Densification of metal coated fibers by elastic–plastic contact deformation

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Near net shape titanium and nickel matrix composites can be produced by the hot isostatic/vacuum hot pressing of alloy coated ceramic fibers. During the initial stage of consolidation, densification occurs by the inelastic deformation of metal–metal contacts. When the temperature is low and the consolidation pressure is high, the dominant mechanism of contact deformation is matrix plasticity. This densification process is investigated by analysing the elastic–plastic contact deformation of aligned fibers. Applying a methodology developed for modelling the consolidation of alloy powders and spray deposited composite monotapes, a contact yield criterion has been proposed and used to predict the dependence of the relative density upon process conditions and matrix mechanical properties. The resulting densification model contains unknown plastic flow ($F$) and contact area evolution ($c$) coefficients. A deformation theory elastic–plastic finite element analysis of a representative coated fiber unit cell loaded in compression is used to find these coefficients. The analysis shows the ceramic fiber significantly constrains plasticity in the alloy coating resulting in fiber volume fraction dependent coefficients and a fiber volume fraction dependent density–pressure relationship for coated fiber consolidation. © 1997 Elsevier Science Limited

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INTRODUCTION

Metal matrix composites (MMCs) consisting of titanium or nickel alloy matrices, reinforced with continuous alumina or silicon carbide monofilament fibers have attracted interest because of their relatively high specific stiffness, strength and creep resistance. They can be manufactured by the hot isostatic or vacuum hot pressing (HIP/VHP) of a preform layup. In the past, many processing methods have been pursued for the production of these preforms including the powder cloth approach and its continuous tape casting analogue, the foil–fiber–foil method and plasma spray deposition. Recently, interest in an alternative, physical vapor deposition process for composite preform fabrication has developed. In this approach, the matrix alloy is first evaporated using either electron beam heating or sputtering, and is then condensed directly onto the fibers. If the fibers are rotated during deposition, a (more or less uniformly) metal coated ceramic fiber like that shown in Figure 1 results.

A composite component can be manufactured from metal coated precursors by either winding the coated fiber on a mandrel, or packing them into a preshaped tool. These preform laytups are then sealed inside a metal canister and subjected to a high temperature consolidation process such as hot isostatic or vacuum hot pressing, or perhaps roll bonding. Contact deformation induced by the high applied pressure eliminates the interfiber porosity while the high temperature facilitates diffusion bonding of the constituents forming a fully dense near-net shape composite component.

The consolidation process can significantly affect the performance of the component. It must result in complete densification whilst ensuring minimal mechanical damage to the fibers (e.g. by bending at fiber crossovers, or longitudinal matrix flow along the fibers) and chemical reaction between the fiber and matrix alloy. Failure to accomplish this can result in a loss of composite strength, creep rupture life, fracture toughness and fatigue resistance. To avoid such damage during the consolidation of spray deposited preforms, it has been very helpful to model the consolidation process and use process simulation to design an optimal process schedule. Basically, models of this type attempt to describe the micromechanisms of densification, fiber damage, and fiber–matrix reaction in a way that enables prediction of the effects of fabrication pressure, temperature and time upon the relative density, the extent of fiber microbending/fracture and the reaction layer thickness.

Following the approach of Wilkinson and Ashby, it has usually proven sufficient to analyze the deformation of two geometries; inter-powder particle or inter-monotape asperity contacts established at the start of compaction (so-called Stage I densification) and the collapse of isolated voids as full density is approached (Stage II). During powder or spray deposited monotape consolidation, the Stage I geometry characterizes the situation encountered from the
beginning of consolidation up to a relative density of about 85%; the latter from about 95% to full density. For both geometries, the models analyze the contributions from plasticity (the time independent component of deformation), power law creep, and diffusional flow and obtain the total response by summing the individual contributions of the three mechanisms acting on the two geometries.

When the consolidation temperature is low and the contact stress is high (i.e. during initial loading or when high consolidation pressures are used to either increase the densification rate or reduce the opportunity for chemical reactions between the fiber and matrix), plasticity is the dominant mechanism of densification. Relationships between relative density, applied pressure and the matrix yield strength have been developed for Stage I consolidation by analysing the inelastic flow at representative contacts \( 2.23 \). For monotapes the resulting model contained two important coefficients, \( F \) and \( c \), identified by Bower et al. \( 24 \) for inelastic indentation and by Gampala et al. \( 23 \) for asperity blunting. The flow coefficient, \( F \), characterized the constraint of the matrix to inelastic flow near the contact while \( c \) related the contact area to the contact’s deformation.

Expressions for the flow coefficient have been obtained for homogeneous (unreinforced) contacts using the slip line field method (i.e. assuming rigid–perfectly plastic behavior) \( 25-28 \). If the contact strain dependence of the constraint is ignored, this approach gives the well known value of 2.97 for \( F \) during indentation and blunting. However, the large shape changes incurred during asperity blunting results in a loss of the constraint provided by surrounding elastic material. Recent finite element calculations show that \( F \) then decreases towards unity (i.e. the value for an unrestrained parallel sided sample loaded in uniaxial compression) as contact blunting (and densification) progresses. Eventually, lateral constraints imposed by adjacent contacts establish a new source of constraint and the deformation resistance of the contact then rapidly rises as the body approaches the incompressible limit at full density.

Here, we extend this modelling approach to examine the Stage I consolidation of metal coated fibers by deformation theory plasticity. Expressions for \( F \) and \( c \) as a function of either contact strain (or center-to-center displacement), fiber volume fraction and matrix yield/work hardening properties are obtained using a finite element method. It will be shown that the presence of a rigid, very strong ceramic fiber has a significant effect upon the contact yielding of the metal coating. The expressions obtained for \( F \) and \( c \) are then inserted into a simple Stage I densification model and used to predict the relationship between relative density and applied pressure, the matrix material’s yield strength/work hardening rate and the fiber volume fraction. Future work will investigate the more complex case of creep densification and the significance of lateral constraint created by multiple contacts.

**DENSIFICATION MODEL**

A coated fiber preform typically contains a variety of packing geometries. When uniformly coated fibers are carefully wound on a mandrel, a close packed hexagonal array with an initial relative density of 0.906 can be obtained. However, when fibers are placed in a die, the local packing usually varies from place to place (i.e. random packing) and includes regions of hexagonal, square and triangular packing. For a random packing of aligned coated fibers, the number of contacts made with a fiber (i.e. the coordination number) is initially low (\( < 4 \)) and increases with density towards a value near 6. Figure 2 shows the cross section of a partially consolidated composite fabricated from silicon carbide fibers sputter coated with a titanium alloy (similar to that shown in Figure 1) and subjected to constrained uniaxial compression in a VHP.
The area shown corresponds to a region of nearly square packing though the die was initially randomly packed.

To illustrate the modeling approach, consider a square array of coated fibers, subjected to a uniaxial pressure, $P$, Figure 3. If the sides of the preform are not constrained, lateral fiber expansion can occur and is analogous to the situation encountered during the early stages of densification by VHP before the fibers have felt the affect of the die side walls through lateral fiber-fiber contacts. In this case, the average initial coordination number of a random packing within the die would be low (i.e. around 4), and the contacts formed perpendicularly to the loading direction are subject to little lateral constraint (and can be ignored). It is also similar to the case encountered during lubricated roll bonding where, again, there is no physical barrier to lateral fiber preform expansion (other than friction with the rolls).

If a constant pressure, $P$, is applied to the preform, the force acting on each of the contacts will be invariant throughout the deformation process. For a square fiber array, Figure 3, the contact force per unit length of fiber $f_c$ balances the applied force. Thus,

$$f_c = 2P_0$$

(1)

where $r_0$ is the radius of the matrix coated fiber. Each fiber is seen to form two large contacts normal to the compaction direction resulting in columns made up of contacting fibers that support the applied pressure. Each of these normal contacts will be subjected to an identical contact force, $f_c$, normal to the plane of contact.

As the applied pressure increases, the matrix initially deforms elastically (see 29 for an analysis of the elastic composite cylinder's contact problem). Plastic yielding occurs once the contact stress, $\sigma_C$, equals or exceeds the contact's flow stress. By analogy with asperity contact deformation 23, we write a yield criterion for the contact in the form

$$\sigma_C = \frac{f_c}{a_c} = F\sigma_y$$

(2)

where $a_c$ is the contact length, $F$ is a plastic flow coefficient and $\sigma_y$ is the matrix material's uniaxial yield strength.

Classical studies of contact yielding for homogeneous rigid–perfectly plastic materials were begun in the early part of this century by Prandtl 25 and continued by Hill 26, Tabor 27, and Johnson 30. They have led to the well known result, $F = \sigma_y/\sigma_\varepsilon = 2.97$, for a homogeneous rigid–perfectly plastic body with a Mises constitutive law. More recently, Gampala et al. 22,23 using the finite element method and
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(a) Initial geometry

Frictionless contacts

\[ v = r_0 \]

Figure 3 Square array of coated fibers with representative volume element

(b) During consolidation

\[ v = r_0 + w \]

Akisanya et al. using slip line field theory have investigated the large strain blunting of hemispherical asperities and homogeneous cylinders, respectively. They determined that initially the flow coefficient was \( \sim 3 \), but rapidly dropped to \( \sim 2 \) with continued deformation. This occurs because as the deformation progresses the region of plasticity is constrained by a decreasing volume of elastic material in these finite bodies. The coefficient for metal coated fibers is also likely to exhibit analogous behavior and will thus be a function of the relative density (or equivalently the normal displacement imposed on the contact).

As the deformation increases, the contact width, \( a_c \), grows. Following the work of Bower et al. and Gampala et al., we propose that for the two-dimensional contact of aligned coated fibers, the contact area per unit length (i.e. contact width), dependence upon the normal displacement, \( h \), can be written in the form;

\[ h = \frac{1}{2} \frac{a^2}{c^2 r_0} \]  

where \( a \) is half of the contact length \( (a_c) \), \( h \) is the normal displacement, \( r_0 \) is the initial coating radius and \( c \) is the contact area coefficient. An area coefficient, \( c \), equal to unity arises if the contact length equals the length of the chord created when a circle is truncated by \( h \), Figure 3(b). In early modeling studies a uniform redistribution concept was used to obtain an expression for the contact area dependence on densification. Applied to this problem, it gives a value of \( c \approx 1.2 \). However, during the contact blunting of an asperity, experiments and finite element analyses both show that material displaced from the contact often piles up at the periphery of the contact and is not uniformly distributed.
As the contact area grows and the contact pressure drops below that required for continued plastic yielding (as determined by equation (2)), no further deformation can occur at that applied pressure. Thus, if the contact width and the flow coefficient are known as a function of the normal displacement, rearrangement of equations (1), (3) and (4) results in a simple expression for the pressure needed to compress a preform by an amount, \( h \):

\[
\frac{P}{\sigma_y} = F(h) \left[ \frac{h}{r_0} \right]^{\frac{1}{2}}
\]

Equation (4) defines the applied pressure, \( P \), required to cause a square array of metal coated fibers to undergo a displacement, \( h \), if the contact material has a yield strength, \( \sigma_y \). It establishes a relationship between densification and the pressure variable of the consolidation process. The effect of processing temperature enters through the temperature dependence of \( \sigma_y \).

Once the expression for \( h \) (equation (4)), is evaluated, the relative density can be simply computed for the fiber array. For example, for an unconstrained square array of metal coated fibers, the relative density, \( D \), is related to the normal displacement contact, \( h \), by:

\[
D = \frac{\pi}{4} \left( 1 + \frac{w}{r_0} \right) \left( 1 - \frac{h}{r_0} \right)
\]

where \( w \) is the change in lateral width of a unit cell in the array, Figure 3(b). It is shown below that \( w \) is only non-zero for homogeneous cylinders. It is very small when a well bonded ceramic fiber is present in a composite cylinder with a fiber volume fraction of 0.25 and above.

**FEM ANALYSIS**

If equations (4) and (5) are to be used to predict a density–processing relationship, the dependence of both the flow and area coefficients on volume fraction of fiber and the extent of deformation, \( h \), needs to be determined. Here, a finite element method is used to obtain \( F \) and \( c \).

The FEM problem we consider is the plane strain deformation of a composite cylinder consisting of a central core that behaves elastically and an outer cylindrical shell with either linear elastic–perfectly plastic or linear elastic–linear plastic hardening constitutive behavior. It is assumed that the fiber–matrix interface is perfectly bonded. Thus, continuity of displacement and tractions are assumed at the interface. Initially, the semicircular surface of the matrix is taken to be stress free and to make a frictionless diametrical contact with the neighboring composite cylinders. Because of the problem’s symmetry, the blunting of only a plane circular quadrant by a rigid flat plate needs to be analyzed (Figure 3(a)).

After a series of convergence tests with different types and sizes of finite element meshes, the unit cell was divided into a mesh consisting of 900 rectangular, second order isoparametric elements with 2800 nodes. Second order interface elements were used to model the line contact that developed between the surface of the cylinder and the rigid plate. Symmetry of the finite element mesh was maintained by constraining the vertical and horizontal centerlines of the coated fiber to remain straight. Thus, the horizontal diametral line defining the upper surface of the cell was constrained to remain horizontal as a uniform applied displacement, \( h \), was applied.

The ceramic fiber was modeled as an isotropic linear elastic material. The outer metal layer was modeled as an isotropic perfectly–plastic or bilinear strain-hardening material (i.e. \( \sigma = \sigma_y + k e^p \) where \( e^p \) is the plastic strain and \( k \) the work hardening rate). Yielding of the outer layer was assumed to be governed by the Mises condition,

\[
\sqrt{(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2} = \sigma = \sigma_y
\]

where \( \sigma_y \) is the matrix material’s uniaxial yield stress and \( \sigma_j \), \( j = 1,3 \) are the principal stresses (in Voigt notation).

The governing stress–strain response of the outer layer material in incremental form was taken to be,

\[
d\sigma^p = d\lambda \frac{\partial g}{\partial \sigma_{ij}}
\]

where \( d\sigma^p \) is the plastic strain increment, \( d\lambda \), is a non-negative constant and \( g(\sigma) \) is the plastic “associated flow” potential. For the Mises flow potential used here, \( g(\sigma) \) is given in terms of the principal stresses by,

\[
g(\sigma) = f(\sigma) = \sum (\sigma_i - \sigma_j)^2
\]

where \( f(\sigma) \) is the Von Mises associated flow rule for an isotropic material.

Detailed calculations have been conducted for representative metal-coated ceramic fibers with ceramic fiber fractions of 0, 0.25 and 0.49. The metal coating has been given the elastic and plastic properties of Ti–6Al–4V whilst the (ceramic) fiber was assigned properties similar to those of SiC (Table 1).

Table 1 Material properties used in FEM analysis

<table>
<thead>
<tr>
<th>Material</th>
<th>Young’s Modulus (GPa)</th>
<th>Poisson’s Ratio</th>
<th>Uniaxial Yield Strength (Mpa)</th>
<th>Work Hardening Rate (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ti–6Al–4V</td>
<td>110</td>
<td>0.324</td>
<td>840</td>
<td>775</td>
</tr>
<tr>
<td>SiC</td>
<td>390</td>
<td>0.240</td>
<td></td>
<td></td>
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</table>
agreed well with other studies of elastic composite cylinder contact \([29]\). As the applied displacement increased, the metal coating began to flow plastically just above the center of the contact. This occurred when the contact pressure was about 1.1 \(\sigma_y\). As the applied displacement was increased, this region of plasticity spread, eventually intersecting the surface of the cylinders. Figure 4 shows Mises stress contours for the three fiber volume fractions after normalized displacements, \(h/r_0\), of 0.0154, 0.0462, and 0.0769. For the homogeneous case, Figure 4(a), the plastic zone had already reached the edge of the contact surface for the \(h/r_0 = 0.0154\); however, the center of the contact remained elastic. As the displacement increased, the plastic zone expanded vertically to the horizontal centerline of the fiber.
and more gradually spread laterally. Figure 4(a) shows that even after a large normalized displacement of 0.0769, the plastic zone of a homogeneous cylinder was still laterally constrained by a shell of elastically deforming material. Figure 4(a) also shows a rapid growth of contact width as \( h/r_0 \) increased.

A small but significant lateral spreading of the cell also accompanied the uniaxial compression of the homogeneous cylinder. The finite element calculated width change depended upon \( h \) and was well fitted by:

\[
\frac{w}{r_0} = 0.002 \left( \frac{h}{r_0} \right) + 4.38 \left( \frac{h}{r_0} \right)^2 \quad \text{for} \quad 0 \leq \frac{h}{r_0} \leq 0.08 \quad (9)
\]

The Mises stress contours for a 0.25 ceramic fiber volume fraction cell are shown in Figure 4(b) for identical normalized displacements to the homogeneous case above. Plasticity was again seen to be initiated just above the contact. As the applied displacement increased, the plastic zone rapidly propagated upwards to the fiber–matrix interface. After encountering the fiber, it extended through the annular metal coating and eventually reached the horizontal centerline of the fiber. Small elastically deforming regions of the metal coating remained near the ceramic/metal interface at the top of the cell and on the periphery of the coating even after the largest normalized displacement, \((h/r_0 = 0.0769)\). No detectable lateral spreading of the cell was observed, even for the largest normalized displacements.

Figure 4(c) shows the Mises stress contours for the 0.49 ceramic fiber fraction case. The fiber had an even more pronounced effect upon the plastic flow of the metal coating. The plastic zone more rapidly reached the fiber–matrix interface during deformation consistent with the higher concentration of stress in the matrix at the fiber matrix interface. This plastic zone then began to extend around the annular metal ring. However, this was again inhibited by the constraint of the well bonded fiber and resulted in stresses within the plastic zone that were significantly greater than the uniaxial yield strength of the matrix. A larger volume of the coating was observed to remain elastic for the higher fiber fraction case.

**Flow coefficient, \( F \)**

The flow coefficient, \( F(h) \), defines the magnitude of the contact pressure required to cause inelastic contact deformation. It is a function of the contact’s normalized displacement and can be found from the analysis of fiber contact. First, the area of contact and the contact pressure were found for a prescribed vertical displacement, \( h \), applied to the unit cell (Figure 3). Since the uniaxial flow stress (an input to the model) was known, equation (2) could then be used to obtain \( F(h) \).

The flow coefficient’s dependence upon normalized displacement for the three fiber volume fractions analyzed is shown in Figure 5 for the elastic–perfectly plastic matrix and in Figure 6 for the elastic work hardening matrix. During the initial elastic contact, the flow coefficient of the three samples rose rapidly towards a value of 2. For the homogeneous cylinders, \( F \) reached a value of about 2.2 and then decreased with normalized displacement as the plastic zone reached the contact. For the perfectly plastic composite samples, \( F \) ceased to significantly increase once the plastic zone reached the outer boundary of the metal shell \((h/r_0 \sim 0.015)\). Further deformation resulted in a slight increase in \( F \). Once the fully plastic behavior was established, the flow coefficient increased with fiber fraction.

The lower flow coefficient of \( \sim 2 \) observed here for the onset of fully plastic flow versus \( \sim 3 \) from the slip line analysis of Akisanya et al. may be a consequence of the two-dimensional (plane strain) cylindrical contact analyzed. Akisanya et al. assumed that the small size of the contact relative to the cylinder diameter justified using the infinite plate result for \( F \) which has a significantly higher constraint because of the need for the slip lines to reach the more remote free surface. The FEM results indicate that by the point at which fully plastic flow is established in a cylindrical contact \((h/r_0 \sim 0.01)\), the contact length is too large (compared to the cylinder’s radius) for this to be a
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Table 2  Results for the flow coefficient dependence \( F = m h r_0 + b \)

<table>
<thead>
<tr>
<th>Fiber volume fraction</th>
<th>Perfectly plastic matrix</th>
<th>Work hardening matrix</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>( m )</td>
<td>( b )</td>
</tr>
<tr>
<td>0.0</td>
<td>-7.7</td>
<td>2.3</td>
</tr>
<tr>
<td>0.25</td>
<td>1.5</td>
<td>2.2</td>
</tr>
<tr>
<td>0.49</td>
<td>1.5</td>
<td>2.5</td>
</tr>
</tbody>
</table>

The area coefficient, \( c \)

The area coefficient, \( c \), was calculated for each applied displacement, \( h \), by determining the contact length (from finite element results) and plotting \( a/r_0 \) against \( \sqrt{2h - l_0} \). Figure 7. The slope of this relationship gives the area coefficient, \( c \). The area coefficient also appeared to be independent of the matrix work hardening relationship as seen in the values of \( c \) shown in Table 3 for the six cases studied.

The values for \( c \) were found to be always less than unity consistent with a contact length smaller than the chord length of a circle truncated by an amount, \( h \). This phenomenon has been referred to as 'sinking-in' of the contact 31 and contrasts with results for spherical contacts where the contacts exhibit \( c > 1 \) and matrix 'piles-up' at the periphery of the contact 23. Values of \( c < 1 \) for fibers arise because of the relative ease of lateral spreading in regions remote from the contact. This is retarded for spherical symmetric contacts by elastic hoop stresses 22. The presence of an elastically deforming ceramic fiber within a composite cylinder also inhibits the lateral expansion and accounts for the approach of \( c \) towards unity as the fiber fraction increases. Finally, we note that a uniform redistribution calculation (like that used in earlier models of particle contact 21,32,33) results in a value of \( c = 1.12 \) for a square fiber array; a significant overestimate of the growth rate of the contact. Other researchers 22 have found the opposite to be true for the blunting of homogeneous spherical contacts. The difference lies in the fact that our analysis is for a two-dimensional contact as...
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Table 3 Values for the Area Coefficient, $c = \frac{a}{2\sqrt{2hr_0}}$

<table>
<thead>
<tr>
<th>Volume fraction of fiber</th>
<th>Perfectly plastic matrix</th>
<th>Work hardening matrix</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>0.84</td>
<td>0.82</td>
</tr>
<tr>
<td>0.25</td>
<td>0.90</td>
<td>0.89</td>
</tr>
<tr>
<td>0.49</td>
<td>0.96</td>
<td>0.95</td>
</tr>
</tbody>
</table>

opposed to the three-dimensional case studied by others. The uniform redistribution model overestimates the area of the contact compared to the finite element solution and therefore overestimates the applied pressure needed to achieve a desired density.

DENSITY PREDICTIONS

Using equations (4) and (5) and the relationships developed above for the flow and area coefficients, one can determine the pressure needed to achieve a prescribed vertical displacement and thus density. Because $F(h)$ and $c$ depend on fiber fraction, we can use the resulting model to assess the effects of varying the volume fraction of fiber upon the densification process. This is shown for the fiber volume fractions used in the finite element calculations in Figure 8. Additional results for intermediate fiber fractions were obtained by linearly interpolating the dependence of $F(h)$ and $c$ upon the fiber fraction. The maximum normalized displacement imposed for the finite element analysis, $h/r_0 = 0.08$, corresponded to a final density of 0.83 for the unreinforced case and 0.85 for the reinforced case because of the larger lateral spreading of the homogeneous fiber.

These results show that the density of the homogeneous cylinder rises more rapidly with pressure than for the reinforced cases; it is a consequence of deformation softening associated with the loss of constraint, Figure 5. The additional constraint provided by a fully bonded elastic fiber/metal matrix interface eliminates this "softening" and results in a fiber fraction dependent densification response.

The effect of the matrices' work hardening rate upon densification is relatively significant for a composite cylinder, Figure 9, because of the larger plastic strain within the more localized contact deformation field of a composite cylinder. Figure 9 also shows the more significant consequences of using a uniform redistribution estimate for $c = 1.12$ as opposed to the finite element calculated case. The increase in the pressure needed to achieve a prescribed density is a direct consequence of the increased contact area (and thus reduced contact pressure). Taken together, the results of Figures 8 and 9 show a strong influence of fiber fraction upon the densification of metal coated fibers and indicate that it will be important to account for this phenomenon when attempting to predict the observed densification response of these materials.

SUMMARY

A finite element analysis has been used to examine the elastic–plastic contact deformation of metal coated fibers. The evolution of the contact area and stress fields were obtained as a function of normalized displacement, and from this the contact's flow and area coefficients were calculated as a function of the volume fraction of fiber and the matrix work hardening rate. The presence of well bonded elastic fibers in a composite cylinder was found to significantly inhibit the inelastic flow of the matrix, resulting in an increased flow coefficient for a given density or normalized displacement. The presence of matrix strain hardening also increased the flow coefficient of the composite cylinder contacts. The area coefficient was less than unity and independent of the normalized contact length and the work hardening relationship. While the area coefficient increased with fiber fraction, it never exceeded unity. The low values of $c$ compared to spherical contact results arose from a reduced resistance to lateral spreading of the unit cell analyzed. These results have been used to predict the densification–pressure relationships for a square array of metal coated fibers. A strong fiber fraction effect is predicted.
ACKNOWLEDGEMENTS

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