Why incongruous knee replacements do not fail early☆

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Abstract

Rapid failure of knee prostheses does not usually occur, despite the non-conforming nature of the articulation between femoral and tibial components and the associated large contact pressures. This theoretical study examines the likelihood of fatigue fracture of a layered elastic model loaded by a sliding cylindrical indenter. Cracks (line-defects) were assumed to have nucleated within the layer. The stress intensity factors (SIFs) associated with these cracks were calculated. The values obtained for the SIFs are quite low, with a corresponding low likelihood of crack-growth. When taken in conjunction with the experimentally derived fatigue laws of previous investigators, they suggest that short line-cracks should not grow. It seems that early failures have not simply been due to large shear stresses which occur beneath the prosthesis surface. Other factors, such as the degradation of material through heat-pressing, sterilisation or oxidation, or the deleterious effect of fusion defects, may be required to drive the cracks to delamination.

Keywords: Knee prosthesis; UHMWPE; Stress; Fatigue; Crack

1. Introduction

The majority of incongruous total knee replacements do not wear out early. Some designs have 10-yr survival rates of over 95% (Ritter et al., 1994). In those retrieved tibial components that have failed, a number of damage modes have been observed. Some, such as abrasion, burnishing and scratching, are not thought to be particularly serious (Wrona et al., 1994). Other modes, notably deformation and cracking, appear to be more critical. Plastic deformation is evident in a large proportion of retrieved tibial components (Kilgus et al., 1991) and there is little doubt that this deformation is due to stressing of the polyethylene material (UHMWPE) above its yield strength. The contact pressures between the femoral and tibial components frequently exceed the yield strength of UHMWPE, even for more conforming designs (Collier et al., 1991), but this does not seem to damage the material greatly, probably due to the state of near-hydrostatic pressure at the material surface. A more appropriate indicator of deformation is large shear stresses. These are known to occur, and to exceed the yield strength, but it appears that this occurs only occasionally (McNamara et al., 1994). In any case, this does not necessarily imply that the UHMWPE component will fail. Initial deformation generally increases contact area and reduces contact stress (though not nearly down to the levels observed in conforming designs (McNamara et al., 1994)), and deformation tends to alter the position of maximum stress continually (Blunn et al., 1992), making further deformation less likely.

A more serious form of damage to tibial components is cracking of the UHMWPE layer, and delamination, which occurs when sub-surface cracks grow near-parallel to the surface, and then rise to strip off sheets of material. The depth of delamination is typically 0.5–1.5 mm (Landy and Walker, 1986). It is generally accepted that this form of wear is due to a fatigue mechanism of failure, the more obvious indicators being that the material experiences a cyclic loading regime, and the damage increases with load (Reeves et al., 1998) and time (Hood et al., 1983).

This study investigates why delamination does not always occur, by examining the likelihood of fatigue fracture of a layered elastic model. SIFs associated with
hypothesised cracks at various positions within the prosthesis were calculated. The hypothesis for the study was that the SIFs are not sufficiently high for rapid crack propagation.

2. Methods

A three-modulus plane-strain model of a prosthesis was developed. The model would be appropriate for moderate-to-low conforming prostheses with cylindrical femoral condyles (e.g. Miller-Galante, AGC). It consists of a doubly layered substrate impressed by a cylindrical indenter, Fig. 1. The UHMWPE/metal and metal/bone make continuous contact along common interfaces and the indenter consists of a rigid cylinder of radius and axial length in the region of 20–25 mm. A force of 0.5–2 kN is applied, typical of the load transmitted by one of a pair of condyles. Sliding friction at the UHMWPE surface was considered. The values of the geometric and elastic parameters employed are shown in Table 1.

In order to solve the indentation problem of the multi-layered medium, a piecewise linear representation of the normal and shear stresses arising in the contact region was assumed (Bentall and Johnson, 1968). An integral representation of the stress and displacement within the bodies was derived using Fourier transform theory, in terms of these unknown stresses. The displacements at the surface were then matched with those in the indenter, whence the contact stresses and displacements at the surface were then matched with those in the indenter, simulating the effects of indenter motion relative to the crack. The total traction arising along the (free-) surface of the defect, due to both the dislocations and the contact, was then set to zero (where a crack closed, the crack-faces were assumed to be in frictionless contact). This formulation resulted in a set of singular integral equations which were solved numerically, using appropriate quadrature rules (Kelly et al., 1997), and the stress distributions in the surrounding materials were obtained. The analysis was performed on a Gateway 2000 Pentium 5, 90 MHz with 32 MB RAM.

With the stresses in the cracked body known, the crack-tip SIFs were found. Two factors were obtained: $K_I$, which is a measure of the likelihood of crack-growth in an opening mode, and $K_{II}$, which corresponds to a shear mode of failure

$$K_I = \lim_{r \to 0} \left\{ \sqrt{2r} \sigma_{\theta \theta} |_{r=0} \right\},$$

$$K_{II} = \lim_{r \to 0} \left\{ \sqrt{2r} \sigma_{r \theta} |_{r=0} \right\} ,$$

(1)

where $(r, \theta)$ are polar co-ordinates measured from the crack-tip and $\sigma_{\theta \theta}$, $\sigma_{r \theta}$, are the normal and shear stresses in the vicinity of the crack-tip. For each crack direction, length and depth, solutions were obtained for numerous crack positions relative to the indenter, simulating the effects of indenter motion relative to the crack. The ranges of SIF were determined: $\Delta K_I = \max(K_I) - \min(K_I)$, $\Delta K_{II} = \max(K_{II}) - \min(K_{II})$, $\min(K_I) = 0$. An effective SIF, $\Delta K_{I,FF}$, may then be found by combining these factors in such a way that the crack grows in a direction normal to the maximum stress at the crack-tips (Erdogan and Sih, 1963; Keer et al., 1982); one has (Elbert et al., 1994).

Table 1

<table>
<thead>
<tr>
<th>Material</th>
<th>Elastic modulus, $E$ (GPa)</th>
<th>Poisson’s ratio, $\nu$</th>
<th>Thickness, $h$ (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Uppermost layer</td>
<td>UHMWPE</td>
<td>0.75–1.5</td>
<td>0.4</td>
</tr>
<tr>
<td>Intermediate layer</td>
<td>Metal</td>
<td>200</td>
<td>0.3</td>
</tr>
<tr>
<td>Substrate</td>
<td>Subchondral bone</td>
<td>20</td>
<td>0.3</td>
</tr>
</tbody>
</table>

Fig. 1. Geometry of the knee-prosthesis model, consisting of a doubly layered substrate: the uppermost layer represents the UHMWPE component, the intermediate layer represents the metal-backing, and the substrate represents the subchondral bone.
$$\Delta K_{\text{EFF}} = \cos^2 \left( \frac{\alpha}{2} \right) \left[ \cos \left( \frac{\alpha}{2} \right) \Delta K_I - 3 \sin \left( \frac{\alpha}{2} \right) \Delta K_{II} \right],$$

$$\alpha = \arcsin \left\{ \frac{\Delta K_{II} \left( \Delta K_I - 3 \sqrt{\left( \Delta K_I \right)^2 + 8 \left( \Delta K_{II} \right)^2} \right)}{\left( \Delta K_I \right)^2 + 9 \left( \Delta K_{II} \right)^2} \right\}$$

and $\alpha$ is the angle at which the crack propagates (measured from the line of the crack). $\Delta K_{\text{EFF}}$ may be used in conjunction with Paris-type fatigue crack-growth laws to estimate crack-growth rates.

3. Results

3.1. UHMWPE: (i) uncracked system

It is worthwhile mentioning some features of the uncracked solution:

1. The contact semi-width varies between 0.5 and 2 mm per condyle.

2. UHMWPE thickness $h$ has little effect on contact pressure or on the Von Mises yield parameter $\sigma_{\text{VM}}$ (a measure of the likelihood of plastic yield) for typical loads (1–4 times body-weight) and for $h > 4$ mm (and not too significant an effect for $3 \text{ mm} < h < 4 \text{ mm}$). It should be emphasised that this holds only for the prostheses of incongruous type considered here.

3. The maximum contact pressure was found to vary between 20–50 MPa, equal to or greater than the yield strength of UHMWPE (20–25 MPa, Bennett et al., 1996), even at relatively low loads ($\approx 1500 \text{ N}$).

4. The maximum value of $\sigma_{\text{VM}}$ within the UHMWPE lies in the range 5–20 MPa at a depth of 0.5–1 mm below the surface, for frictionless contact. When sliding friction is considered, with a coefficient of friction $\mu = 0.1$ (Wimmer and Andriacchi, 1997), the maximum value increases by more than 5%, and occurs nearer to the surface.

5. A force of only 1 kN is required to produce a contact pressure which exceeds the yield strength whereas one of over 4 kN (or high friction) is required to produce a Von Mises stress in excess of the yield strength.

3.2. UHMWPE: (ii) cracked system

As with many similar (non-cracked) analyses (e.g. Bartel et al., 1985), two regions within the layer were identified when the stress-field in the uncracked system was used to predict where cracks are likely to propagate. The first is at the surface where the largest tensile stresses occur parallel to the surface and just outside the contact area. The second occurs about 0.5–1 mm below the surface, directly beneath the load, where large shear stresses arise. Thus vertical surface-breaking cracks and horizontal sub-surface cracks were considered. Short cracks (0–1 mm in length) were analysed.

For vertical surface-breaking cracks, Fig. 3(a) shows that Mode I SIFs are positive, and likely to lead to propagation, but only when the crack lies outside the embrace of the contact. For zero friction, they are symmetrical at the leading and trailing edges of the contact but are increased at the trailing edge by friction and decreased at the leading edge. Negative values, arising when the crack is under contact, are not included in the graph. The effects of friction are illustrated only at the trailing edge. Positive Mode I SIFs decrease with increasing crack depth (Fig. 3(c)) as the crack-tip becomes more remote from the region of maximum tension.

Fig. 3(b) shows that Mode II SIFs for vertical surface-breaking cracks under frictionless loading vary asymmetrically as the indenter passes the crack but are increased in magnitude near the leading edge and decreased near the trailing edge by friction. Maximum Mode II SIF magnitudes increase with increasing crack length (Fig. 3(c)).
When the effective SIF is calculated (Fig. 3(d)), its maximum value is found to increase with crack length for both values of the load studied but does not increase linearly with increasing load. A friction coefficient of 0.1 results in only a modest increase in the effective SIF. Fig. 4 shows that the effective SIF for horizontal subsurface cracks increases with crack length, negligibly affected by a friction coefficient of 0.1. Significant subsurface tensile stresses, requiring larger friction coefficients, are needed to influence the effective SIF for horizontal sub-surface cracks.

In order to quantify these results, they may be compared with the Paris laws of Elbert et al. (1994), derived for centre-notched UHMWPE specimens under mixed-mode fatigue loading

$$\Delta K_{\text{eff}} = \left( \frac{\Delta L / \Delta N}{C} \right)^{1/m}. \tag{3}$$

Here, \( \Delta N \) is the number of cycles required to grow a crack an increment in (half) crack length \( \Delta L \), and \( \Delta K_{\text{eff}} \) is the average value of the effective SIFs for the initial and final cracks. \( C \) and \( m \) are constants obtained by experiment: for a crack under predominantly tensile loading, \( C = 9.4 \times 10^{-10} \) and \( m = 15.6 \); for a crack under combined tension and shear, \( C = 3.6 \times 10^{-10} \) and \( m = 15.0 \). Taking \( \Delta K_{\text{eff}} = 0.3 \) (from Figs. 3(d) and 4) and these values of the fatigue parameters, one finds that a crack will grow but only about 10\(^{-11}\) m in \( \Delta N = 1,000,000 \) load-cycles. These and similar results from fatigue experiments (Pruitt and Ranganathan, 1995; Dunlap and Rimnac, 1998) suggest that values of \( \Delta K_{\text{eff}} > 1 \) are necessary for significant crack-growth.

3.3. Metal fracture

The model predicts that stress levels in the perfectly bonded metal layer only ever reach about 40 MPa at most. Thus there should be no fracture provided there are no stress-raisers. The effect of poor metal/bone fixation (the metal fixed at its edges and poorly
supported towards its centre) was simulated by assigning a low Elastic modulus to the subchondral bone. The tensile stresses in the metal then reach over half the tensile strength of typical prostheses (~900 MPa), in which case the metal might be susceptible to (fatigue) fracture.

4. Discussion

Solid UHMWPE is manufactured from a melted powder using a compression moulding or a ram extrusion sintering process. Perfect fusion is unlikely (Rose et al., 1982). The line-cracks studied in this paper may simulate intergranular imperfections where molecules have not diffused across the interface between neighbouring particles (Wu et al., 2001). Dowling et al. (1978) described a mesh of similar surface-breaking line-cracks, micrometers in length, in acetabular components retrieved at post-mortem.

The methods used in this paper, mathematical formulation of integral equations, was preferred to the finite element method because of the need to calculate SIFs for cracks of many lengths, orientations and positions relative to the indenter for various load values (Hills et al., 1996). The advantage of computational efficiency and accuracy has to be traded against the use of a very simple geometry.

The paper does not address the many questions raised by the effects of various sterilisation and cross-linking procedures on the strength of UHMWPE. The calculated SIFs depend only on the elastic properties of the material and are unlikely to be affected significantly by post-consolidation procedures, although the \( \Delta K_{EFF} \) at which stable crack growth will occur could be as much as 30% less for irradiated material than for non-sterile material (Pruitt and Ranganathan, 1995).

The results for the uncracked geometry agree well with the numerous previous stress analyses, and with experimental observation, for example contact area magnitudes (Statler et al., 1992), contact pressure levels (Miller et al., 1995; McNamara et al., 1994), position of maximum \( \sigma_{VM} \) (Blunn et al., 1992), magnitude of \( \sigma_{VM} \) (Collier et al., 1991), and the ability of the Hertz theory for a half space to predict contact pressure for a layer (Whelen and Little, 1992).

The results for vertical surface breaking cracks agree well with the finite element analysis of Eberhardt and Kim (1998). They concluded that the increase in maximum mode II SIF with increasing crack length, as demonstrated here in Fig. 3(c), “suggests that propagation in mode II along the original crack plane would be unstable”.

The calculated SIFs are small, the associated crack propagation rates are very low, and suggest that short line-cracks should not grow early in the lifetime of a prosthesis—if at all. In an FE study examining surface cracks, Estupiñán et al. (1998) evaluated the maximum value of \( \Delta K_{EFF} \) to be about 0.46, similar to the present results. However, Estupiñán et al. (1998) concluded that, since propagation rates are very sensitive to changes in \( \Delta K_{EFF} \), this value is consistent with damage occurring after several years; a doubling of \( \Delta K_{EFF} \) would grow the crack >0.1 mm in 1 million cycles. Nevertheless, especially since the \( \Delta K_{EFF} \) values at the more common physiological loads (see Fig. 3(d), \( P = 1.5 \) kN) are significantly lower than the maximum predictions, the present study suggests that the calculated growth-rates may be too low for significant crack-growth.

It has been argued that the most important factor affecting UHMWPE integrity is the presence of unconsolidated UHMWPE powder particles (Collier et al., 1991). A correlation between delamination and these fusion defects has been established (e.g. Wrona et al., 1994). Delamination does sometimes occur in components with a low density of fusion defects (Tanner and Whiteside, 1998). However, Bragdon et al. (1996), in a study of ram-extruded material, showed that what was apparently well-consolidated UHMWPE actually contained numerous micro void-like defects, 10–20 \( \mu \)m in diameter, occupying 20% of freeze-fractured surfaces, and contributing to sub-surface cracking. These defects had little effect on wear of acetabular components, which suggests that it was the combination of the higher stresses in tibial components and the defects which produced the damage. In the light of the present results, it can be argued that the presence of a single defect may not be damaging, even though it could result in local rapid crack propagation. Once away from the immediate influence of the defect, propagation rates would likely reduce to those reported here. A distribution of defects would be necessary, with interactions between cracks growing from several neighbouring defects.
A study of the interaction between voids/inclusions, cracks and the sub-surface stress environment is warranted, and may help to explain some of the occurrences of early delamination. Together with further experimental work on fatigue of UHMWPE (such as that carried out by Connelly et al., 1984; Weightman and Light, 1985; Elbert et al., 1994; Pruitt and Ranganathan, 1995; Pruitt et al., 1995, and others), such studies would help to better quantify the factors which might influence long-term damage to tibial components.

Finally, it should be mentioned that, of the less than 30 reports of metal base-plate fracture, most were caused by eccentric loading due to varus deformity (Abernethy et al., 1996) and/or insufficient bone support of the medial tibial plateau, often in conjunction with the medial tray cantilevered off a fixed central stem (Scott et al., 1984), or with slots between tray and stem (Flivik et al., 1990). The results here regarding stress levels in the metal concur with these findings. Further, it can be surmised from the discussion in Whelen and Little (1992) that results for an unbonded metal tray would not differ greatly from those presented here (i.e. for a bonded metal-backing).

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References


Wu, J.J., Buckley, C.P., O’Connor, J.J. (2001) to be completed.