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RAPID FRACTURE IN BEARING STEELS

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ABSTRACT

Rolling contact bearings normally fail by spalling fatigue, but rapid brittle fracture can occur when a combination of tensile hoop stresses, bending stresses, Hertz stresses and localized textural stresses are imposed on light-weight bearings. Since the materials are hard, of the order of 62 Rockwell C, and the ductility is low, simple fracture mechanics techniques may be used to predict the critical flaw size. We summarize the available data on the role of inclusions and use some new experimental values of K_{Ic} to define the conditions under which inclusions may become critical. The nature of the inclusions also comes into play. Oxide-type inclusions, which are predominantly calcium silicates, are more dangerous than sulfide inclusions (MnS and CaS), and we consider the reasons for these differences.

1. INTRODUCTION

High performance aircraft gas turbines utilize precision rolling element bearings on the mainshafts. As the performance requirements have risen the bearing rings have been reduced in section in order to save weight. In some instances the bearing rings have become a part of the support structure, to the extent that bending stresses may be imposed on the outer rings. In addition, the bearings are large enough to develop significant hoop stresses at operating speeds, and the superposition of hoop stresses and bending stresses onto the normal dynamic Hertz stresses can produce tensile stresses which may be large enough to produce rapid brittle fractures. And indeed, fractures have been observed. Ring fractures under test conditions have been documented by Moore et al [1] and by Clark [2], and a few service failures have also been observed. There is now considerable pressure to increase the performance demands on these bearings. Most mainshaft bearings operate at about 2.0 million DN. DN is a bearing speed parameter which is the product of the bearing bore in millimeters and the shaft speed in RPM. Future engines will approach 3.0 million DN, and under these conditions fracture may become the most significant mode of failure.

Most bearings fail by spalling fatigue. In the classic case a small crack forms below the surface, in the vicinity of the maximum normal Hertz shear stress. The initiation point is usually associated with a non-metallic inclusion, a void, or a manufacturing defect. The crack propagates toward the surface by a fatigue mechanism and failure is detected when a small piece spalls away. Littmann and Widner [3], among others, have shown how rolling contact fatigue originates at surface and subsurface origins. Theories of the fatigue life of bearings are based on a statistical approach

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to the occurrence of flaws in the loaded volume of the material [4]. The fatigue life is inversely proportional to the (load)^3, since an increase in load leads directly to an increase of stress at the defect and also to an increase in the loaded volume. Fracture, as such, is not considered a usual mechanism of failure, except when spalling has proceeded to the point where the bearing can no longer operate.

The performance requirements are reflected in the specifications of gas turbine bearing steels. Relatively few compositions are used: 52100 steel (1.0 C, 1.5 Cr) is used for some of the smaller and older engines and M-50 high speed steel (0.82 C, 4.2 Mo, 4.0 Cr, 1.0 V) is used in the newer high-performance engines. In British engines 18-4-1 high speed steel (0.75 C, 18 W, 4.0 Cr, 1.0 V) is used in high performance engines.

Vacuum arc remelting is common in the U.S. and there is an increasing tendency to specify vacuum induction premelting for the electrode melt. Electroslag remelting is used in British practice. Ultrasonic inspection of the billets is common and is also in use for some bar stock. Etched discs are used for the detection of carbide patterns and segregation, and metallographic examinations are used to determine inclusion contents. Nevertheless, some defects are always present in these bearings. Inclusions and other defects are present in the steel, and defects may be introduced during the ball-heading, forging, heat treating and grinding steps used in the manufacturing process.

The service requirements also place severe limitations on the heat treatment of these bearing steels. Good abrasion resistance is required, and the hardness is thus specified to be of the order of 61-63 Rockwell C. Dimensional stability is necessary and the retained austenite content may thus be specified to be less than three percent. The resultant material has very low ductility, of the order of one percent, a very high yield stress, of the order of 1860 MPa and a very low fracture toughness.

When the bearings are operated at low stresses, the stress intensity range, ΔK, may be below the threshold value for propagation, K_{th}, and the existing defects in the load zone will not propagate. If the stress levels and defect sizes result in stress intensity factors which are only slightly above K_{th}, cracks will propagate slowly and the usual spalling fatigue failures will develop. Spalling is undesirable, but it is a relatively gradual failure process which may provide some warning of difficulty by an increase in vibration of the engine or by the presence of chips in the oil. At high stress levels the stress intensity range may reach values where the crack propagation is rapid and brittle fracture occurs. These failures are catastrophic. There may not be any spalling at all and consequently no warning. In order to avoid difficulty we must have good data on the operating stress levels, on the nature of the defects in the bearings, and on the critical stress intensity factors of the material.

Unfortunately, we do not have extensive data on the fracture toughness of these materials. We have used some preliminary results from work which is now in progress in our laboratory and some earlier measurements of K_{Ic}, the critical stress intensity factor for plane strain [5], in order to estimate values of K_{Ic} in aircraft bearing materials. There is a considerable body of work on the nature of the inclusions present in bearing steels, with some indications that not all types may be dangerous, at least in fatigue failures. There have also been calculations by finite element methods of the behavior of inclusions in steel and some speculation on the role of the interfacial energy in the initiation of cracks. Finally,

there is a continuing controversy on the sizes of inclusions which may be tolerated in bearing steels and on the methods which can be used to find these critical inclusions.

In this paper, we apply simple linear fracture mechanics to examine the role of inclusions in the rapid fracture of bearing steels. Factors which arise from the nature of the inclusion are examined and we attempt to assess the influence of operating stresses. We then try to place limits on the sizes of acceptable flaws and speculate on what can be done to raise the values of K_{Ic} in bearing materials.

2. INCLUSIONS

The importance of inclusions in the fatigue life of bearing steels has been recognized for many years and the ASTM charts used for rating microsclpic inlcusions in these steels are well known. There has been considerable disagreement, however, on whether inclusions of all types are equally harmful. The concept of a critical inclusions size has also been debated. One view holds that all inclusions in the load zone are dangerous, no matter how small, since these are stress raisers at which microcracks will initiate and then propagate to failure. On the other hand, there is the observation that many inclusions in the load zone do not lead to spalling fatigue cracks and that only the large inclusions are dangerous. The critical size has not been defined, however, and we will attempt to approach this using the concepts of fracture mechanics.

The mechanical behavior of various kinds of inclusions under conditions of spalling fatigue has also been investigated. In a classic series of papers Laslo [6] described methods for the calculation of tessellated (or textural) stresses in steels. The basic concept was that microstress systems can develop around inclusions as a result of differential thermal contraction. Brooksbank and Andrews extended the concept of microstresses around inclusions in an extensive series of papers [7-12]. Textural stresses were considered to arise from differences in thermal contraction between inclusions and the steel matrix resulting from the quenching operation in hardening. These tesselated stresses were of the form:

$$\sigma = \pm \beta \,[(\alpha_2 - \alpha_1)\,\Delta T] \tag{1}$$

where β is a function of the elastic moduli of the inclusions and the matrix and of the inclusion shape, size and distribution, α_2 and α_1 are the expansion coefficients of the matrix and the inclusion, respectively, and ΔT is the temperature drop on hardening. The inclusion is assumed to be at equilibrium with the matrix at the austenitizing temperature and the stresses arise because of changes in the relative volumes after quenching to room temperature.

The expansion coefficients of a wide variety of materials found in bearing steel inclusions were measured and a summary of some of these data are given in Table 1. The expansion coefficient of each nonmetallic compound, α_1, is to be compared with the average effective expansion coefficient arising from the austenite-to-martensite reaction, $\alpha_2 = 12.5 \times 10^{-6}/^{0}C$. It is immediately evident that the inclusions fall into two classes; those with expansion coefficients smaller than α_2 and those with expansions greater than α_2. The stress distributions around spherical and cylindrical inclusions were also studied and it was shown that tensile circumferential stresses develop when $\alpha_2 > \alpha_1$, and that compressive circumferential stresses arise when $\alpha_2 < \alpha_1$.

Particular attention may be focussed onto a few common types of inclusions. For example, the calcium aluminates, which are usually designated as oxide-type inclusions and which are commonly found in bearing steels can have expansion coefficients as low as $5 \times 10^{-6}/^{0}C$. Such a particle could develop a circumferential tensile stress of over 700 MPa. This is a significant tensile contribution to the stress system. A typical calculation of stresses around a $CaO \cdot 2\ Al_2O_3$ spherical particle is shown in Figure 1. On the other hand, MnS and CaS have expansion coefficients which are considerably larger than α_2. Sulfide particles can shrink away from the matrix and form small voids at the interface, but in any event, the circumferential stress at the interface is low or in compression. In some instances, oxide inclusions may be completely surrounded by a sulfide envelope. Brooksbank and Andrews have shown that such an encapsulated inclusion may be almost free of circumferential stress at the interface, as shown in Figure 1.

Bearing test data involving failures by spalling fatigue appear to confirm the approach by Brooksbank and Andrews in identifying the inclusion types which are unfavorable. They cite several investigations which indicate that aluminate inclusions are deleterious and that sulfide inclusions are innocuous. Furthermore, there are experimental data to suggest that sulfide-encapsulated oxide inclusions are not as dangerous as the uncoated oxides. The general features of these results have been confirmed in extensive bearing tests here [13]. In some instances the steel compositions have been adjusted to provide slightly larger sulfur contents in order to encapsulate the unfavorable inclusions. A word of caution, in this respect, is in order, however. If the sulfur content is increased too much, the sulfide inclusions can become very large. The large inclusions have a large number of voids at the interfaces and act essentially as cracks. These cracks may then be large enough to lead to other difficulties.

The principal difficulty with this approach is that it does not adequately take into account the size of the inclusions. Size only comes in as a scaling effect in defining the region over which the textural stresses operate, but in principle, a small inclusion generates as much stress at the surface as a large one. Experience has indicated that the large inclusions are much more dangerous than the small ones and it is evident that other factors come into play. Nevertheless, the Brooksbank and Andrews approach is important in providing a criterion for the identification of the types of inclusions which are unfavorable and the basic premises of this theory appear to be consistent with the observation that small sulfide inclusions do not play a significant role in the initiation of spalling fatigue whereas oxide inclusions are very prominent in this respect.

3. INCLUSIONS AND THE INITIATION OF MICROCRACKS

Inclusions of calcium aluminum silicate and other oxides, which we categorize as "oxide" inclusions, are frequently observed in bearing steels. These are rated by microscopic observation at 100X and compared with standard fields listed in the ASTM charts. A typical globular oxide inclusion in an ASTM chart has a diameter of about 8 μm. It has been recognized that such particles in a steel matrix are stress raisers, and the conditions for crack initiation have been treated recently by Sundström [14].

Sundström considered the case of a cylindrical alumina particle in steel. He applied linear fracture mechanics and used a finite element calculation to determine the conditions under which a crack will form at the interface. An energy criterion was used, with the assumption that the interfacial particle-matrix energy (or the work of adhesion) was less than the surface energy of the matrix or of the particle material. The geometry of the calculation model is shown in Figure 2. A stress criterion for the initiation of interfacial microcracks was derived as a function of inclusion size and bonding strength. It was shown that the microcracks would not propagate, at a constant stress, below a minimum size, about d/4 (corresponding to the angle, $\psi = \pi/6$ in Figure 1). Above this size the cracks will increase in size, still following the interface, with a net decrease in energy release rate. The maximum size is limited, however, to a length which is somewhat less than the diameter of the particle, since the crack will not form in a direction parallel to the applied tensile stress.

Sundström calculated the critical stress required to form a stable crack for an alumina particle 1 μm in diameter with a work of adhesion, $W_0 = 1\ J/m^2$. The results can be scaled for other values of d and W_0. These results are summarized in Figure 2 and we have added the calculation for a particle of 8 μm diameter. For $W_0 = 1\ J/m^2$, which is a reasonable estimate for the oxide-matrix interface, the critical stress is approximately 200 MPa. However, the resultant crack size is quite small, of the order of 8 μm, and we must still consider whether the crack will propagate through the matrix and lead to failure.

The Sundström calculation is very valuable in that it demonstrates that a relatively small tensile stress is required to delaminate an oxide inclusion. If we consider the tesselated stresses in the matrix, as calculated by Brooksbank and Andrews, it is evident that the circumferential stresses may provide a favorable stress field for the subsequent propagation of these interfacial cracks into the matrix. The size of the particle, the fracture toughness, K_{Ic}, of the matrix and the operating stresses then become the critical parameters.

4. THE PROPAGATION OF FRACTURE FROM INCLUSIONS AND FLAWS

The test data on bearings [1,2] have indicated that brittle cracks can be generated in rings from flaws which are at or near the surface. Since these hardened steels have a very low ductility and a yield stress which approaches the fracture stress, plane strain conditions almost always prevail. The plastic zone in front of a crack is very small, and may be neglected.

Following the procedure outlined by Clark [2] we use the plane strain stress intensity factor for a semi-elliptic surface crack [15].

$$K_I = \frac{1.1\ \sigma\ (\pi a)^{1/2}}{(\phi^2 - .212\ (\sigma/\sigma_y)^2)^{1/2}} \qquad (2)$$

where K_I is the plane strain stress intensity factor, σ is the applied normal stress, a is the crack depth, 2c is the crack length, σ_y is the yield stress, and Φ is an elliptic integral which takes values from 1.0 to 1.57. The second term in the denominator is a correction factor for the plastic zone size, and since it is small we may combine the denominator in equation (2) into a flaw parameter, $Q = \phi^2 - .212\ (\sigma/\sigma_y)^2$. We then write

$$K_I^{\,2} = 1.21\ \pi\sigma^2 \left(\frac{a}{Q}\right) \tag{3}$$

For the values $a/2c$ = .25 and σ = 0.5 σy, $Q \simeq 1.25$, and we may further simplify the equation to

$$K_I^{\,2} = \pi\sigma^2 a \tag{4}$$

For rapid fracture,

$$a_c = \frac{K_{IC}^2}{\pi\sigma^2} \tag{5}$$

where K_{Ic} is the critical stress intensity factor for plane strain fracture.

Few values for K_{Ic} have been measured for bearing steels. Data for a deep hardening bearing steel [5] and a few values for 52100 have been measured in our laboratory by means of compact tension specimens. A value for M-50 has been inferred by Clark from bearing test data [2]. These data are summarized in Table 2. It should be emphasized that these are only preliminary data, but it is unlikely that much higher values will be obtained when our experimental work is completed.

Our data indicate K_{Ic} = 15.5 $MPa \cdot m^{1/2}$ for 52100 and Clark has obtained $K_{Ic} \simeq 17.5\ MPa \cdot m^{1/2}$ for M-50. We assume an average value of 16.5 $MPa \cdot m^{1/2}$ in our estimates of the critical crack size.

Clark has shown that a typical 150 mm bearing operating at 2.0 million DN will develop a tangential stress of the order of 138 MPa (20 ksi). This stress includes 103 MPa from free ring centrifugal forces and 35 MPa from fit-up stresses. At 3.0 million DN the corresponding tangential stress is about 262 MPa (38 ksi) with 228 MPa (33 ksi) arising from centrifugal forces alone.

Most of the high performance aircraft bearings now in use operate at about 2.0 million DN. Let us first consider an inclusion which does not have any textural stresses associated with it. Following Brooksbank and Andrews this could be a sulfide inclusion or a free crack. Applying equation (5) and using the average value, K_{Ic} = 16.5 $MPa \cdot m^{1/2}$, and a hoop stress of 138 MPa we arrive at a critical crack size of 4.6 mm. This is a crack of macroscopic dimensions. On the other hand if we consider an oxide inclusion with the additional tesselated stress of 700 MPa as calculated by Brooksbank and Andrews we now have a total stress of 838 MPa, and the critical crack size becomes a_c = 0.12 mm. This is quite a small macroscopic inclusion and quite difficult to detect by non-destructive methods. On a microscopic scale, at 100X, a globular inclusion of this size would probably lead to a rejection of the steel.

These data, which are summarized in Table 3, illustrate the dilemma in assessing the quality of steels. It appears that the microscopic inclusions, of the size specified in the ASTM charts, may be of little importance in the rapid brittle fracture of bearings. The large inclusions are important, but these are of a size which is difficult to detect by macroscopic

non-destructive methods. At the present, sampling methods, such as etch discs and fracture samples are used to assess the probability that the steel is free of such defects, but such a system is not completely reliable.

In the future it appears that aircraft bearings may approach performances of 3.0 million DN. At an operating stress of 262 MPa the critical defect size becomes 1.3 mm for a sulfide or a free crack. This size is also in the awkward micro-macro size range. On adding the tesselated stress, 700 MPa, the critical defect size becomes 0.094 mm, and the critical size of an oxide inclusion falls into the microscopic range. At a size of 94 µm such an inclusion is still very large by ASTM microscopic standards. These critical inclusion sizes are summarized in Table 3.

It is significant to note the microscopic inclusions of the ASTM size (8 µm) are too small to lead to rapid fracture even in the most unfavorable case, an oxide type operating at the high performance level of 3.0 million DN. The dangerous inclusions are in the micro-macro size range 0.1 to 1 mm.

DISCUSSION

Investigations of fractures in bearings operating at levels of hoop stress in the vicinity of 207 MPa (30 ksi) [1,2] have confirmed the general outlines of our calculations of the critical defect sizes in bearing steels. We have used the concepts of Brooksbank and Andrews in an attempt to differentiate between the kinds of inclusions which are usually found in bearing steels. This leads to the conclusion that oxide inclusions are much more dangerous than sulfide inclusions or free cracks. Although the calculation of tesselated stresses may have some uncertainties the general experience with spalling fatigue tests seems to indicate that sulfides are not nearly as harmful as oxides.

For crack initiation at an inclusion, even at a microscopic inclusion of the order of 10 µm, only about 200 MPa are required to form a crack at the interface, in accordance with the calculation of Sunstrom [14]. If the particle is an oxide inclusion, the operating hoop stresses alone may be sufficient to delaminate the particle, and the textural stresses will assist in the propagation into the matrix. However, the particle must be quite large (see Table 3) if the crack is to propagate in a rapid brittle failure. Unfortunately, inclusions of this size are still found in bearing steels, even with the best current practice, as shown in Figure 3.

The concept of encapsulated oxides, with a sulfide surrounding the oxide, may provide an important practical mechanism by which the dangerous aspects of oxide inclusions can be reduced somewhat. Inclusions of this type are observed, and a typical example is shown in Figure 4. This suggests that the sulfur content and the steel mill practice may be adjusted to promote this encapsulation, but the process cannot be carried to extremes. If the sulfide content is too high, large sulfide stringers will be formed and the inclusions will approach the critical size for a free crack. The sizes of the critical inclusions pose awkward problems in the quality control of bearing steels. The dangerous sizes are in the micro-macro region and are difficult to detect by non-destructive means. As a result, we must rely on sampling methods and on the control of the steelmaking process.

These problems would be alleviated by the development of steels with higher fracture toughness values and research in this area is now getting underway.

Some progress may also be made in reducing the bending stresses by changes in bearing design, but it is evident that the centrifugal hoop stresses alone at 3.0 million DN will result in difficulties with our current materials.

Finally, we must have a better picture of the significant fracture parameters of these materials. Our data were obtained at room temperature in laboratory air. The situation may be much different at 200°C in a synthetic turbine oil. We also need basic data on the threshold value of the stress intensity factor, K_{th}. Some of our data indicate that this may be as low as 5.5 MPa•m$^{1/2}$. This suggests that spalling fatigue starts at smaller inclusions than rapid fracture, but under some conditions these spalling cracks may become large enough to lead to catastrophic failure.

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TABLE 1

Thermal Expansion Coefficients of Inclusions in 52100 Bearing Steel

Inclusion type	Compound	Mean expansion coefficient (0-800°C); α_1 (10^{-6}/°C)
calcium aluminates	CA_6 CA_2	8.8 5.0
(C = (CaO))	CA	6.5
(A = $A\ell_2O_3$)	$CA_{12}A_7$ C_3A	7.5 10.0
Silicates	$(A\ell_2O_3)_3(SiO_2)_2$	5.0
Alumina	$A\ell_2O_3$	8.0
Spinels	$MgO{\cdot}A\ell_2O_3$	8.4
MATRIX		
austenite	γ FE (850°C → M_s)	23.0
martensite	α'Fe (M_f → RT)	10.0
average effective expansion on hardening	$\gamma \rightarrow \alpha'$ (850°C → RT)	α_2 = 12.5
Sulfides	MnS	18.1
	CaS	14.7

Note: Data taken from Brooksbank and Andrews [11].

TABLE 2

Values of Critical Stress Intensity
Factors for Bearing Steels

Steel	Hardness Rockwell C	K_{Ic} $MPa \cdot m^{1/2}$
deep hardening bearing steel [5]	60	15.5(1)
52100 bearing steel	62	15.5(1)
M-50 bearing steel	61	17.5(2)

(1) compact tension data

(2) inferred from fracture data on bearings [2]

TABLE 3

Critical Inclusion Sizes in Bearing Steels
(K_{Ic} = 16.5 $MPa \cdot m^{1/2}$)

Bearing performance DN(10)	Stress MPa	Type of inclusion	Critical size a_c, mm
2.0	130 (hoop stress)	sulfide, free crack	4.6
2.0	830 (hoop + textural)	oxide	0.12
3.0	262 (hoop stress)	sulfide, free crack	1.3
3.0	962 (hoop + textural)	oxide	0.094

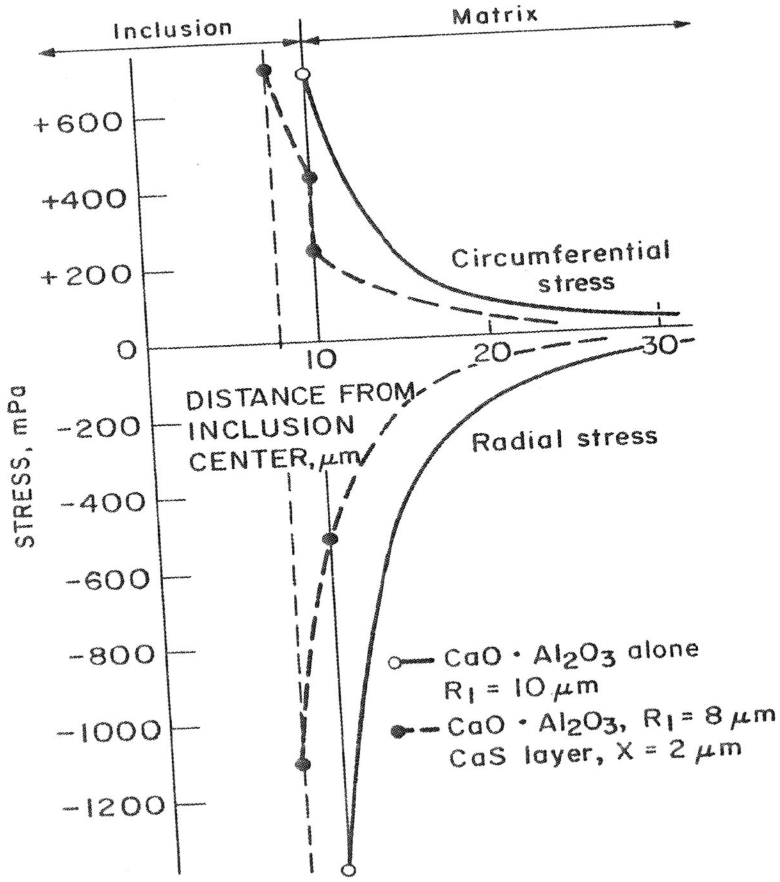

Figure 1 Textural stress around a $CaO.2A\ell_2O_3$ particle after quenching a 1.0 C, 1.5 Cr steel from 850°C, and around an aluminate particle which has been encapsulated in a sulfide layer, after Brooksbank and Andrews [10].

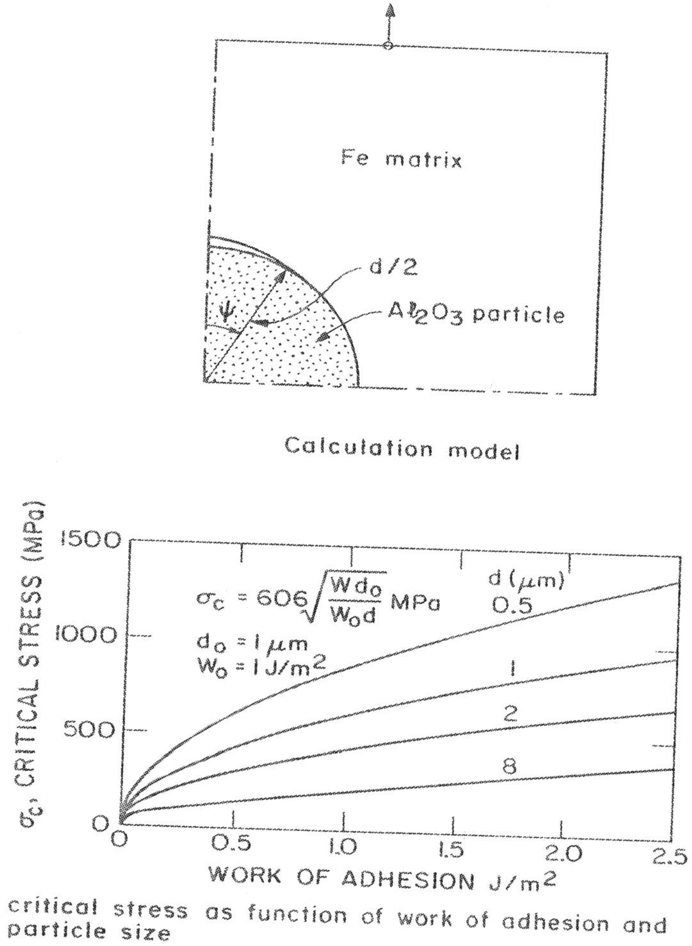

Figure 2 Initiation of cracks at interface of alumina particle and steel matrix, after Sundström [14].

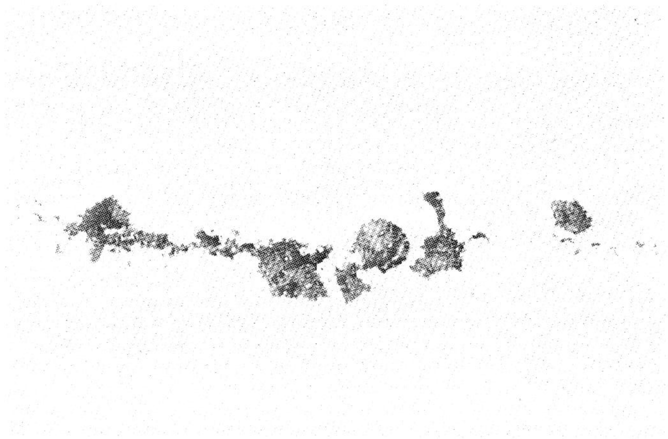

Figure 3 Portion of an oxide inclusion in a failed bearing, at 500X. Total inclusion was approximately 2.5 mm long.

Figure 4 Sulfide inclusion which has encapsulated an oxide inclusion in the center. 1000X.