Equilibrium of crack growth and wear rates during unlubricated rolling–sliding contact of pearlitic rail steel

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Abstract: It is generally accepted that large rolling contact fatigue cracks in rails do not develop during unlubricated rolling–sliding contact, and damage under these conditions is restricted to wear of the rail steel. However, close examination of a worn rail steel surface reveals the presence of a multitude of wear flakes, the roots of which closely resemble shallow rolling contact fatigue cracks.

Experiments have been conducted under unlubricated rolling–sliding conditions to examine the early development of flakes, or cracks, using a laboratory-based, twin-disc test machine to simulate the contact pressure and slip characteristic of the contact between a rail and a locomotive driving wheel. Small defects were found after as few as 125 unlubricated contact cycles. It was found that an equilibrium between crack growth rate and surface wear rate was established after approximately 10 000 cycles, leading to a shallow steady state crack depth. Initial crack growth by ratchetting (accumulation of unidirectional plastic strain until the critical failure strain of the material is reached), followed by shear stress-driven crack growth described by fracture mechanics, was found to be a sequence of mechanisms in qualitative agreement with the observed crack growth and steady state crack depth.

Keywords: wear, rolling contact fatigue, crack growth, rate, pearlitic steel

NOTATION

\( E \) \quad \text{Young's modulus}

\( H \) \quad \text{hardness}

\( k \) \quad \text{shear yield strength}

\( K_{II} \) \quad \text{mode II stress intensity factor}

\( K_{\sigma,t} \) \quad \text{equivalent stress intensity factors for crack growth by tensile and shear mechanisms respectively}

\( N_{k,W} \) \quad \text{number of revolutions during a test of rail and wheel discs respectively}

\( P_0 \) \quad \text{maximum Hertzian contact pressure}

\( P \) \quad \text{contact load per unit length}

\( P_{sh} \) \quad \text{ratchetting stress} = p_0 - P_{sh}

\( P_{sg} \) \quad \text{elastic shakedown limit, defined by equation (4)}

\( P_{sh} \) \quad \text{shakedown parameter, defined by equation (6)}

\( R \) \quad \text{equivalent radius of contact, defined by equation (2)}

\( R_{k,W} \) \quad \text{radii of rail and wheel samples respectively}

\( z \) \quad \text{coordinate normal to the contact surface}

\( \Delta K_{th} \) \quad \text{shear mode threshold stress intensity factor}

\( \tau_{xy} \) \quad \text{orthogonal shear stress beneath a line contact}

1 INTRODUCTION

This investigation into crack growth and the wear of rail steel during unlubricated contact formed the first stage of a project in which the influence of flange lubrication on the fatigue of rail steel was to be investigated [1, 2]. Flange lubrication aims to reduce wear of the rail and wheel, which can be particularly severe in curves owing to the high degree of sliding that takes place when the wheel flange and rail gauge face make contact [3]. Since lubricants are often applied to overcome an existing wear problem, it was decided that, to appreciate fully the effects of applying a lubricant to the rail–wheel contact, it was important first to examine carefully the behaviour of the contact in the absence of any lubrication.

Previous studies of unlubricated contact, conducted by Tyfour et al. [4] and others (for example, references [5] and [6]), have concentrated on the wear behaviour of the rail steel, mainly because it has been generally accepted since the work carried out by Way [7] that rolling contact fatigue (RCF) cracks do not develop under dry conditions.
However, if examined closely, the worn surface of rail steel following repeated dry contact is found to contain a multitude of flakes, each of which could be thought of as the start of an RCF crack. To investigate the development of these flakes it was decided to conduct unlubricated tests, and also to re-examine samples from similar tests carried out by Tyfour et al. [4] which had previously only been analysed to assess wear behaviour of the steel.

The study revealed that very rapid development of small surface cracks occurs during the early stages of unlubricated contact. This was linked to development of surface damage by a ratchetting mechanism [8]. In addition, it was found that, during prolonged periods of unlubricated contact, an equilibrium developed between crack growth and surface removal by wear. This behaviour was found to be in qualitative agreement with a shear mechanism of crack growth described by fracture mechanics. Importantly, knowledge of these mechanisms allows predictions to be made about the possible effects of applying lubricants to a previously unlubricated (or poorly lubricated) rail–wheel contact.

2 PROCEDURE

2.1 Experimental equipment

Tyfour et al. [4] conducted unlubricated tests on rail steel samples with durations in the range of 500–40 000 cycles, from which specimens were available for re-examination. Additional tests have been carried out at very low numbers of cycles to examine the early stages in the development of wear flakes. In their investigation of wear in dry conditions, Tyfour et al. [4] used the LEROS (Leicester Rolling Sliding) contact testing machine to simulate the rail–wheel contact. It was decided to use the SUROS (Sheffield University Rolling Sliding) contact testing machine to simulate the rail–wheel contact. It was determined that the SUROS machine is based on a Colchester Mascot 1600 lathe, adapted for use as a test machine. The specimens are held using machine tool arbours mounted in bearings held by castings secured to the machine bed midway along its length. The rail specimen is driven by the lathe motor and gearbox, while the wheel specimen is driven by a separate 7 kW a.c. motor mounted at the tailstock end of the machine bed. Normal load is applied to the discs by a hydraulic piston, with loading of up to 29 kN available, and is measured by a load cell mounted in line with the piston. Torque due to rolling–sliding contact between the specimens is measured by a transducer secured to the drive shaft of the rail specimen. Disc speeds and numbers of revolutions are measured by shaft encoders fitted in line with the drive shafts. Disc temperature is monitored using a non-contact infrared probe, and air cooling with filtered compressed air is available to keep the disc temperature below 40 °C. Specimens for use on the machine are manufactured to the drawing shown in Fig. 1a, and are removed from the parent rail and wheel components as shown in Fig. 1b. The machine is shown schematically in Fig. 1c.

Throughout each test, a computer-based data acquisition system maintains a record of the time from start of test (s), torque (N m), rail and wheel disc speeds (r/min), rail and wheel disc total revolutions and contact load (kN). From this information the ratio of tractive to normal forces transmitted by the contact can be calculated (the traction coefficient). In addition to this, the computer maintains slip between the discs by adjustment of the speed of the a.c. motor driving the wheel disc specimen.

The LEROS machine, used for the earlier tests by Tyfour et al. [4], was similar to the SUROS machine, with the exception that a 4 kW d.c. motor was used in place of the 7 kW a.c. motor, and less sophisticated control and data acquisition equipment was available. The specimens used were manufactured to the same specification as those used on the SUROS machine, and were prepared and handled using the same procedures.

To ensure that premature failure of the specimens did not occur, both the rail and wheel discs were examined before each test to ensure that no significant defects were present that could affect the results. Wong [11] showed that the standard methods of disc preparation give disc surfaces that, when the discs are sectioned and observed using a scanning electron microscope, are free of plastic deformation, so deformation seen following the tests cannot be attributed to the disc preparation method.

2.2 Contact conditions

The maximum Hertzian contact pressure in the line contact between the test discs is calculated using the equation developed by Timoshenko and Goodier [12] for two elastically identical steel cylinders. Maximum Hertzian contact pressure is given by

\[ p_0 = 0.418 \sqrt{(PE)R} \]

where \( P \) is the load per unit length of the contact, \( E \) is Young’s modulus and \( R \) is defined as

\[ 1/R = 1/R_R + 1/R_W \]

where \( R_R \) and \( R_W \) are the radii of the rail and wheel discs respectively.

The cumulative slip ratio (in per cent), representing the ratio of sliding to rolling distances over the entire test, is defined as
Fig. 1 (a) SUROS test specimen dimensions (in mm), (b) typical locations of test specimens in cross-sections of the wheel and rail and (c) schematic representation of the SUROS machine
Cumulative slip = \[200 \frac{R_N N_R - R_W N_W}{R_N N_R + R_W N_W}\]  

(3)

where \(N\) is the number of revolutions made by the disc, and the subscripts indicate the disc type.

The work carried out by Tyfour et al. [4] took place at a maximum Hertzian contact pressure of 1500 MPa and \(-1\) per cent slip, defined by equations (1) and (3) respectively. The additional tests were also carried out under these conditions, which represent the contact between a locomotive driving wheel and the rail head. Although lubrication of the rail (the overall subject of the investigation) usually takes place on the rail gauge face, it was decided that using conditions characteristic of the rail head would represent the worst case of migration of lubricant on to the rail head, which is where RCF failures are most often initiated.

Table 1 gives the test codes and duration for each test conducted or sample examined, together with information on the wheel steel used in each case, which varied between the tests but had very similar properties in each experiment. The same rail steel was used in all the tests. Table 2 gives the chemical composition and mechanical properties of the test materials. A few of the test materials used in each test (the rail steel used in all the tests was type BS11).

### Table 1 Test identification codes and wheel steels used in each test (the rail steel used in all the tests was type BS11)

<table>
<thead>
<tr>
<th>Test code</th>
<th>Wheel steel</th>
<th>Duration (cycles)</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1</td>
<td>WH268</td>
<td>125</td>
</tr>
<tr>
<td>D2</td>
<td>WH268</td>
<td>250</td>
</tr>
<tr>
<td>D3</td>
<td>WH268</td>
<td>500</td>
</tr>
<tr>
<td>D4</td>
<td>W8A</td>
<td>1000</td>
</tr>
<tr>
<td>D5</td>
<td>W8A</td>
<td>5000</td>
</tr>
<tr>
<td>D6</td>
<td>W8A</td>
<td>10,000</td>
</tr>
<tr>
<td>D7</td>
<td>W8A</td>
<td>17,500</td>
</tr>
<tr>
<td>D8</td>
<td>W8A</td>
<td>40,000</td>
</tr>
</tbody>
</table>

### Table 2 Chemical composition and mechanical properties of the test materials

<table>
<thead>
<tr>
<th>Material Code</th>
<th>Rail BS11</th>
<th>Wheel W8A</th>
<th>Wheel WH268</th>
</tr>
</thead>
<tbody>
<tr>
<td>Chemical composition (wt %)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>C</td>
<td>0.52</td>
<td>0.64</td>
<td>0.55</td>
</tr>
<tr>
<td>Si</td>
<td>0.2</td>
<td>0.23</td>
<td>0.27</td>
</tr>
<tr>
<td>Mn</td>
<td>1.07</td>
<td>0.71</td>
<td>0.75</td>
</tr>
<tr>
<td>Ni</td>
<td>0.03</td>
<td>0.17</td>
<td>0.20</td>
</tr>
<tr>
<td>Cr</td>
<td>&lt;0.01</td>
<td>0.18</td>
<td>0.26</td>
</tr>
<tr>
<td>Mo</td>
<td>&lt;0.01</td>
<td>0.03</td>
<td>0.08</td>
</tr>
<tr>
<td>S</td>
<td>0.018</td>
<td>0.041</td>
<td>0.018</td>
</tr>
<tr>
<td>P</td>
<td>0.013</td>
<td>0.023</td>
<td>0.011</td>
</tr>
</tbody>
</table>

Mechanical properties prior to testing:

<table>
<thead>
<tr>
<th>Property</th>
<th>Rail BS11</th>
<th>Wheel W8A</th>
<th>Wheel WH268</th>
</tr>
</thead>
<tbody>
<tr>
<td>Average hardness (HV 500 g)</td>
<td>245</td>
<td>275</td>
<td>270</td>
</tr>
<tr>
<td>Ultimate tensile strength (MPa)</td>
<td>781</td>
<td>857</td>
<td>906</td>
</tr>
<tr>
<td>Tensile yield strength (MPa)</td>
<td>406</td>
<td>325</td>
<td>573</td>
</tr>
<tr>
<td>Shear yield strength (MPa)</td>
<td>203</td>
<td>163</td>
<td>n/a</td>
</tr>
<tr>
<td>Total plastic elongation (%)</td>
<td>21.5⁴</td>
<td>20.3³</td>
<td>14³</td>
</tr>
<tr>
<td>Young’s modulus (GPa)</td>
<td>209</td>
<td>199</td>
<td>n/a</td>
</tr>
</tbody>
</table>

⁴2% proof stress. ⁵Derived from tensile yield strength using the Tresca criterion. ⁶Gauge length 10 mm. ⁷Gauge length 69 mm. n/a = not available. Data for BS11 and W8A steel taken from Tyfour et al. [4]. Data for WH268 steel supplied by Adiranz (1996, personal communication).

The load applied to the rail by the wheels of passing trains leads to yield of the steel near to the surface of the rail, which is apparent from inspection of rails in service, without the need for any stress analysis [13]. Kapoor and Johnson [8] show that plastic deformation of the rail surface will take place during the initial wheel passes at loads above the yield strength of the rail steel. However, elastic material surrounding the highly stressed contact region restricts the extent of plastic flow, and the increment of plastic strain with each successive contact pass is reduced as the material undergoes ‘shakedown’. Shakedown to a completely elastic state is only possible for loads below the elastic shakedown limit of the rail. Loads above this limit but below the plastic shakedown limit of the steel will lead to cyclic plasticity of the rail, and above this limit continued incremental increases in rail steel plastic deformation (ratchetting) will occur [8].

The ductility of the rail steel is finite. Hence, as ratchetting takes place, steel reaching the limit of its ductility undergoes ductile fracture and breaks away from the main body of the steel. Ratchetting therefore forms a link between a rail surface that is free of significant surface damage and one that contains cracks large enough for growth to take place that may be described by fracture mechanics, i.e. large enough for the stress intensity factor (SIF) range at the crack tip to exceed the appropriate threshold stress intensity range. Ratchetting must therefore be the starting point of crack development in almost all cases, except for those in which a large pre-existing defect is present, or when the surface material fails by a low-cycle fatigue mechanism [14]. Under typical rail–wheel contact conditions, ratchetting rather than low-cycle fatigue was found to be the dominant damage mechanism during twin-disc tests conducted by Tyfour and Beynon [15] to investigate the effects of rolling direction reversal on the wear rate of pearlitic rail steel.

To examine ratchetting of rail steel, a method similar to
that devised by Tyfour et al. [16] was applied. For ratchetting to occur, the applied contact pressure must exceed the plastic shakedown limit (ratchetting threshold) which in the case of line contact is found to equal the elastic shakedown limit, \( P_{sh} \) (A. Kapoor, 1999, personal communication) [17]. For alternative contact geometries this is not necessarily the case, and direct calculation of the plastic shakedown limit (ratchetting threshold) may be required. The elastic shakedown limit for a rolling–sliding Hertzian line contact is given by an expression whose derivation is described in detail by Kapoor and Williams [18]:

\[
P_{sh} = \left( \frac{p_0 k}{(\tau_{x_{max}})_{z_{\min}}} \right)_{\text{min}}
\]

(4)

where \( k \) is the shear yield strength of the material, \((\tau_{x_{max}})_{z}\) is the maximum absolute value of the orthogonal shear stress at any depth \( z \) below the rail surface, and \( p_0 \) is the maximum Hertzian contact pressure.

Since ratchetting near or at the surface is particularly important in the initiation of cracks, it is useful to examine the variation with depth in the shakedown parameter, \( P_{sh} \), where this represents the maximum Hertzian contact pressure that may be applied before ratchetting will take place at the depth considered. In calculating this parameter it is possible to include the variation in shear yield strength with depth, which results from plastic flow of the steel, and which is indicated by a variation in hardness with depth below the contact surface. Since shear yield strength is a linear function of hardness [19], the shear yield strength \( k_z \) at any depth \( z \) may be found following measurement of material hardness in tested samples, and is given by

\[
k_z = \frac{H_z}{H_{\text{core}}} k_{\text{core}}
\]

(5)

where \( H_z \) is the measured hardness at depth \( z \), \( H_{\text{core}} \) is the hardness of the undeformed material and \( k_{\text{core}} \) is the shear yield strength of the undeformed material. Substituting \( k_z \) for \( k \) in equation (3) and removing the requirement to find the minimum of the value with variation in \( z \) gives the new shakedown parameter \( P_{sh} \):

\[
P_{sh} = \frac{p_0 k_z}{(\tau_{x_{max}})_{z}}
\]

(6)

Parameter \( P_{sh} \) indicates the variation with depth in the maximum contact pressure that may be applied to the contact (following the number of contact cycles prior to the hardness measurement) without producing additional plastic deformation of the steel. For a non-strain hardening material, or when hardness data are not available, \( k_z \) may be taken as a constant for all depths. In the current series of experiments, hardness measurements were made on rail steel sections taken after tests D1 and D2, while data were available from Tyfour et al. [4] for tests D3 to D8. Since hardness measurements very close to the surface were not available (owing to the inaccuracy of measurements very close to the disc surface when made on the sectioned specimen), the conservative approximation was made that the hardness of material between the surface and the first measured point would equal the hardness at that first measured point.

Ratchetting will occur at a particular depth when \( P_0 \) exceeds \( P_{sh} \) at that depth, and the severity of the stress driving the ratchetting process may be assessed from the magnitude of the ratchetting stress, \( P_{rh} \), defined as the difference between \( P_0 \) and \( P_{sh} \). Stress \( P_{rh} \) indicates the rate at which plastic deformation by ratchetting will take place. An increase in \( P_{rh} \) will lead to an increase in the plastic deformation per cycle [20, 15].

The orthogonal shear stress required in the above calculations can be determined either by the Hertzian model of smooth surface contact, or by use of a rough surface contact model. Examination of the stresses predicted by the elastic rough surface contact model developed by Sayles [21] for the un lubricated contact of SUROS test discs was carried out. This revealed that, for the cases examined, the surface roughness produced very high stresses that would not be predicted by a smooth surface contact model, and that would be likely to produce significant plastic flow only within a few micrometres of the rail disc surface [22]. For the bulk of the steel, the results from a smooth surface Hertzian contact model are therefore adequate to describe the subsurface stress, and the orthogonal shear stress required in equation (6) was calculated using the Hertzian model presented in ESDU item 85007 [23]. The assumption was made that the maximum traction coefficient of 0.43 reached during a typical un lubricated test would apply in all cases, despite some variations between individual tests.

### 2.4 Crack growth by a shear mechanism

Kaneta et al. [24] considered growth of an existing crack by a shear mechanism driven by the traction at the contact surface and controlled primarily by the level of crack face friction (i.e. the ease with which the crack faces may slide over one another). Although un lubricated rolling–sliding contact produces a complex mixed mode sequence of stress intensities at the crack tip, Kaneta et al. [24] showed that an equivalent stress intensity can be found on the plane normal to the maximum tensile hoop stress adjacent to the crack tip (equivalent tensile stress intensity, \( K_{eq} \)) and also in the direction of the maximum radial shear stress adjacent to the crack tip (equivalent shear stress intensity, \( K_s \)). It was shown that these equivalent SIFs can be calculated by application of the equations developed by Erdogan and Shi [25] at every point in the mixed mode loading cycle. For crack growth under un lubricated conditions, controlled by surface and crack face friction coefficients, it was found that growth is always by shear and takes place along the line of the original crack. The simplification can therefore
be made for this case that $K_T = K_{II}$ and $K_{I}$ need not be considered.

Recently, a simple method has been developed [26] for prediction of SIFs at the tip of an inclined semi-circular crack beneath an unlubricated rolling–sliding contact. The method includes consideration of both the surface and crack face friction coefficients, and makes use of the Green’s functions developed by Rooke et al. [27] to calculate the mode II SIF found by Kaneta et al. [24] to be equal to $K_T$. When using the same boundary conditions used by Kaneta et al. [24] it can be shown [26] that the method predicts stress intensities that are in good agreement with the previously published data. It was therefore decided to combine this new method of SIF calculation with the previously developed application method used by Kaneta et al. [24] and to predict the cycles of SIF $K_T$ to which the cracks were subjected during the current series of experiments. Although crack growth angles varied between the tests, for calculation of $K_T$, the growth angle was taken as 30°, the shallowest angle for which the Green’s functions on which the method is based are available. A threshold stress intensity factor, $\Delta K_{th}$, for growth of cracks in carbon steel was measured by Otsuka et al. [28] for shear mode crack growth of the type predicted by the model. Although the value is not specific to the steel used in the current series of experiments, it was thought sufficient for indicating the possibility of crack growth, and also to put in context the values of $\Delta K_T$ predicted by the shear crack growth model.

3 RESULTS

3.1 Variation in crack size with the number of unlubricated contact cycles

Data in Table 3 show the development of flake, or crack, size during periods of unlubricated contact. The length was measured as a straight line from the tip to the mouth of the longest crack observed and, similarly, the depth was taken as the vertical distance from the disc surface to the crack tip of the longest crack observed. In most of cases there were found to be several cracks with dimensions approximately equal to those of the longest crack, suggesting that the measurements taken from this crack were reasonably representative of cracks throughout the whole disc.

Both crack length and depth showed a general trend to increase in magnitude over the first 10 000–15 000 cycles and then to remain approximately constant. Since disc surface wear was not taken into consideration when measuring crack sizes, this apparently constant crack size implies that crack growth was taking place at a rate that compensated for removal of the disc surface material by wear. It was found that exponential relationships could be fitted to the length and depth dimensions:

\[
\text{Length} = 1100(1 - e^{-N/4000})
\]

\[
\text{Depth} = 43(1 - e^{-N/4000})
\]

However, since there was no obvious physical basis for such relationships, their use was not pursued.

Figures 2 and 3 show the cracks observed after tests D1 and D8, the shortest and longest tests respectively, which illustrate the range of defect sizes observed. The majority of the cracks were reasonably straight for the majority of

<table>
<thead>
<tr>
<th>Test code</th>
<th>Test duration, including dry phase (rail cycles)</th>
<th>Rail disc mass change per cycle (µg/rail cycle)</th>
<th>Rail disc diameter change (nm/rail cycle)</th>
<th>Wheel disc mass change per cycle (µg/wheel cycle)</th>
<th>Wheel disc diameter change (nm/wheel cycle)</th>
<th>Maximum crack length observed in rail disc (µm)</th>
<th>Maximum crack depth observed in rail disc (µm)</th>
<th>Approximate growth angle at crack mouth (deg)</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1 122</td>
<td>4.9</td>
<td>329.0</td>
<td>4.1</td>
<td>81.8</td>
<td>13</td>
<td>4</td>
<td>20</td>
<td></td>
</tr>
<tr>
<td>D2 249</td>
<td>3.6</td>
<td>80.4</td>
<td>1.6</td>
<td>40.0</td>
<td>73</td>
<td>6</td>
<td>2</td>
<td></td>
</tr>
<tr>
<td>D3 499</td>
<td>1.0</td>
<td>20.9</td>
<td>3.2</td>
<td>0.0</td>
<td>61</td>
<td>11</td>
<td>10</td>
<td></td>
</tr>
<tr>
<td>D4 1000</td>
<td>2.9</td>
<td>5.0</td>
<td>0.1</td>
<td>5.0</td>
<td>60</td>
<td>11</td>
<td>15</td>
<td></td>
</tr>
<tr>
<td>D5 5000</td>
<td>7.0</td>
<td>5.0</td>
<td>7.6</td>
<td>5.0</td>
<td>528</td>
<td>30</td>
<td>5</td>
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<tr>
<td>D6 10000</td>
<td>13.4</td>
<td>4.5</td>
<td>16.8</td>
<td>3.0</td>
<td>751</td>
<td>43</td>
<td>5</td>
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<tr>
<td>D7 17500</td>
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<td>35.8</td>
<td>9.2</td>
<td>1226</td>
<td>43</td>
<td>5</td>
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</tr>
<tr>
<td>D8 40000</td>
<td>15.5</td>
<td>5.0</td>
<td>41.4</td>
<td>9.9</td>
<td>1022</td>
<td>43</td>
<td>5</td>
<td></td>
</tr>
</tbody>
</table>

*Test carried out by Tyfour et al. [4].
their length, a feature characteristic of growth by a shear mechanism [29], although the plastic deformation of the surface layers of the disc may also have influenced the crack growth direction, since this material was not isotropic following the deformation. In several cases, particularly in the longer tests, cracks were found to have developed from the roots of large surface flakes. The angle at which crack growth took place varied in the range of approximately 5–20°.

3.2 Wear and disc diameter reduction

The majority of the tests discussed here have previously been discussed from the point of view of wear by Tyfour et al. [4]. The important additional results presented here are for the very short tests, D1 and D2. It was found that the wear rate of both the rail and wheel discs in these short tests was comparable with that found over the first 5000 cycles of wear tests carried out by Tyfour under similar conditions, and this is shown by the data in Table 3. Rail disc diameter change per cycle during the very short tests initially appeared to indicate a trend for very large diameter changes during the early cycles, followed by more stable reduction per cycle after 500–1000 cycles (Table 3). However, more careful examination of the data showed that the rail disc in test D1 lost a greater amount from its diameter than did the rail disc in test D2, a test that lasted twice as long as test D1. It therefore appears that scatter in the results is too large for any trend to be identified at these relatively low levels of wear. The difficulty in measuring the very low rail disc diameter losses such as those in tests D1 (0.04 mm) and D2 (0.02 mm) was thought to be responsible for this. For similar reasons it was decided not to rely on the wheel disc diameter measurements from these tests.

3.3 Rail surface plastic deformation

Although reported by Tyfour et al. [4], the additional results from tests D1 and D2, together with the shakedown curves now available for each test, justify presentation of a summary of the evolution of the rail surface plastic deformation during unlubricated contact. Concentrating on the very short tests, Figs 4a to c show the plastic deformation at the rail disc surface following tests D1 to D3 respectively. Figure 4d shows the deformation present after the much longer test, D8. Figure 5 shows the shakedown curves corresponding to tests D1 to D3 and D8.

By comparison of Figs 4a to d with Fig. 5, the relationship between the application of a maximum Hertzian contact pressure, \( P_0 \), exceeding the shakedown parameter \( P_{sh} \) and the production of significant plastic flow in the rail material can be seen. The surface level (\( z = 0 \)) for the plot in Fig. 5 is taken to equal the surface level of the discs at the end of each test. Since, in each case, Figs 4a to d reveal plastic deformation using an optical micrograph at a magnification of less than 200, it is reasonable that the shakedown parameters were calculated using the smooth surface Hertzian rather than rough surface contact stress results.

Figure 4a shows that considerable plastic flow has taken place within the upper 200 µm of the rail steel after only approximately 125 cycles of unlubricated contact. Although material below 200 µm shows no noticeable plastic flow, the hardness curve for this test (Fig. 6) shows an increase in hardness that reaches to a depth of around 400 µm, indicating that some plastic flow has taken place to this depth. A reduction in plastic flow with depth is to be expected since Fig. 5 shows that the shakedown parameter for test D1, \( P_{shD1} \), approaches the applied contact pressure, \( P_0 \), as depth increases.

Figure 4b shows that, after approximately 250 unlubricated cycles, considerably more plastic flow has taken place within the upper 200 µm of the rail sample after only approximately 125 cycles of unlubricated contact. Although material below 200 µm shows no noticeable plastic flow, the hardness curve for this test (Fig. 6) shows an increase in hardness that reaches to a depth of around 400 µm, indicating that some plastic flow has taken place to this depth. A reduction in plastic flow with depth is to be expected since Fig. 5 shows that the shakedown parameter for test D1, \( P_{shD1} \), approaches the applied contact pressure, \( P_0 \), as depth increases.

Figure 4c shows a larger area of the section taken after
Fig. 4  Optical micrographs of etched rail disc sections, revealing plastic deformation of the steel. Surface traction acted from left to right. Note the differences in scale between the micrographs: (a) test D1 (125 cycles); (b) test D2 (250 cycles); (c) test D3 (500 cycles); (d) test D8 (40,000 cycles)
test D3, relative to the micrographs for tests D1 and D2. Plastic deformation is visible to a depth of approximately 0.4 mm, but the hardening curves (Fig. 6) again show that material to a much greater depth has hardened due to plastic deformation, in this case to around 1.2 mm deep. It is shown by Fig. 5 that $P_{\text{shD3}}$ reaches zero at approximately 0.8 mm below the surface, so hardening below this depth must have occurred during the initial plastic flow, which is an inherent part of the shakedown process.

The trend for increasing plastic flow and shear strain accumulation continues throughout the remainder of the tests, as does the hardening due to the plastic flow of the

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**Fig. 5** Shakedown parameters for unhardened rail steel, $P_{\text{sh}}$, and for tests D1 to D3 and D8 (indicated by additional subscripts) and applied contact pressure, $P_0$. $P_{\text{sh}}$ is defined by equation (6) and $P_0$ is defined by equation (1).

**Fig. 6** Rail disc hardness variation with depth following various numbers of unlubricated contact cycles.
steel, which produces an increase in $P_{sh}$ (at any depth) with the increasing number of cycles. Figure 5 shows that by 40 000 cycles (test D8) the shakedown parameter $P_{shD8}$ at the surface has increased to approximately 1300 MPa from 507 MPa for the unhardened material, so the stress difference driving further deformation has been reduced. Figure 4d shows the pattern of plastic deformation observed following test D8, which was found to be little different to that observed following test D7 (17 500 cycles of contact). This reduction in the rate of plastic deformation relative to that during the early cycles of the tests (revealed by tests D1 to D3) corresponds to the reduction in ratchetting stress, $P_{rh}$, with increasing numbers of cycles. The period from 17 500 to 40 000 cycles is also the period in which Tyfour et al. [4] identified the onset of steady state wear. The combination of a steady state wear process and the accumulation of plastic strain in the surface layers of the disc was discussed by Tyfour et al. [4], and that discussion will not be repeated here.

### 3.4 Traction coefficients

The plot of traction coefficient for test D8 (Fig. 7) shows the complete dataset of which each of the shorter tests is effectively a subset (the traction behaviour is very repeatable). The traction coefficient plot for test D3 is included as an inset in Fig. 7 and illustrates the rapid changes in traction coefficient that take place during the earliest cycles of a test. The rate of change in the latter part of the longer tests is much lower, with the traction coefficient tending to fall as the number of cycles increases.

Because of the way the traction coefficient evolves, short periods of dry contact include a disproportionately high number of cycles at a lower, less damaging, traction coefficient, and this will almost certainly influence the accumulation of damage and rate of crack development during dry contact. However, this was not taken into account when examining the results of the current test series since its consideration would not give any additional information about the defects that exist after a given number of unlubricated contact cycles.

### 4 DISCUSSION

After prolonged periods of unlubricated contact, both crack length and depth are found to stabilize, leaving a disc surface containing long shallow cracks. Observed crack depth at steady state was typically around 15 per cent of the contact half-width, while the observed length was found to reach up to 400 per cent of this contact dimension.

While crack length and depth measured relative to the disc surface appear to be stable after 10 000—15 000 cycles, this stability is in fact the result of equilibrium between the processes of crack growth and surface removal by wear. To examine this it was decided to plot the crack tip position and surface position relative to the centre of the disc (Fig. 8). The reduction in surface radial distance was calculated by assuming that the measured wear loss in each test was due to the removal of a layer of appropriate uniform thickness around the circumference of the disc. This was thought to be more accurate than using the measured disc diameters from each test since diameter measurements were known to be unreliable for the shorter tests, and were thought to be influenced by the development of surface flakes during the longer tests. In practice, the reduction in diameter would exceed that predicted by wear loss because side flow of material is found to occur during the tests (Fig. 9). However, since this flow was thought to contribute reasonably equally to change in both surface radial position and crack tip distance from the centre of the disc, it was
neglected. Wear of the surface contributes only to surface position change and not to crack tip movement and is therefore of much greater significance.

From Fig. 8 (which shows crack depth for clarity but could also have shown crack length) it can be seen that after approximately 10 000 cycles the difference between crack tip position and surface position, i.e. the crack depth, is almost constant (see Table 3). However, both the surface and crack tip are continually moving towards the centre of the disc as the number of contact cycles increases. The steady state crack depth observed is due to the establishment of an equilibrium between surface wear rate and crack growth rate.

The explanation of why cracks do not extend more rapidly than the surface is wearing away is thought to lie in the stress state that exists below the rail disc surface during the passage of the contact. The steady state crack tip depth of over 40 \(\mu\)m is below the depth at which subsurface stress is influenced significantly by individual asperity contacts and the behaviour of these cracks may therefore be investigated using a model based on the smooth surface Hertzian contact results. It was noted that the crack growth direction predicted by the shear mode of crack growth (Section 2.4) is consistent with the straight crack path seen following initial surface failure by ratchetting during unlubricated contact.

While remembering that the cracks lie in plastically deformed and non-isotropic material, the range of the stress intensity factor, \(\Delta K\), for the cracks is the best available descriptor of their behaviour. Owing to the limited Green's functions available for use with the shear crack growth model, the shallowest cracks to which it may be applied are at 30\(^\circ\) below the surface. This is steeper than is typical of cracks found in unlubricated contact, but the results are useful since they do give some indication of the variation in \(\Delta K\) with crack length. In addition, the trends in \(\Delta K\) revealed by the model for changes in surface and crack face friction coefficients may be used to make predictions about the behaviour of cracks even at angles below 30\(^\circ\). The model may therefore be useful in predicting crack behaviour should, for example, a flange lubricant be applied to a previously worn rail.
Variation in $\Delta K_e$ with crack length (radius) for a semicircular crack traversed by a Hertzian line contact of 1500 MPa maximum pressure is presented in Fig. 10. Both surface and crack face friction coefficients of 0.43 were chosen, representing the maximum value found at the surface and an estimate of the crack face friction coefficient respectively. If the crack face friction coefficient were higher than this estimate, $\Delta K_e$ would fall for any crack length considered, and conversely it would rise if a lower crack face coefficient of friction were to occur.

From Fig. 10 it can be seen that, even for very small cracks (length 6 μm, ratio of length to contact half-width 0.02), the threshold stress intensity factor, $\Delta K_{th}$, of 1.5 MPa m$^{1/2}$ given by Otsuka et al. [28] is exceeded, and growth is therefore predicted. Cracks of this length may also be subject to high stress owing to asperity contacts, but since Fig. 10 indicates that the smooth surface contact stress is sufficient to produce crack growth, this possibility was not considered further. As crack length increases, $\Delta K_e$ initially rises but then falls back towards $\Delta K_{th}$, and, assuming variation in crack growth rate with the change in SIF range, the crack growth rate would be expected to show a similar variation with crack length. If the pattern of a decreasing growth rate with increasing crack length, for cracks beyond a certain length, were to be repeated for a very long shallow crack, it would be consistent with the observed crack behaviour. A negative slope to the growth rate curve would explain how a stable equilibrium crack length, growth rate and surface wear rate could be achieved, preventing cracks growing beyond a few tens of micrometres in depth during unlubricated (high surface wear) contact. However, the picture is complicated since the effect on $\Delta K_e$ of reducing the crack inclination angle is not known, the crack face friction level can only be estimated and the influence of the anisotropy of the material has not been considered. These issues, particularly the influence of material anisotropy on crack growth, are topics on which further research is required.

5 CONCLUSIONS

Re-examination of specimens available from previous dry wear tests, and several further tests, have allowed the description of crack depth and length evolution during dry contact of pearlitic rail steel with wheel steel. It is found that small surface defects are present after as few as 125 unlubricated contact cycles, and these develop to form long shallow cracks following the accumulation of plastic strain in the surface layers of the rail steel. The accumulation of strain can be described as a ratchetting process following shakedown of the rail steel after initial plastic flow. The observed steady state crack length and depth are the result of equilibrium between material removal from the rail surface by wear and continuing crack growth at the crack tip. The inability of the cracks to extend to depths over approximately 15 per cent of the contact half-width
qualitatively agrees with a shear mechanism of crack growth.

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