Modelling wear and crack initiation in rails

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Abstract: Wear and crack initiation of steel rails is a problem of great significance to the railway industry. A high wear rate shortens the life of rails and the frequent rail replacement is expensive in terms not only of resources but also of track access time and delays affecting timetables. In addition, railhead profiles change gradually as the rails are worn and the greater the wear rate, the more often the rails need to be reground to maintain good train running performance. In contrast, a low wear rate means that cracks have time to develop in the plastically deformed rail steel and these may propagate deeper into the rails with potentially disastrous consequences. Finding the optimum combination of wear and grinding to maintain railhead profile and prevent cracks from growing is key to running a safe and cost-efficient railway.

The large number of variables arising from track geometry, train dynamics, and wheel and rail profiles leads to wide variation in contact patch size and location. The two-dimensional model of ratcheting wear developed by Kapoor et al. has been developed to model the damage accumulation near the surface of the rail on the basis of a full three-dimensional contact stress distribution. Different rail steel microstructures can also be modelled and the effects of microstructure on wear and crack initiation are explored.

Keywords: computer simulation, ratcheting failure, wear, crack initiation, rail–wheel contact

1 INTRODUCTION

During their operational life, rails typically experience millions of load cycles from the passage of train wheels. How the rails respond to this depends primarily on the type of traffic, the geometry and substructure of the track, and the type of rail steel. Wear is inevitable, leading to slight changes in the railhead profile – and consequently, sometimes, significant changes in vehicle behaviour. Rails need to be reground periodically to maintain good train running performance, and if the loss of material from the railhead through wear and grinding is too great, the rails need to be replaced. Grinding and rail replacement are expensive in terms not only of resources but also of track access time and delays affecting timetables.

Another major problem affecting rails, especially on curves where the track forces are higher, is cracking. There are several different types of cracks (tache ovaux, squats, etc.) associated, for example, with welds or defects in rails. Gauge corner cracks are generally associated with a particular location, often several cracks occurring together with an interval of a few millimetres, and reoccur after grinding or even rail replacement. These cracks initiate at the surface and propagate deeper into the rail.

The growth of surface-breaking cracks is linked to the wear rate. If the wear rate is greater than the crack propagation rate, then the crack will shorten and, eventually, disappear. One of the benefits of frequent grinding is that short cracks are removed; however, depending on the wear rate, not all short cracks grow into long cracks and unnecessary grinding ultimately increases the costs of running the rail network.

The ability to predict where cracks will occur and grow to dangerous lengths, and to predict where, how much and how often to grind, would aid network management and reduce the costs of rail maintenance.

With this aim, the ‘brick’ model, described subsequently, is a tool that is being developed to predict...
the wear rate and the potential for crack initiation. Two major new developments to the model are described here. First, mechanical properties of multiple materials can be defined and a microstructure can then be simulated which consists of regions of specified materials; this is used below to construct a model of pearlitic rail steel microstructure. Second, previous investigations with the ‘brick’ model were restricted to line contact (e.g. twin-disc contact). Wheel–rail contact cannot be reduced to a two-dimensional model, but the contact patch can be approximated as elliptic. This has now been implemented, so the ‘brick’ model can take contact data from vehicle dynamics simulations of train–track interaction.

2 THE ‘BRICK’ MODEL

Analysis of cross-sections of rails, which have been in service, show that material close to the contact surface of the railhead has accumulated considerable plastic shear strain. Depending on the type of rail steel and the actual loading conditions, the thickness of this deformed layer varies from a few microns to a few millimetres.

How a ductile material (such as rail steel) responds to a repeated cycle of load depends on the magnitude of the load. If it is beneath the elastic limit of the material then the load is supported completely elastically. If the load is higher than the elastic limit but less than the material’s elastic shakedown limit then there will be some plastic deformation during early cycles, during which the material will accumulate protective residual stresses and may harden, but thereafter the load will again be supported elastically. In either case, the life will be very long and failure will occur most likely by high cycle fatigue.

If, however, the load is greater than the elastic shakedown limit, then there will be some plastic flow with each load cycle. If the load is less than the plastic shakedown limit then the cycle of plasticity is closed and there will be no accumulation of plastic strain and failure will occur by low cycle fatigue (LCF). If the load exceeds the plastic shakedown limit then plastic strain will accumulate – a process called plastic ratcheting. For this reason, the plastic shakedown limit is also called as the ratcheting threshold. Plastic ratcheting can lead to extrusion of thin slivers of material from the edges of the contact region (and this causes ‘lipping’ of rails); ratcheting is also the mechanism causing the large plastic shear strains observed in the near-surface material of the railhead. Both types are discussed by Kapoor [1].

Material cannot accumulate strain indefinitely. Ratcheting failure is the process whereby material accumulates strain up to some critical value, at which point the ductility of the material is exhausted and the material ‘fails’. Material subject to ratcheting can also fail by LCF. Kapoor [2] hypothesizes that the two failure mechanisms are independent and that failure will occur by whichever mechanism results in the shorter life, although it is also possible that the two mechanisms are additive, in which case life can be determined by using a summative rule such as Miner’s.

Tyfour et al. [3, 4] studied plastic ratcheting of pearlitic rail steel in a series of twin-disc tests. There is an initial period during which plastic strain accumulates in initially undeformed material and during which there is no significant wear. Later, as the profile of strain with depth steadies, so does the wear rate. Changing the rolling direction, and thus the direction of traction, changes the direction in which strain accumulates, and the wear rate drops [5]. Oxley and coworkers [6] have used Coffin–Manson’s LCF equation to estimate the wear rate of material being traversed by a wedge-shaped asperity. A similar approach is used by Torrance’s group [7, 8]. However, these approaches, unlike plastic ratcheting, are insensitive to the direction of strain accumulation.

This process of ratcheting leading to wear is the basis of the ‘layer’ model, described in detail by Kapoor and Franklin [9], and the ‘brick’ model, described in detail by Franklin et al. [10, 11].

In twin-disc tests, which simulate the effect of a driving wheel against a rail, the direction of traction is opposite to the direction of motion, as shown in Fig. 1, but this is not necessarily the case in rail–wheel contact where there can also be a component of lateral traction. A cross-section through the rail (or disc), parallel to the direction of traction, is modelled as a mesh of elements (or ‘bricks’). Each element

![Fig. 1](Image 300x184 to 538x274)
is assigned material properties, according to some description of microstructure, such as initial shear yield stress ($k_0$) and critical shear strain for failure ($\gamma_c$).

The model is driven by repeated application of a load, and it is the orthogonal shear stress ($\tau_{zx}$, the shear stress in the $zx$-plane, where $x$ is the direction of the applied traction and $z$ is depth into the material) which is of interest. For plastic flow to occur, the load has to be above the shakedown limit, in which case the pattern of residual stresses within the material is such that the orthogonal shear stress is the dominant stress. In particular, it is the maximum absolute value of $\tau_{zx}$ occurring at a given depth which is used: $\tau_{zx(max)}$.

The model allows elements to harden as strain accumulates. Associated with each element, therefore, are the total accumulated shear strain of the element ($\gamma$) and the effective shear yield stress ($k_{0eff}$). With each load cycle, there will be an increment of plastic shear strain ($\Delta \gamma$) if the maximum orthogonal shear stress in an element exceeds the effective shear yield stress. As shear strain accumulates, so the effective shear yield stress increases; a modified Voce equation with two parameters ($\alpha$ and $\beta$) is used to calculate the new value. (This equation was chosen as suitable for fitting to data – shear strain and hardness measurements – from twin-disc tests; a recent analysis using a data – shear strain and hardness measurements – equation was chosen as suitable for fitting to $JRRT60$.)

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The material is discretized into a regular $Nx \times Nz$ mesh of equal-sized ($dx \times dz$) rectangular elements, so that the width and depth of the simulated area are $Nx \cdot dx$ and $Nz \cdot dz$, respectively. (The simulation is periodic in $x$, so that properties at $x + Nx \cdot dx$ are equivalent to properties at $x$, and, for the simulations below, the elements are square, i.e. $dx = dz$.) Each load cycle, the following sequence is evaluated for each element ($ij$) in row $j$ ($j = 1, \ldots, Nz$) and column $i$ ($i = 1, \ldots, Nx$)

$$
\frac{k_{ij}^e}{k_0} = k_0 \max \left\{ 1, \beta \sqrt{1 - e^{-\alpha \gamma}} \right\}
$$

$$
\Delta \gamma^j = C \left[ \frac{\tau_{zxj(max)}^j}{k_{ij}^e} - 1 \right]
$$

$$
\gamma^j = \gamma^j + \Delta \gamma^j
$$

where $\tau_{zxj(max)}$ is the maximum orthogonal shear stress occurring at the depth of row $j$, and $C$ is a constant estimated as 0.00237 for BS 11 rail steel by Tyfour et al. [4].

An alternative formulation of ratcheting strain accumulation has been developed by Su and Clayton [13], based on rolling/sliding contact of pearlitic steel, in which the total ratcheting strain is a function of the maximum shear stress. Parameters are estimated for a specific case of water-lubricated contact and a slide/roll ratio of 10 per cent. These experiments showed that slide/roll ratio significantly affects the ratcheting strain rate during the first 30 000 cycles [14]. The slide/roll ratio can affect the stress distribution [11] but can also influence the ratcheting strain rate through its effect on thermal stresses and softening of the material because of heat generated at the contact [15].

### 2.1 Wear

The ‘brick’ model is not a finite element model as the elements, or bricks, are mostly independent of one another. They accumulate shear strains at different rates, which is not physically correct, but by relaxing this physical constrain the model can approximate the behaviour of microstructural features over hundreds of thousands of load cycles in the space of a few hours. (In practice, the variation of material properties across the microstructure will give rise to a local distribution of applied and residual stresses which will affect how plastic flow occurs. Currently, the stress distributions calculated by the ‘brick’ model assume a homogeneous material and neglect, therefore, the effect of microstructure.) The elements reach their critical shear strain, and ‘fail’, at different times and are marked as weak.

In the earlier ‘layer’ model, the top layer was removed as wear debris as soon as it failed. In the ‘brick’ model it is possible to have a weak element with healthy (i.e. non-failed) elements around. In this case, it is difficult to argue that weak elements should be removed automatically from the simulation as wear debris; instead a number of patterns (called heuristics) of weak/healthy bricks are designated as scenarios in which an element may be removed. These are shown in Fig. 2 (there is one additional heuristic, $i$, given in reference [11] which is not used in the current model). These heuristics were chosen with a constant direction of shear in mind.

![Fig 2](image)

Elements can be healthy, weak, or non-existent. The grey elements in this figure are non-specific; they can have any of the three states. The black elements are weak. The uncoloured elements are non-existent, i.e. they have been removed as wear debris.
The elements exposed to the surface are scanned each cycle for patterns of failed and removed elements which match one of the heuristics, and if a match is found then there is probability \( p \) that the central element of the \( 3 \times 3 \) group is removed as wear debris. (For example, if a particular element matches one of the patterns in Fig. 2, and if \( p = 0.2 \), then it will take, on average, 5 cycles to remove that element.) The effect of varying this probability was examined in reference [11].

One major change in the current model is that the probability \( p \) of brick removal is related to the thickness, \( dz \), of the element, the argument being that the larger the physical size of the brick, the more cycles needed to remove it as wear debris. This is important because the elements in the model are usually chosen to be \( 1 \times 1 \mu\text{m}^2 \) (or similar) in size, and measured wear rates of pearlitic rail steel are on the order of 1 nm/cycle. Modelling microstructure with grains sized \( \sim 50-100 \mu\text{m} \) on a nanometre scale is not computationally feasible.

In the current model, the probability \( p \) is defined as

\[
p = \frac{W}{dz} \tag{4}
\]

where \( W \) is a reference wear rate which can be used if necessary to calibrate the model and is set currently to the value 10 nm/cycle. This change to the model will affect simulations where the wear rate saturates, i.e. at values close to (or above) \( W \). In effect, the maximum wear rate the model can predict is approximately \( 1.5 \times W \).

However, by the time this becomes an issue, ratcheting failure is unlikely to be the only mechanism of wear acting on the material. The various ratcheting parameters have been calibrated against the results of twin-disc tests in which the slide/roll ratio is low, typically \( \sim -1 \) per cent (simulating a driving wheel), and the wear rates are typically 1 nm/cycle or less – significantly less than the above saturation value. As the slide/roll ratio increases, such as flange contact of wheel and rail, or wheel slip, thermal effects become increasingly important [15], and the raised temperature can cause microstructure transformation and formation of white etching layer (WEL) [16, 17]. As the degree of sliding at the wheel–rail contact increases, third-body effects (such as WEL) are increasingly significant [18, 19], and hard particles can cause abrasive wear [20, 21]. Abrasive wear resistance of pearlitic and martensitic steels has been studied by Modi et al. [22–24].

### 2.2 Roughness

Wheel and rail surfaces are rough at the microscale, and instead of a smooth Hertz contact pressure in the contact between the two surfaces, contact occurs at asperity peaks and the real contact area is only a fraction of the apparent contact area. The pressures at these contact points are correspondingly high. Kapoor et al. [25] performed a series of twin-disc tests which showed that, even when the load is beneath the shakedown limit, roughness can cause plastic deformation within a few microns of the surface. Numerical analyses of rough surface contact show that roughness causes very high contact stresses within 50 \( \mu\text{m} \) of the surface, as shown in Figs 3 and 4.

For two-dimensional contact, such as twin-disc contact, it is possible to use the subsurface stress distribution calculated from rough surface analysis as an input to the ‘brick’ model and thereby includes the effect of wheel roughness. This has been done in references [25, 26].

Neglecting the effect of surface roughness gives misleading results, but performing a complete rough surface analysis requires measured profiles and the analysis is more complicated for three-dimensional contacts such as rail–wheel contact. The current ‘brick’ model, therefore, uses an approximation of rough contact by multiplying the

![Fig. 3 Numerical prediction of contact pressure and subsurface stress distributions in a twin-disc contact. (In this case the principal shear stress, i.e. the maximum shear stress occurring in the \( \text{zx} \)-plane, is plotted rather than the orthogonal shear stress which is used elsewhere in this paper.) The nominal peak Hertz pressure is 0.95 GPa, but the elastic calculations with surface roughness predict peak pressures as high as 8 GPa and a maximum principal shear stress of \( \sim 2 \) GPa within a few microns of the surface (from reference [13]).](image-url)
orthogonal shear stress at depth $z$ by

$$A^{1-z/d}$$

where $d$ is the affected depth and $A$ is a magnification factor. Currently the values for $A$ and $d$ are 2 and 15 $\mu$m, respectively.

A complete model of surface roughness would need to consider a number of factors beyond the scope of this paper. In practice, a single wheel pass represents multiple asperity contacts and, therefore, the near-surface material experiences multiple stress cycles and additional strain accumulation. Although the contact stress at the surface is very high, so is the hydrostatic pressure, which increases the ductility of the plastically deforming material. It is also possible for the steel to undergo dynamic recrystallisation [16, 17], in which case a pearlitic microstructure model will not be appropriate.

Removal of elements as wear debris causes the simulated surface to become rough also. As the applied load remains constant, the stress should increase as the contact surface decreases. In the model, therefore, as more and more elements are removed from a layer, more and more stress is concentrated in the remaining elements by multiplying the orthogonal shear stress by $Nz/N_r$ where $Nz$ is the original number of elements in the layer and $N_r$ is the remaining number of elements.

### 2.3 Microstructure

Pearlitic rail steel has a microstructure of ‘grains’ of pearlite bordered by regions of proeutectoid ferrite (the prior-austenite grain boundaries). A pearlite grain consists of a number of colonies of pearlite, which has a lamellar structure of thin planes of cementite (with a thickness of $\sim 0.1 \mu$m) separated by ferrite. The orientation of the lamellae varies from colony to colony. The separation of the cementite planes and the amount of proeutectoid ferrite surrounding the grains both affect the hardness of steel. The hardness of the pearlite is inversely proportional to the square root of the separation of the lamellae [27]. The carbon content of the ferrite also affects the strength of the pearlite, and this increases as the cementite dissolves into the ferrite during deformation of the pearlite [28].

In a study of abrasion resistance of pearlitic and martensitic steels, Jha et al. [24] found that the optimum was a mix of martensite and ferrite, rather than the harder purely martensitic steel, because the softer ferrite phase makes the resultant steel both more compliant and less brittle. In selecting a microstructure model for simulating wear of steel, it is important, therefore, to be able to include different phases as appropriate. Eden et al. [29] examined the deformation of pearlitic rail steel using SEM microscopy and nanohardness measurements, and found that the proeutectoid ferrite regions accumulated greater strain than the pearlite.

The ‘brick’ model can simulate microstructures through selection of appropriate material properties. By refining the mesh used in the model, it is possible to simulate the cementite lamellae of pearlite, but the method used in the present paper is to represent the pearlite grains and proeutectoid ferrite grain boundaries by a hexagonal pattern of ‘pearlite’ grains bordered by ‘ferrite’. A schematic is given in Fig. 5. Grain width and boundary thickness are easily configurable, as are material properties.

Here, the ‘pearlite’ and ‘ferrite’ have respective mean hardness values of 370 and 250 kgf/mm$^2$, with standard deviation 15 per cent of the mean value, based on nanohardness measurements by Garnham et al. [30]. The hardening ratios in equation...
(1) were estimated as $\beta = 1.55$ for the pearlite and $\beta = 1.48$ for the ferrite. (The second parameter, $\alpha$, which determines the rate at which the material hardens as shear strain accumulates, has been assumed to be unity for the purpose of the calibration. Further analysis is needed to estimate this parameter.) For both materials the mean critical shear strain for failure ($\gamma_c$) is set as 11, with standard deviation 5 per cent of the mean value. Tyfour et al. [4] estimated a value of 11.5 for $\gamma_c$ for BS11 rail steel – but this is a difficult parameter to estimate and further study is needed. Kapoor and Franklin [9] showed that the predicted wear rate is inversely proportion to $\gamma_c$.

When the mesh is generated, each element is allotted a material type (‘pearlite’, ‘ferrite’, etc.) based on its position in the overall microstructure. Values for the initial shear yield strain ($k_0$) and the critical shear strain for failure ($\gamma_c$) are then sampled from a Gaussian distribution with appropriate mean and standard deviation. Following the calibration exercise in reference [30], the shear yield stress [unit: Pa] is calculated from the measured nanohardness ($H_n$) [unit: kgf/mm$^2$] by

$$k_0 \approx 0.8 \times 10^6 H_n$$

(6)

This assumes that yield stress (and thus shear yield stress, by the von Mises criterion) is proportional to the measured nanohardness; based on data for metals in Tabor [31], there is proportionality to bulk hardness.

While it is also possible to model the pearlite properly as lamellae of cementite and ferrite, this would require a finer mesh (element size of 0.1 $\mu$m or less); for the model in Fig. 5, an element size of 1 $\mu$m is suitable. Figure 6 illustrates the model with this microstructure. Being softer than the pearlite, the ferrite accumulates strain faster and fails sooner.

### 2.4 Crack initiation

A failed, or ‘weak’, element represents material which has exhausted its ductility. One interpretation is that the element is unable to support tensile stress, and that the material contains one or more microcracks. Certainly, it is weakened material in which crack initiation is possible and through which crack propagation will be relatively easy.

While a solitary failed element is of no great significance, clusters of failed elements are sites where crack initiation and propagation to a significant size is likely. Fletcher et al. [32] presented a method of using image analysis of the simulation output to identify clusters of failed elements; this can be used to predict crack initiation depth with number of load cycles, and thus can be helpful in determining rail life.

Another approach to predicting crack initiation depth is to use percentage damage depth. In addition to wear rate, the current ‘brick’ model outputs 1 and 10 per cent damage depths. These are the maximum depths in the simulation at which 1 and 10 per cent, respectively, of the elements in the layer have failed. For the microstructure in Fig. 5, as the ferrite fails sooner than the pearlite, the 10 per cent damage depth indicates that most (or all) of the ferrite at that depth has failed, and therefore that crack initiation to that depth is very likely. The 1 per cent damage depth indicates that at least 4 per cent of the ferrite has failed and that crack propagation to this depth is likely to be rapid.

The 1 and 10 per cent damage depth measures should not be taken as absolute statements of crack initiation. Their significance depends on the microstructure model used, and they are merely indicators of the amount of deformation in the material. Nevertheless, they are useful for comparing material response to different contact conditions.

### 3 RESULTS

#### 3.1 Microstructure

1 and 10 per cent damage depths and average wear rates for two different hexagonal microstructures are compared in Fig. 7. The first (‘60/4’) is that shown in Fig. 5, with grain width and grain boundary width of 60 and 4 $\mu$m, respectively. The second
('52/2') has grain width and grain boundary width of 52 and 2 µm, respectively. The simulations represent twin-disc contact, and each plotted line represents the average behaviour over 20 simulations with different initial random seed values. Steady state wear rates (with some fluctuation) were achieved within 10 000 cycles; the average wear rates over the subsequent 90 000 cycles are noted for each microstructure.

For this contact, there is not a large difference between the two cases, but the 1 per cent damage depth is slightly greater for the ‘60/4’, levelling off at 643 µm compared with 611 µm for the ‘52/2’; the 10 per cent damage depth levels off at 398 and 355 µm, respectively. (The semicontact width for this twin-disc contact is ~300 µm.) The wear rate is also slightly higher for the ‘60/4’, averaging 0.635 nm/cycle compared with 0.569 nm/cycle for the ‘52/2’.

### 3.2 Elliptic contact

However twin-disc contact can be modelled as two-dimensional, modelling rail–wheel contact requires a three-dimensional approach. Rail and wheel profiles have a complex geometry and the shape of the contact patch can vary considerably. In some cases there will even be multiple contact patches. As an approximation, the contact patch can be modelled as an ellipse.

In practice, even if considering a particular wheel of a particular vehicle, the centre-line of the rail–wheel contact varies slightly from wheel pass to wheel pass, so that the wear will be spread out across a greater width. When variations in wheel profile and vehicle characteristics are also considered, the variation not only in contact location but also contact forces means that wear will occur across much of the railhead. Further, as wear occurs the railhead profile also changes, which affects vehicle dynamics and the rail–wheel contact.

The current ‘brick’ model uses the method developed by Fletcher and Kapoor [33] for calculating the orthogonal shear stress in the plane of the mesh caused by a (fully slipping) Hertz pressure distribution over an elliptic contact patch. As shown in Fig. 8, the plane of the mesh can be offset from the centre-line of the contact. This can be used to predict the wear rate across the width of the contact, as shown in Fig. 9.

The transverse semicontact width in Fig. 9 is 3 mm and wear rates have been calculated for four friction coefficients (0.1–0.4) and 13 offsets (0.0–2.4 mm); each wear rate has been averaged across three simulations of duration 100 000 cycles. The ‘60/4’ microstructure (Fig. 5) was used.

For the case with coefficient of friction 0.1, the predicted wear rate is negligible (and is not shown in

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**Fig. 7** 1 and 10 per cent damage depth results for two different hexagonal microstructure definitions. The simulations represent twin-disc contact, and each plotted line represents the average behaviour over 20 simulations with different initial random seed values. Steady state wear rates (with some fluctuation) were achieved within 10 000 cycles; the average wear rates over the subsequent 90 000 cycles are noted for each microstructure.
Fig. 9). In the other cases, although the wear is certainly greatest at the centre-line and drops off towards the edge of the contact, the spread of wear is quite even. For the case with coefficient of friction 0.4 and offsets less than 2 mm, the 10 per cent damage depth is 0.9 mm or greater (when compared with a total simulation depth of 1 mm). This correlates with the very high-predicted wear rates (>2 nm/cycle).

4 DISCUSSION

A number of improvements over the 'layer' and 'brick' models used in previous work [6–8] have been made which are necessary for the purpose of predicting wear of and crack initiation in rails. First is the introduction of a hexagonal two-material microstructure representing pearlitic grains and proeutectoid ferrite grain boundaries, in which the
widths of each can be adjusted to represent different grades of pearlitic rail steel. Hardened pearlitic steels, for example, have almost no proeutectoid ferrite. The comparison of '60/4' and '52/2' microstructures, the latter having thinner grain boundaries, showed that the '60/4' had slightly greater wear rate and 1 and 10 per cent damage depths.

In addition, manufacturing processes can result in microstructural variations with depth (decarburized layers, for example), and this can be approximated by making the grain width and grain boundary width functions of depth.

The '1 per cent damage depth' introduced here is an arbitrary measure, its significance depending to some extent on the microstructural model. Damage depth can be used to predict the relative potential for crack initiation of different contact cases for a given microstructure. However, caution should be used when comparing with different microstructures, as above: 1 per cent damage depth for the '60/4' configuration indicates that ~4 per cent of the ferrite has failed; 1 per cent damage depth for the '52/2' configuration indicates that ~6 per cent of the ferrite has failed.

The results of the twin-disc tests by Garnham et al. [30] show the general trend that RCF life increases as the volume fraction of proeutectoid ferrite decreases. The damage depths agree with this trend, increasing as the thickness of the proeutectoid ferrite grain boundaries in the model increases. In the absence of other RCF-inducing factors (e.g. inclusions) the 'brick' model would predict maximum life for a fully pearlitic steel.

The proeutectoid ferrite of the grain boundaries is relatively soft when compared with the pearlite of the grains and thus accumulates strain faster and fails sooner. The model predicts, therefore, that cracks will initiate along the heavily strained ferrite grain boundaries, which matches the observations of Eden et al. [29]. More likely, however, is that cracks initiate along a pearlite–ferrite interface as it is here that the strain difference is greatest; Figure 17 in Carroll and Beynon [34] shows a crack following a strained pearlitic region in a decarburized layer which is predominantly ferrite.

The current 'brick' model uses failed elements as the basis for crack initiation, but this can be refined by considering strain difference.

Accurate prediction of wear rates and crack initiation requires good material data, and the work of Garnham et al. [29, 30] is providing hardness and hardening data for ferrite and pearlite. Some preliminary results were used for the material properties used in this paper, with the consequence that the 'brick' model is able to predict wear rates for twin-disc contact and rail–wheel contact (Fig. 9) which are close to measured values. (Measured wear rates are typically ~0.4 nm/cycle at the gauge corner of the rail and ~0.2 nm/cycle at the top of railhead; unlubricated flange contact leads to much higher wear rates. Direct comparison is difficult because the spread of measured wear indicates a wide range of contact pressures and locations.)

The ability to model an elliptic contact patch makes it possible to take input about rail–wheel contact locations, sizes, and forces from vehicle dynamics simulations and to build up a picture of the how the railhead will wear – and this, in turn, could be fed back into vehicle dynamics simulations.

There are still some limitations to the elliptic contact model, in particular the assumption of fully slipping contact. Apart from flange contacts (between the wheel flange and the side of the railhead), which are particularly severe and often lubricated to prevent wear, the rail–wheel contact is partially slipping, i.e. parts of the contact are slipping and parts are sticking. Components of spin and creep can be calculated (Kalker [35]) but this has not yet been incorporated into the 'brick' model. (Partial slip of two-dimensional contact is simpler to model and is discussed in reference [11].)

Although rail–wheel contact is modelled in three dimensions, the 'brick' model remains essentially a two-dimensional model. If the traction and the 'brick' model are aligned with the direction of the rail (which is not the case for curved track) then the orthogonal shear stress distribution needs to be calculated only in the plane of the 'brick' model. Otherwise, the stress distribution needs to be calculated across a three-dimensional mesh.

Although a proper three-dimensional 'brick' model is not needed for predicting wear rates and damage depths in rail–wheel contact, an accurate model of crack initiation and early propagation will need to consider how grain boundaries link together in three dimensions. (This is the direction now being taken by researchers at Cornell University [36].) The next major development of the 'brick' model will therefore be to model realistic three-dimensional microstructures and crack initiation at boundaries with high strain differences.

5 CONCLUSIONS

Two major developments to the 'brick' model, a computer simulation for predicting wear and crack initiation, have been presented: the capability to simulate microstructures with multiple material types (e.g. proeutectoid ferrite, 'pearlite'); and the calculation of stress in the plane of the mesh for an elliptic contact patch.
The new model uses a configurable two-phase hexagonal microstructure which can be used to represent pearlitic rail steel. Metallurgical analysis of pearlitic rail steel samples from controlled twins-discs tests has provided estimates of hardness and hardening data for the two phases, proeutectoid ferrite and pearlite, which have been used to calibrate the model [30]. Predicted wear rates are of the appropriate order, although still slightly high.

A new method for predicting crack initiation based on percentage damage depth is introduced. This is suitable for comparing different load cases for a particular microstructure.

Rail–wheel contact with an elliptic contact patch can now be modelled. Unlike two-dimensional contact (such as twin-disc contact) where wear rate is constant across the width of the contact, wear rate for elliptic contact is highest at the centre-line of the contact and drops to zero near the edges; however, the wear rate is quite even over most of the contact width.

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APPENDIX

Notation

\( d \) roughness-affected depth
\( dx \) width of each element
\( dz \) thickness of each element
\( k_0 \) (initial) shear yield stress of undeformed material
\( k_{\text{eff}} \) (effective) shear yield stress of deformed material
\( p \) probability that an element is removed
\( x \) coordinate axis: direction of traction
\( z \) coordinate axis: depth
\( A \) roughness stress-amplification factor
\( C \) ratcheting constant
\( H_n \) measured nanohardness
\( N_x \) number of elements remaining in a given layer
\( N_y \) number of elements horizontally
\( N_z \) number of elements vertically
\( W \) reference wear rate
\( \alpha \) hardening rate
\( \beta \) hardening ratio
\( \gamma \) total accumulated shear strain
\( \gamma_c \) critical shear strain for failure
\( \mu \) coefficient of friction
\( \tau_{xx} \) orthogonal shear stress in \( zx \)-plane
\( \tau_{xx}(\text{max}) \) maximum absolute \( \tau_{xx} \) at a given depth
\( \Delta \gamma \) shear strain increment