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**OAK RIDGE
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**Interference of Wedge-Shaped Protrusions
On the Faces of a
Griffith Crack in Biaxial Stress**

Final Report

J. A. M. Boulet

**Prepared by
The University of Tennessee
Knoxville, Tennessee 37996-2030**

**CERAMIC TECHNOLOGY FOR
ADVANCED HEAT ENGINES**

**MANAGED BY
MARTIN MARIETTA ENERGY SYSTEMS, INC.
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DEPARTMENT OF ENERGY**

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INTERFERENCE OF WEDGE-SHAPED PROTRUSIONS
ON THE FACES OF A
GRIFFITH CRACK IN BIAxIAL STRESS

J. A. M. Boulet

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INTERFERENCE OF WEDGE-SHAPED PROTRUSIONS ON THE FACES OF A GRIFFITH CRACK IN BIAXIAL STRESS

J. A. M. Boulet

ABSTRACT

An initial investigation of the influence of protrusion interference on the fracture toughness required to prevent unstable propagation of a Griffith crack in a brittle material is described. The interference is caused by relative shear displacement of the crack faces when subjected to remote biaxial stress with neither principal stress parallel to the crack. It is shown that for room temperature cracks smaller than about one centimeter in silicon carbide, or about one millimeter in silicon nitride, the presence of interference changes the fracture stress. A mathematical model based on linear elasticity solutions and including multiple interference sites at arbitrarily specified positions on the crack is presented. Computations of the change in required fracture toughness and its dependence on wedge geometry (size and vertex angle), applied stresses (orientation and magnitude), and location of the interference site are discussed. Results indicate that a single interference site has only a slight effect on required toughness. However, the influence of interference increases monotonically with the number of interference sites. The two-dimensional model described herein is not accurate when the interference sites are closely spaced.

INTRODUCTION

When a Griffith (line) crack is subjected to arbitrarily oriented principal stresses applied at infinity, the stress intensity factors at the crack tips (ends) may be combined in any of several fracture criteria to predict the fracture stress (the level of applied stress required to initiate unstable crack propagation). In the classical analysis of this problem¹, the crack faces do not interact with each other. But for real cracks, various kinds of mechanical crack-face interaction can occur. Under compressive stresses, contact of the faces can produce pressure along the entire crack. Under biaxial tension with the crack not parallel to either principal direction, relative shear displacement of the faces can cause the protrusions on rough faces to interfere with each other, and so generate interaction forces. For fibrous composites, fibers that bridge the crack can mediate interaction between the faces. Any crack-face interaction changes the stress intensity factors from what they would be if the interaction were not present. Consequently, the predicted fracture stress and the fracture toughness required to avoid unstable crack propagation are changed. The amount of change is a direct measure of the importance of any kind of crack-face interaction.

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CONCENTRATED LOAD MODEL

The problem considered herein is that of predicting the initiation of unstable propagation of a Griffith crack subjected to arbitrary biaxial stress at infinity, with crack-face interaction represented by concentrated loads. The use of concentrated loads is appropriate when interaction takes place in regions very small relative to the crack size.

Concentrated loads are not the only reasonable model for crack-face interaction. Another approach to this problem has recently been reported in several investigations of fracture in ceramic composites²⁻⁴. There, interaction is modeled as traction distributed continuously over some region of the crack faces. The latter model admits a Green's function formulation that leads to a nonlinear integral (or differential) equation. With the former model, as described below, the Green's function formalism is unnecessary and the governing equations lead directly to a set of simultaneous, nonlinear algebraic equations. Thus, the concentrated loads model leads to the simpler computational problem.

The concentrated loads representing interaction of crack faces must be found before their influence on crack propagation can be assessed. This can be done with an iteration scheme based on compatibility of displacements for two problems, referred to hereafter as problems A and B, in the linear theory of elasticity. (The assumption of linearity is appropriate for analysis of brittle fracture.) Problem A is a line crack subjected simultaneously to arbitrarily oriented biaxial stress at infinity and concentrated loads on the crack faces. Problem B is a half-plane with interaction sites on the boundary subjected to concentrated loads statically equivalent to those used in problem A. From the solution to each problem one can compute the displacement of the upper crack face relative to that of the lower crack face. The correct set of interaction loads gives equal (compatible) relative displacements for both problems. We assume that the set of interaction forces that gives compatible displacements is unique.

FRACTURE CRITERION

Once interaction loads are found, their effect on crack propagation can be evaluated using any of various fracture criteria. For our initial investigation, we choose the Griffith fracture criterion for biaxial stress written in terms of stress intensity factors⁵⁻⁶

$$K_I^2 + K_{II}^2 = K_{IC}^2 \quad (1)$$

Using the crack half-length, a , and the material's shear modulus, μ , we define the following normalized versions of the quantities in (1).

$$K_I^* = \frac{K_I}{\mu \sqrt{\pi a}} \quad (2)$$

$$K_{II}^* = \frac{K_{II}}{\mu \sqrt{\pi a}} \quad (3)$$

$$K_{IC}^* = \frac{K_{IC}}{\mu \sqrt{\pi a}} \quad (4)$$

We also define the normalized applied stress as

$$\sigma^* = \frac{\sigma_1}{\mu} \quad (5)$$

and the biaxial stress ratio as

$$\rho = \frac{\sigma_2}{\sigma_1} \quad (6)$$

where σ_1 and σ_2 are, respectively, the larger and smaller principal stresses at infinity.

PROTRUSION INTERFERENCE IN SHEAR

As indicated above, problem A is that of a line (Griffith) crack subjected to remote biaxial stress and crack-face interaction loads. Depending on their signs and orientation, the applied stresses tend either to open or to close the crack. Also, when the crack is aligned with neither stress, the stresses tend to shear the crack (slide the opposing faces parallel to themselves, but in opposite directions). Unless the crack is opened enough so that protrusions on the opposing faces are not interleaved, shearing may cause the protrusions to interfere with each other. The specific kind of interaction we examine herein is the interference of protrusions on crack faces during relative shear displacement of the faces.

For our initial investigation, we suppose that the crack is opened enough so that the faces are in contact at only one point (the site of the tallest bumps) and that the interference site is small compared with the crack size. Problem A is then represented by Figure 1, where the interference site is shown at $x = b$. (For multiple interference sites, Figure 1 would show concentrated loads at several locations.) We also assume that the protrusions are wedge-shaped. The wedge geometry is chosen for mathematical convenience rather than for realism. (A more realistic geometry is discussed at the end of this report.) Our model of the interference region is pictured in Figure 2.

When protrusions on opposing crack faces come into contact with each other, they deform elastically. For wedge-shaped bumps, the deformation is essentially bending, as is illustrated qualitatively in Figure 3. Consequently, contact occurs over a finite region, the contact patch, whose size is small relative to other dimensions of the problem. In general, the contact

Figure 1. Griffith Crack Subjected Simultaneously to Arbitrarily Oriented Biaxial Stress at Infinity and Concentrated Loads Representing Interaction at $x = b$. The angle β is referred to later as the stress orientation angle.

Figure 2. Interference Between Wedge-Shaped Bumps on the Faces of a Griffith Crack.

Figure 3. Exaggerated Bending of Wedge-Shaped Protrusions During Interference Due to Relative Shear Displacement of Crack Faces.

pressure is not uniform over the patch. Analysis of the problem is greatly simplified if we assume that the contact patch is essentially a point, so that the pressure distributed over the patch is replaced by a point load (concentrated force per unit length normal to the xy plane), P , as shown in Figure 4. Except for a small region in the immediate vicinity of the point where the force is applied, the stress field associated with this simpler problem will be the same as that for the more realistic problem. The difference near the force is not important for the present study, because the stresses that produce fracture are those at the crack tip, which is outside the wedge. Thus, as illustrated in Figure 4, problem B is a half-plane with a wedge-shaped bump on the boundary subjected to a concentrated force normal to one face of the wedge.

As mentioned above, we assume that the interference region is much smaller than the crack. Far from this region, the effect of the interference is the same as if the wedges were represented by the concentrated loads shown in Figure 5. These are the concentrated loads that appear in problem A at $x = b$ (Figure 1).

FRACTURE CRITERION

Let the number of interference sites be " n ". We define the normalized contact forces at the sites as

$$P_i^* = \frac{P_i}{\mu a}, \quad i = 1, 2, \dots, n \quad (7)$$

Figure 4. Problem B: Concentrated Normal Force, P , Applied to One Side of a Wedge-Shaped Bump on a Half-Plane.

Figure 5. Concentrated Loads Staticaly Equivalent to the Stresses on the Line $y = h$, $|x| < h \tan \alpha$ in Fig. 4. These are the loads applied at $x = b$ in Figure 1.

where P_i is the contact force at site "i." When the contact force is present, the stress intensity factors for the right-hand crack tip ($x = a$) can be derived from published results⁷⁻⁸. They are

$$K_I^* = (K_I^*)_0 (1 + \Delta_I) \quad (8)$$

$$K_{II}^* = (K_{II}^*)_0 (1 - \Delta_{II}) \quad (9)$$

where

$$(K_I^*)_0 = D_I \sigma^* \quad (10)$$

$$(K_{II}^*)_0 = D_{II} \sigma^* \quad (11)$$

$$D_I = \cos^2 \beta + \rho \sin^2 \beta \quad (12)$$

$$D_{II} = (1 + \rho) \sin \beta \cos \beta \quad (13)$$

$$\Delta_I = \frac{\sin \alpha_i}{\pi D_I} \sum_{i=1}^n \sqrt{\frac{1 + S_i}{1 - S_i}} \frac{P_i^*}{\sigma^*} \quad (14)$$

$$\Delta_{II} = \frac{\sin \alpha_i}{\pi D_{II}} \sum_{i=1}^n \sqrt{\frac{1 + S_i}{1 - S_i}} \frac{P_i^*}{\sigma^*} \quad (15)$$

$$S_i = \frac{b_i}{a} \quad (16)$$

where the stress ratio, ρ , is defined in (6), the stress orientation angle, β , is defined in Figure 1, and α_i (Figure 4) and b_i (Figure 1) are the wedge half-angle and position for site "i." Equations for the left-hand crack tip ($x = -a$) are very similar to those shown here. Thus, the fracture criterion of (1) gives

$$\sigma_F^* f(\sigma_F^*) = K_{IC}^* \quad (17)$$

$$f(\sigma^*) = \sqrt{[D_I(1 + \Delta_I)]^2 + [D_{II}(1 - \Delta_{II})]^2} \quad (18)$$

In the absence of contact, this reduces to

$$\sigma_0^* f_0 = K_{IC}^* \quad (19)$$

$$f_0 = \sqrt{D_I^2 + D_{II}^2} \quad (20)$$

A convenient measure of the significance of the interference is

$$R = \frac{f_0}{f(\sigma^*)} \quad (21)$$

Physically, R is (for given applied stresses, crack size, wedge geometry, and wedge location) the ratio of the fracture toughness required in the absence of interference to the fracture toughness required in the presence of interference.

COMPATIBILITY CONDITION

As discussed previously, the correct values of P_i^* are those that give compatible displacements for problems A and B at all sites. To find these, we must first establish the compatibility condition precisely. Let us define displacements u and v in the x and y directions, respectively (Figures 1 and 4). Displacements of the upper crack face relative to those of the lower crack face are

$$\Delta u = (u_{y=0^+}) - (u_{y=0^-}) \quad (22)$$

$$\Delta v = (v_{y=0^+}) - (v_{y=0^-}) \quad (23)$$

For these relative displacements to exist in problem A, the wedge bending displacement shown in Figure 3 must be (see Figures 2 and 4)

$$\delta_A = \Delta u - \Delta v \tan \alpha \quad (24)$$

For problem B (Figure 4), we take the wedge bending to be the difference between the centerline x -direction displacements at the y coordinate of the contact force and at the base of the wedge ($y = h$). Hence, we have

$$\delta_B = 2(u_P - u_O) \quad (25)$$

For compatibility, P_i^* must be such that at each pair of wedges

$$\delta_A^* = \delta_B^* \quad (26)$$

where the asterisk denotes dimensionless displacements normalized with respect to the crack half-length. To apply this condition, we must evaluate δ_A^* at some point in the immediate vicinity of the concentrated loads representing the wedges. We choose to do so at

$$x_i = b_i + h \tan \alpha_i, \quad i = 1, 2, \dots, n \quad (27)$$

which is as close to the wedges as possible without being inside them. (It would be inappropriate to evaluate δ_A^* inside the "point" where the concentrated loads act. Furthermore, δ_A^* is singular at that point.)

Before imposing the compatibility condition, (26), we must relate the contact force to the displacements in problems A and B. These relationships follow from elasticity solutions for the two problems.

SOLUTION FOR PROBLEM A

A solution for problem A can be constructed by superimposing solutions available from the literature for biaxial stress at infinity⁹ and for concentrated loads on the crack faces⁷. From these solutions, (27), and careful consideration of the limiting values obtained when approaching the crack from above or below, we find the load-displacement relationship at site "i" to be

$$\delta_A^* = A_1 - \sum_{j=1}^n A_{2j} P_j^* \quad (28)$$

where

$$A_1 = (C_1 - C_2 \tan \alpha_i) \sigma^* \quad (29)$$

$$A_{2j} = C_{3j} (\cos \alpha_j + \sin \alpha_j \tan \alpha_i) \quad (30)$$

$$C_1 = \frac{1}{2} (\kappa + 1) (1 - \rho) \sin \beta \cos \beta \sqrt{1 - (S_i + Q)^2} \quad (31)$$

$$C_2 = \frac{1}{2} (\kappa + 1) D_I \sqrt{1 - (S_i + Q)^2} \quad (32)$$

$$C_{3j} = \frac{\kappa + 1}{4\pi} \ln \left\{ \frac{1 - S_j (S_i + Q) + \sqrt{(1 - S_j^2) [1 - (S_i + Q)^2]}}{1 - S_j (S_i + Q) - \sqrt{(1 - S_j^2) [1 - (S_i + Q)^2]}} \right\} \quad (33)$$

$$Q = \frac{h_i}{a} \tan \alpha_i \quad (34)$$

$$\kappa = \begin{cases} 3 - 4\nu, & \text{plane strain} \\ \frac{3 - \nu}{1 + \nu}, & \text{plane stress} \end{cases} \quad (35)$$

SOLUTION FOR PROBLEM B

No solution to problem B is available from the literature. Equations presented herein for problem B are based on an original solution soon to be submitted for publication. We base our solution on the well-known complex variable formulation of the equations of elasticity⁹⁻¹¹. We first map the half-plane with a wedge bump to a half-plane using a degenerate Schwarz-Christoffel transformation with only three vertices not at infinity. Then we continue the (as yet unknown) Goursat potential functions analytically across the portions of the boundary on which no resultant force acts, and so produce a Hilbert problem that can easily be solved for the Goursat potential functions. From these functions, we can calculate the displacements for problem B. On the centerline, the normalized displacement in the x direction at site "i" is

$$u^*(\eta) = -P_i^* F(\eta) \quad (36)$$

where

$$F(\eta) = \frac{1}{4\pi} \left\{ \frac{2 A(\alpha, \eta) (\xi_P \sin \alpha_i - \eta \cos \alpha_i)}{(\xi_P^2 + \eta^2) \left(\frac{1 + \eta^2}{\eta^2} \right)^\gamma} - \sin \alpha_i (\kappa - 1) \left[\pi - \tan^{-1} \left(\frac{\eta}{\xi_P} \right) \right] - \frac{1}{2} \cos \alpha_i (\kappa + 1) \ln (\xi_P^2 + \eta^2) \right\} \quad (37)$$

$$A(\alpha_i, \eta) = \int_0^\eta \left(\frac{1 + s^2}{s^2} \right)^\gamma ds \quad (38)$$

$$B(\alpha_i, \xi) = \int_0^\xi \left(\frac{1 - s^2}{s^2} \right)^\gamma ds \quad (39)$$

$$\gamma = \frac{1}{2} - \frac{\alpha_i}{\pi} \quad (40)$$

and the value of ξ_P is found by iteration from

$$B(\alpha_i, \xi_P) = \frac{r_i}{h_i} \cos \alpha_i B(\alpha_i, 1) \quad (41)$$

The variable η is the imaginary part of the complex variable to whose plane the wedge geometry is mapped. From (26) and (37), we find the required load-displacement relationship

$$P_i^* = \frac{\delta_B^*}{2 [F(\eta_O) - F(\eta_P)]} \quad (42)$$

where the values of η_P and η_O are found by iteration from

$$A(\alpha_i, \eta_P) = \frac{r_i}{h_i} \cos^2 \alpha_i B(\alpha_i, 1) \quad (43)$$

$$A(\alpha_i, \eta_O) = \cos \alpha_i B(\alpha_i, 1) \quad (44)$$

Note that P_i^* depends on the crack opening displacement, Δv , through (41), (43), and (see Figures 2 and 4)

$$2 r_i \cos \alpha_i + \Delta v = h_i \quad (45)$$

CONDITIONS FOR INTERFERENCE TO OCCUR

Certain physical conditions must be satisfied if the applied stresses are to induce interference at a given pair of wedges. One of these is that the relative crack face shear displacement must be in the sense that moves the wedges toward, rather than away from, each other. Thus, from (24), Δu must exceed $\Delta v \tan \alpha$. This and (29) imply that $A_1 > 0$, which leads to the restrictions in Table 1, where

$$\phi = \frac{\tan(\beta - \alpha)}{\tan \beta} \quad (46)$$

Table 1. Restrictions on Stress Ratio. If these are not satisfied, interference does not occur. The quantity ϕ is defined in (46).

$\beta^* = \alpha - 90^\circ$	$\sigma_1 > 0$	$\sigma_1 < 0$
$0 \leq \beta \leq 90^\circ$	$\rho < \phi$	$\rho > \phi$
$\beta^* < \beta < 0$	$\rho > \phi$	$\rho < \phi$
$-90^\circ \leq \beta \leq \beta^*$	$\rho < \phi$	$\rho > \phi$

If the restrictions in Table 1 are not satisfied, the remote stress field does not bring the wedges into contact and interference does not occur.

The four possible combinations of tensile and compressive principal stress to which a two-dimensional crack may be subjected are labeled as cases I through IV in Table 2. The points (ρ, β) for which the various cases produce interference are illustrated in Fig. 6. In all cases except I, there are intervals of the stress orientation angle, β , for which ρ is unrestricted (every value of ρ produces interference). These intervals are listed in Table 3.

Table 2. Stress Cases. The four possible combinations of tensile and compressive principal stress are labeled in the rightmost column.

σ_1	σ_2	ρ	Case
> 0	≥ 0	≥ 0	I
> 0	≤ 0	≤ 0	II
< 0	≥ 0	≤ 0	III
< 0	≤ 0	≥ 0	IV

Figure 6. Regions in Which the Various Stress Cases Produce Interference. The curved lines are the quantity ϕ defined in (46). The lines $\beta = 0$ and $\beta = \beta^*$ are asymptotes ($\beta^* = \alpha - 90^\circ$). Between them, $\phi > 1$. The region labels follow from (46) and Tables 1 and 2. For each stress case, the ratio of the labeled area to π [the area corresponding to all possible points (ρ, β) for one case] is a measure of the likelihood that interference will occur. Thus, for the value of α used here ($\tan \alpha = 1/2$), the likelihood of having interference is much greater for biaxial compression (case IV) than it is for biaxial tension (case I).

Table 3. Intervals of Unrestricted Stress Ratio. In these intervals, every value of the stress ratio, ρ , produces interference.

Case	Interval
II	$\alpha < \beta < 90^\circ$
III	$-90^\circ + \frac{\alpha}{2} < \beta < \frac{\alpha}{2}$
IV	$-90^\circ < \beta < \alpha$

Dependence of the I - IV boundary (ϕ in the first quadrant of Figure 6) is illustrated in Figure 7. As the wedge half-angle, α , decreases, the region in which biaxial tension (case I) produces interference increases in size, while that for biaxial compression (case IV) decreases.

Figure 7. Dependence of Boundary between Stress Cases I and IV on Wedge Half-Angle, α . The region below the curve is stress case I, and that above the curve is case IV [see (46), Table 2, and Fig. 6]. As α decreases (wedge sharpens), the region in which biaxial tension (case I) produces interference grows, while that in which biaxial compression (case IV) produces interference shrinks.

The value of the contact force at site "i" is limited to the range

$$0 < P_i^* < \frac{A_1}{A_{2i}} \quad (47)$$

because the contact force is necessarily compressive (left-hand inequality) and cannot exceed what would be provided by a rigid wedge (right-hand inequality).

Another physical restriction that must be satisfied if interference is to occur at a potential interference site is that the crack opening displacement there must be small enough so that the protrusions overlap. This implies that the opening displacement not exceed the protrusion height, or

$$0 < \Delta v|_{x=b} < h \quad (48)$$

The left-hand inequality precludes interpenetration of the crack faces. To satisfy the right-hand inequality, the applied stresses must not be too large. (This restriction applies to all protrusions of height h , regardless of shape, as well as to cracks with "jogs" of length h .) The stress required to produce $\Delta v = h$ at $x = b$ is

$$\sigma_h^* = \frac{2h}{(\kappa + 1) D_I \sqrt{a^2 - b^2}} \quad (49)$$

Interference can occur only if $\sigma^* < \sigma_h^*$. For a given stress field and protrusion height, σ_h^* is smallest when the interference site is at the center of the crack ($b = 0$). It follows that interference can occur at the center, and therefore everywhere on the crack, only if

$$\sigma^* < \sigma_M^* \quad (50)$$

where

$$\sigma_M^* = \frac{2h}{(\kappa + 1) D_I a} \quad (51)$$

CONDITIONS FOR INTERFERENCE TO INFLUENCE FRACTURE

We now consider whether interference is expected to influence fracture. If the protrusions still overlap [See (48) and (50).] when the stress is raised to $\sigma^* = \sigma_0^*$ [the fracture stress in the absence of interference, (19)],

then any interference will change the fracture stress from σ_0^* to σ_F^* in (17).

Hence, the presence of any interference will influence fracture whenever

$$\sigma_M^* > \sigma_0^* \quad (52)$$

From (4), (19) and (20), we have

$$\sigma_0^* = \frac{K_{IC}}{\mu \sqrt{\pi a (D_I^2 + D_{II}^2)}} \quad (53)$$

Since σ_0^* is proportional to $a^{-1/2}$ and σ_M^* is proportional to $1/a$, it follows that for some crack half-length, say a_c ,

$$\sigma_M^* = \sigma_0^* \quad (54)$$

For all smaller cracks, σ_0^* is less than σ_M^* . Hence, for these cracks, the presence of interference implies a change in the fracture stress.

From (51), (53) and (54), we find the critical crack length

$$2 a_c = 2 \pi \left[\frac{2 \mu h}{(\kappa + 1) K_{IC}} \right]^2 \left[1 + \left(\frac{D_{II}}{D_I} \right)^2 \right] \quad (55)$$

The last term depends only on the stress field parameters ρ and β , and is strictly nonnegative. Hence the crack length below which any interference influences fracture satisfies

$$2 a_c \leq 2 \pi \left[\frac{2 \mu h}{(\kappa + 1) K_{IC}} \right]^2 \quad (56)$$

For plane stress, this reduces to

$$2 a_c \leq \frac{\pi}{8} \left[\frac{E h}{K_{IC}} \right]^2 \quad (57)$$

where we have used (36) and

$$\mu = \frac{E}{2(1 + \nu)} \quad (58)$$

and E is Young's modulus.

Calculations based on (57) are presented in Table 4. The values used for protrusion height, " h ," are typical grain sizes. All data are taken from the literature¹². Since typical room-temperature crack lengths for the materials considered are much less than the values computed for a_c , we conclude that the presence of interference changes the fracture stress.

Table 4. Critical Crack Length. For cracks smaller than a_c , the presence of interference alters the fracture stress. Data in the first three columns are from the literature¹². Values of a_c are computed from (57).

	K_{IC} (MPa m ^{-1/2})	E (GPa)	h (μm)	a_c (mm)
SiC	5	43	1.4	5.7
Si ₃ N ₄	5	200	1.0	0.6

ACCURACY OF CONCENTRATED LOAD MODEL

As indicated above, replacing the wedges with concentrated loads acting at their bases gives accurate results far from the wedges. But how far away is far enough? This question must be answered to determine how large the wedges can be relative to the crack size, how close an interference site can be to the crack tip, and how close two neighboring sites can be to each other, without compromising the accuracy of the concentrated load model. The question can be resolved through comparison of the stress field around the wedges with that generated by the concentrated loads.

In solving problem A, we have used the representation of the interference region depicted in Figure 5. But the correct representation is that shown in Figure 4. We seek to establish the condition under which the representation of Figure 5 is an accurate approximation to that of Figure 4. To accomplish this, we expand the Goursat potential function in the solution to problem B into an infinite series about the point $(h \tan \alpha, h)$ in Figure 4. The first and second terms of the series are potential functions for the concentrated forces and moments, respectively, in Figure 5. Subsequent terms change the series to that for the potential function appropriate for Figure 4. Near the wedge, these subsequent terms are important. But at some distance from the wedge ("far" from the wedge), they are negligible. The distance in question is that for which the third term of the series is much less in magnitude than the second term. This requirement leads to

$$\frac{x}{h} \gg d \quad (59)$$

where

$$d = \tan \alpha + \frac{\frac{2\gamma}{\xi_p} + \xi_p - \Gamma}{B(\alpha, 1) \cos \alpha} \quad (60)$$

$$\Gamma = \sum_{n=0}^{\infty} C_n^\gamma \frac{(-1)^n}{1-2n} \quad (61)$$

and the C_n^γ are coefficients of the binomial expansion.

Condition (59) implies that the present model is accurate only if each interference site is much farther from its nearest neighbor (another site or a crack tip) than "h" times the normalized distance "d" in (60). When n equally spaced sites are present, the requirement for site "i" is

$$\frac{h_i}{a} \ll \frac{2}{(n+1)d_i} \quad (62)$$

Since it depends on the wedge half-angle and the location of the contact load on the wedge face, d_i is not the same for each site.

ITERATION PROCEDURE

The correct values of P_i^* , $i = 1, 2, \dots, n$, are those for which (26) is satisfied simultaneously at all interference sites. These can be found by the following iteration procedure. First, choose an applied stress within the limits of (48). Then guess P_i^* , $i = 1, 2, \dots, n$, within the limits of (47). Use (28) to compute δ_A^* , which is the wedge bending displacement required by problem A. Then because of (26), put δ_A^* from (28) into (42), once for each site. This gives the values of P_i^* for problem B corresponding to the δ_A^* at each site.

It is not difficult to show that if a guessed value of P_i^* is higher/lower than the correct value, the value of P_i^* from (42) is necessarily lower/higher. A bisection procedure guided by this fact converges rapidly to the correct values of the entire set of contact loads, P_i^* . Given these, we can compute R from (14), (15), (18), (20) and (21). The iteration also yields the location of each

contact load on the wedge face ("r" in Figure 4). From this and (41), ξ_p can be computed and the accuracy condition (59) checked.

RESULTS AND CONCLUSIONS

We show results only for stress case I (biaxial tension), for which the middle column of Table 1 applies.

SINGLE INTERFERENCE SITE

For a single interference site, the influence of various parameters on the required fracture toughness ratio [R in (21)] is illustrated in Figures 8 through 13. In these figures, the abscissa is σ^* from (5). The range of σ^* is set by (48). Specifically, we use values of σ^* that give an initial crack opening displacement that varies from 0.05 to 0.95 times the wedge height, "h".

Figure 8. Dependence of the Required Fracture Toughness Ratio on the Wedge Half-Angle, α . The ordinate, R, is the ratio of the fracture toughness required when interference is absent to that required when interference is present. The interference site is located at the center of the crack. Narrower wedges have steeper sides, and so present more of a barrier to relative shear displacement than do broader wedges. For a given wedge, increasing the applied stress moves the contact force closer to the apex, where the wedge is more flexible. Thus, the contact force generated by a given relative shear displacement, and so the significance of interference, decreases. From (46), Table 1, and the value of ρ and β , interference occurs only for $\alpha < 26.6^\circ$.

Figure 9. Dependence of the Required Fracture Toughness Ratio on Orientation of the Applied Stress. The interference site is located at the center of the crack. In the absence of interference, the relative shear displacement of the crack faces is maximum for $\beta = 45^\circ + \alpha/2$ ($\beta = 52.5^\circ$). Hence, the influence of interference increases as β approaches this value.

The dependence of R on the wedge half-angle, α , is illustrated in Figure 8. As σ^* increases, the contact force moves nearer to the apex of the wedge. Consequently, the flexibility of the wedge increases and the curves shown have negative slopes. The drop-off is more pronounced for the narrower wedges. The significance of the interference decreases for broader wedges because for a given crack opening displacement, they allow greater relative shear displacement before contact occurs.

The dependence of R on the stress orientation angle, β , is illustrated in Figure 9. If the wedges offered no resistance to bending, their bending displacement would be maximum for $\beta = 52.5^\circ$. [See (28), (29), (31) and (32) with $\alpha = 15^\circ$, $\rho = 0$, $P_j^* = 0$.] Consequently, the effect of interference increases as β approaches 52.5° .

The dependence of R on the biaxial stress ratio defined in (6) is illustrated in Figure 10. The stress σ_2 produces relative crack-face shear displacement opposite to that produced by σ_1 . Hence, as the ratio of σ_2 to σ_1 increases, the net relative shear displacement, as well as the influence of interference, decreases.

As shown in Figure 11, variation of Poisson's ratio within physically realistic limits has little effect on R .

The dependence of R on the size of the wedge is illustrated in Figure 12. As one would expect, larger wedges produce greater changes in the

Figure 10. Dependence of the Required Fracture Toughness Ratio on the Stress Ratio, ρ . Both stresses are tensile. The interference site is located at the center of the crack. Because the relative shear displacements produced by the two principal stresses oppose each other, increasing the stress ratio reduces interference.

required fracture toughness. It is clear that if the wedges are too large relative to the crack size, replacing the wedges with concentrated loads in problem A is not appropriate.

In Figure 13, dependence of the change in required fracture toughness on the location of the interference site is illustrated. As the interference site approaches the crack tip, the change in required fracture toughness increases. This is to be expected. The nearer the site is to the crack tip, the greater its influence on the crack tip stress state.

MULTIPLE INTERFERENCE SITES

From the results shown in Figures 8 through 13, it is clear that the influence of protrusion interference at a single site on the fracture toughness required to prevent unstable crack propagation is slight. In a real crack with rough faces, protrusion interference would not, in general, be limited to a single interaction site. Consequently, the effect of multiple interaction sites must be considered before the significance of protrusion interference can be established.

When multiple interaction sites are present, it is not always clear which crack tip, $x = a$ or $x = -a$, is more affected by the interference. But we show results only for site distributions that are symmetric about $x = 0$. Hence, both tips are almost equally affected.

Figure 14 shows the influence of the number of sites on the fracture toughness ratio, R . The fact that the influence increases monotonically with

Figure 11. Dependence of the Required Fracture Toughness Ratio on Poisson's Ratio. The interference site is located at the center of the crack. Variation of Poisson's ratio within realistic limits has little influence on the amount of interference.

the number of sites is not surprising. The greater the number of sites, the more shear is carried by the protrusions and the less by the crack tips. Because of this result, one would expect the influence of interference to be substantial when it occurs at many sites.

Given that the influence of interference increases with the number of interaction sites, it is logical to consider extending the results shown in Figure 14 to include a large number of sites. But, as is now shown, the present model would not give accurate results in this case. Figure 15 shows the dependence of the normalized required separation distance, "d" (60), on the normalized crack opening. As the crack is opened, the contact load moves up the side of the wedge [See Figures 2 and 4.] This increases the relative magnitude of the higher order terms in the Goursat potential series, so that the region in which the approximation associated with Figure 5 is inaccurate grows. From (62) and Figure 15, we see that for a given crack length and wedge geometry, increasing the number of sites or the remote stress level eventually renders the present model inaccurate.

RECOMMENDATIONS

Although mathematically convenient, the wedge geometry used in the present model is probably not realistic. Specifically, the region adjacent to the wedge on the side opposite the contact force (see Figures 2 and 4) is not empty. If the missing material were filled in on this "back" side of each wedge, the geometry of Figure 2 would change to that of a line crack with a "step" at the interference site. The methods used herein could be extended to accommodate this geometry. Since the wedge is more flexible than the step,

Figure 12. Dependence of the Required Fracture Toughness Ratio on Wedge Size. The interference site is located at the center of the crack. As the wedge height increases, so does the interference.

use of the wedge could significantly underestimate the importance of the interference. To determine the error associated with using the wedge shape, more realistic protrusion geometries should be investigated.

The fact that the influence of interference increases with the number of sites indicates that the extent of this influence cannot be calculated unless cracks with many closely spaced sites can be modeled. Because the present model is not accurate when interference sites are near each other or near the crack tips, the model should be improved to accommodate closely spaced sites. This amounts to changing the mapping function used in problem B to that for a jagged crack.

Once the recommendations listed above have been followed, the influence of protrusion interference on unstable propagation of line cracks will be established. If that influence is significant, it would be appropriate to generalize the above method to accommodate a three-dimensional model. Such a model could include, for instance, a penny-shaped crack in triaxial stress with interference sites at various places on the crack. Clearly, the portion of this model corresponding to problem B (concentrated force on the side of a protrusion), above, as well as the kinematics of the displacement compatibility condition, would be far more complicated than they are in the model presented here.

It is unreasonable to expect to know precise protrusion geometries and interference locations for a real crack. However, it is reasonable to expect to know key parameters of statistical distributions of protrusion geometries and interference locations for a real crack. Given these, a protrusion interference model like that presented here could be incorporated in a simulation (based, for instance, on Monte Carlo methods) to predict the influence of

Figure 13. Dependence of the Required Fracture Toughness Ratio (at $x = a$) on the Location of the Interference Site. As the interference site approaches the crack tip, the influence of the interference on the fracture toughness required at the crack tip increases.

interference on the likelihood of fracture. If interference is important for arbitrarily specified crack geometries, then a simulation should be done.

Given the ability to simulate interference in real cracks, a procedure for incorporating the results of simulation into current design practices should be explored.

Finally, it should be noted that the concentrated load method used here in analysis of protrusion interference can be adapted to other kinds of crack-face interaction. For instance, it could be used to study Mode I crack opening of fiber-reinforced composites. The fact that crack bridging occurs at discrete fibers, rather than via a continuous traction, suggests that the concentrated load model is the more natural one for this problem.

Figure 14. Influence of Number of Interference Sites on Required Fracture Toughness. Applied (remote) stress is that which, in the absence of interference, opens the crack center a distance equal to five per cent of the wedge height ($\Delta v/h = 0.05$ at $x = 0$). As the number of sites increases, so does the influence on required fracture toughness. The smaller the wedge height relative to the crack length, the less the influence of the interference.

Figure 15. Required Separation of Interference Sites. As the crack opens, the contact load moves up the wedge face. This increases the distance on either side of the wedge at which the approximation of the present model becomes inaccurate.

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