified form of the Johnson–Cook (JC) model.\(^{[12]}\) From Eq. (8), we obtain the expression

\[
\frac{\sqrt{3}}{2} \left[ \left( \sigma_\theta - \frac{2}{3} h \varepsilon^p \right) - \left( \sigma_r - \frac{2}{3} h \varepsilon^p \right) \right] = \sigma_s = \kappa + R = A + B (\varepsilon^p)^n + \eta \varepsilon^p, \tag{11}
\]

where \( A, B \) and \( n \) are the material parameters of the JC model, \( \eta \) is the viscosity of the material, \( \varepsilon^p \) is the equivalent plastic strain and \( \varepsilon^p \) is the equivalent plastic strain rate.

The equivalent plastic strain and strain rate are expressed as

\[
\varepsilon^p = \frac{2}{3} [\varepsilon^p - \varepsilon^p] = \frac{2}{3} \left[ \left( \frac{\partial u}{\partial r} - \frac{u}{r} \right) \right],
\]

\[
\dot{\varepsilon}^p = \frac{2}{3} \frac{d}{dt} \left[ \frac{\partial u}{\partial r} - \frac{u}{r} \right]. \tag{12}
\]

Then Eq. (11) can be written as

\[
\sigma_\theta - \sigma_r = (2/\sqrt{3}) [A + B (\varepsilon^p)^n + \eta \varepsilon^p] - h \varepsilon^p. \tag{13}
\]

Substituting Eq. (13) into Eq. (7), we have

\[
\alpha \sigma = \frac{2}{\sqrt{3}} \int_a^b \frac{2}{r} \left[ A + B (\varepsilon^p)^n + \eta \varepsilon^p \right] \, dr - \int_a^b \frac{2}{r} h \varepsilon^p \, dr. \tag{14}
\]

The radial displacement can be calculated by

\[
\dot{u} = r - r_0 = \left[ r^3 + B(t) \right]^{1/3}. \tag{15}
\]

Substituting Eqs. (12) and (15) into Eq. (14) and finishing the integration, we have

\[
\alpha \sigma = \frac{4 \sqrt{3}}{9} \left[ A \ln \left( \frac{\alpha}{\alpha - 1} \right) - B \left( \frac{2}{3} \right)^n \Pi(\alpha) \right] - \frac{4}{3} h |\Gamma(\alpha)|
\]

\[
+ \frac{8 \sqrt{3}}{27} \eta \left( \frac{\alpha}{\alpha - 1} \right)^{1/3} |\Gamma(\alpha)|, \tag{16}
\]

where

\[
\Pi(\alpha) = \int_{B(t)/a^3}^{B(t)/b^3} \frac{1}{x} \chi(1 + \chi)^{-2/3} \, d\chi, \tag{17}
\]

\[
G(\alpha) = \left( \frac{\alpha_0 - 1}{\alpha - 1} \right)^{1/3} - \left( \frac{\alpha_0}{\alpha} \right)^{1/3},
\]

\[
F(\alpha) = (\alpha - 1) \left( \frac{\alpha}{\alpha_0} \right)^{2/3} - \alpha \left( 1 - \frac{\alpha - 1}{\alpha_0 - 1} \right)^{2/3}. \tag{18}
\]

When \( \dot{\alpha} \) in Eq. (16) approaches zero, the threshold stress for void growth is obtained,

\[
\sigma_g = \frac{4 \sqrt{3}}{9} \frac{1}{\alpha} \left[ A \ln \left( \frac{\alpha}{\alpha - 1} \right) - B \left( \frac{2}{3} \right)^n \Pi(\alpha) \right]
\]

\[
- \frac{4}{3} h \frac{1}{\alpha} |\Gamma(\alpha)|. \tag{19}
\]

Then Eq. (16) can be written as

\[
\alpha(\sigma - \sigma_g) = \frac{8 \sqrt{3}}{27} \eta \frac{\alpha}{\alpha - 1} |\Gamma(\alpha)|. \tag{20}
\]

Expressing Eq. (20) in terms of the VVF, we have

\[
\sigma - \sigma_g = \frac{8 \sqrt{3}}{27} \eta F(\xi) \dot{\xi}, \tag{21}
\]
where

\[
\sigma_g = \frac{4\sqrt{3}}{9} (1 - \xi) [A \ln(1/\xi) - B(2/3)^n \Pi(\xi)] \\
- \frac{4}{3} h (1 - \xi) G(\xi),
\]

\[
\Pi(\xi) = \int_{\chi_1}^{\chi_2} \frac{1}{\chi} |\chi(1 + \chi)^{-2/3}| d\chi,
\]

\[
\chi_1 = \frac{1}{\xi} (\frac{\xi_0 - \xi}{1 - \xi_0}), \quad \chi_2 = \frac{\xi_0 - \xi}{1 - \xi_0},
\]

\[
G(\xi) = \left( \frac{1 - \xi}{1 - \xi_0} \right)^{1/3} \left[ 1 - \left( \frac{\xi_0}{\xi} \right)^{1/3} \right],
\]

\[
F(\xi) = \left( \frac{1 - \xi_0}{1 - \xi} \right)^{2/3} \left[ \frac{1}{\xi} \left( \frac{\xi_0}{\xi} \right)^{2/3} - 1 \right].
\]

The void growth rate is expressed as

\[
\dot{\xi} = \frac{27}{8\sqrt{3}} \frac{1}{\eta F(\xi)} (\sigma - \sigma_g) e^{\phi \xi},
\]

where \(e^{\phi \xi}\) is a modification proposed by Eftis et al.\(^5\) for the void interaction during the coalescence process. Most material parameters in Eq. (2) can be obtained from the JC model and only one additional parameter \(h\) is needed to describe the BE. It is convenient to implement numerical simulations by the proposed model.

The proposed void growth model is introduced into our newly developed Eulerian codes.\(^{11,13}\) A novel representation of crack by level set proposed by us\(^{11}\) is adopted to perform the formation and propagation of the crack. The VVF accounts for the spall fracture and its critical value is \(\xi_c = 0.3\). In addition, a limit strain of 2.5 is used to describe the shear fracture near the crater.\(^7\)

Table 1. Material parameters of the target plates: aluminum 1100 and oxygen-free high conductivity (OFHC) copper.

<table>
<thead>
<tr>
<th></th>
<th>(\rho)</th>
<th>(K_0)</th>
<th>(G_0)</th>
<th>(A)</th>
<th>(B)</th>
<th>(n)</th>
<th>(\eta)</th>
<th>(\xi_0)</th>
<th>(\xi_c)</th>
</tr>
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<tbody>
<tr>
<td>Copper</td>
<td>8930</td>
<td>130</td>
<td>43.33</td>
<td>90</td>
<td>292</td>
<td>0.31</td>
<td>1.0</td>
<td>3</td>
<td>0.3</td>
</tr>
<tr>
<td>Al(_{1100})</td>
<td>2770</td>
<td>78.6</td>
<td>27.1</td>
<td>150</td>
<td>170</td>
<td>0.34</td>
<td>1.0</td>
<td>3</td>
<td>0.3</td>
</tr>
</tbody>
</table>

The plate impact problem is considered as a benchmark for the spall fracture. One of the plate impact experiments performed by Russian scientists\(^{14}\) is simulated. The experiment consists of a 2-mm-thick aluminum plate acting as a high-speed projectile and a 12-mm-thick copper plate. The flyer plate is at a speed of 450 m/s and the target plate is at rest before impact. The material parameters of the target plate can be found in Table 1. The comparison between the numerical results and the experimental data of particle velocity history at the free surface is shown in Fig. 2. According to the experimental plot,\(^{14}\) time \(t = 0\) is relevant to the moment when particles on the back side of the target start to move. The particles at the free surface are initially stationary. It can be observed from Fig. 2 that the velocity history obtained by simulation essentially coincides with the experimental profile.

Fig. 2. Comparison of the free surface velocity profiles between the experiment and the simulation.

Fig. 3. Comparison of the evolution of VVF distribution inside the target plate between the numerical results considering and not considering the BE. Results (a), (b) and (c) do not include the BE and the initial yield strength is 40 MPa. Results (d), (e) and (f) include the BE and the initial yield strength is 150 MPa. Here (a) and (d) are taken at \(t = 5\) \(\mu\)s, (b) and (e) at \(t = 10\) \(\mu\)s, (c) and (f) at \(t = 15\) \(\mu\)s.

Then a more complicated problem, of which an aluminum 1100 target plate is impacted by a spherical soda-lime glass projectile at a velocity of 6.0 km/s,\(^{15}\) is simulated. The thickness of the target plate is 12.5 mm. The diameter of the projectile is 3.2 mm. In order to investigate the influence of the BE on the spall process, the problem is simulated by both models with and without the BE term. Investigation is also made on the initial yield strength of Al\(_{1100}\) since different values are found in different literature.\(^{15,16}\) The material parameters of the target plate are listed...
in Table 1.

Figure 3 presents the VVF distribution inside the target plate at \( t = 5, 10 \) and \( 15 \mu s \). The plots on the left side are the numerical results not considering the BE and with the 40 MPa initial yield strength, while the plots on the right side are the results considering the BE and with the 150 MPa initial yield strength. The crack on the right side is obviously longer than the one on the left side. The right crack keeps expanding from 5 \( \mu s \) to 15 \( \mu s \). We may deduce that the crack is produced by two factors: The main part which occupies the crack’s most length is caused by spall; the shorter part at the corner of the crack is torn apart by the fast-moving scab. The left crack, however, hardly propagates after 5 \( \mu s \) when the spall is finished. The tensile strength of the material at the crack corner is weakened since the material has been compressed into plastic region. The material at the corner is not ripped since the BE is not considered. The craters on the left side are larger than the ones on the right side since the initial yield strength of the material on the left side is smaller, which makes the material softer.

![Contour plot of the equivalent plastic strain at \( t = 15.0 \mu s \). Geometric parameters indicating key deformation characters are denoted.](image)

The contour plot of the equivalent plastic strain at \( t = 15 \mu s \) is shown in Fig. 4. The highest strain appears near the crater. The plastic strain near the spall plane on the scab is also quite high, which indicates that the material on the spall plane goes into the plastic region before spall. A fracture surface caused by shear deformation is observed at the prominent lip of the crater. The equivalent plastic strain at the lip is close to the limit strain 2.5. The geometric parameters which describe the key deformation characters of the plate after impact are marked in Fig. 4. The numerical results are compared with the experiment in Table 2. The result calculated by the model with the BE agrees well with the experimental data, including the crater size and the crack length. The crack is much shorter than the experiment when the BE is not considered. The numerical results prove the influence of the BE on the spall process. However, the scab thickness is smaller and the crack open width is much larger than the experiment. To improve this, a more precise kinematic hardening model is needed.

Table 2. Comparisons of damage features between the experiment and the simulations.

<table>
<thead>
<tr>
<th></th>
<th>a (mm)</th>
<th>b (mm)</th>
<th>c (mm)</th>
<th>d (mm)</th>
<th>e (mm)</th>
<th>f (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>a</td>
<td>5.9</td>
<td>7.5</td>
<td>4.8</td>
<td>4.2</td>
<td>1.4</td>
<td>8.6</td>
</tr>
<tr>
<td>b</td>
<td>5.6</td>
<td>7.4</td>
<td>4.7</td>
<td>5.0</td>
<td>1.1</td>
<td>8.0</td>
</tr>
<tr>
<td>c</td>
<td>6.6</td>
<td>9.0</td>
<td>3.7</td>
<td>4.7</td>
<td>1.3</td>
<td>5.2</td>
</tr>
</tbody>
</table>

In conclusion, we have proposed a void growth model which considers the BE. Numerical simulation of a plate impact problem is carried out and the results are qualitatively in agreement with the experimental data. Then the impact process of an Al\(_{1100}\) target plate by a spherical glass projectile at a velocity of 6.0 km/s is simulated. The influence of the BE on the spall process is investigated by numerical analyses. Better agreement with the experiment is obtained when the BE is considered. The proposed model reveals the effect of the BE on the spall process, which is difficult to test by experiment.

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