COMPARISON OF DUCTILE DAMAGE MODELS DURING SCRATCH TESTS - A NUMERICAL STUDY

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Abstract: The ‘wear mode diagram’ has been commonly used to classify the deformation regime of the soft work-piece during scratching, into three modes: ploughing, wedge formation and cutting. The scratch test is used to evaluate wear modes and material removal associated with wear. There are different damage models in the literature used for the description of material behaviour after damage initiation under different loading conditions. However, there has been little analysis to compare damage models during scratch test conditions. The first aim of this work is first to use a finite element modelling package (Abaqus/Explicit) to build a 3D model to capture deformation modes during scratching with indenters with different attack angles. Three different damage models are incorporated into the model and patterns of damage initiation and propagation are compared with experimental results from the literature. This work highlights the role of the damage model in accurately capturing wear modes and material removal during two body sliding interactions.

Keywords: Ductile damage, Finite element, Pile-up, Scratch test

1. INTRODUCTION

Abrasion can occur when a hard protrusion on one surface slides against a softer contacting surface. For the case where the softer material is ductile, plastic deformation begins when the contact pressure causes stresses that exceed the elastic limit. As a result of the sliding of the indenter, the plastically deformed material may be pushed: to the side to form ridges located at the sides of the indenter; and/or towards the front of the indenter as a wave of plastic deformation; or even upwards in the form of chips. Challen and Oxley [1] used 2D slip line theory to classify three modes of deformation depending on the attack angle (the angle between the frontal contact face of the asperity and the sliding direction) and the normalized interfacial strength (the ratio of shear strength of the interface to the sheet). Kayaba et al. [2] and Hokkirigawa and Kato [3] performed a series of scratch tests with rigid indenters to identify the now well-known ‘wear mode diagram’, which identifies the conditions which cause these three modes of deformation - ploughing, wedge formation and cutting.

Scratch tests have been widely used to study the abrasive wear of materials at different conditions. For instance, Mezlini et al. [4, 5] investigated how subsurface hardness and indenter geometry can change the wear modes in single and repetitive scratch tests. These studies confirm that an increase in the attack angle will shift the deformation from ploughing mode to cutting mode.

The Finite Element Method (FEM) has helped researchers gain further insights into the deformation and stress state during scratch tests [6]. Bucaille et al. [7] modelled scratching of elastic plastic sheet by frictionless rigid conical indenters using the commercial FEM package Forge3®. It was concluded that increasing the rheological factor - the ratio of elastic modulus to yield stress multiplied by the tangent of attack angle - increased the overall coefficient of friction and ridge height in front of the indenter. In [8] FEM was utilized to show that pile-up height reduces with increasing strain hardening exponent.

Wear particles are created when parts of the work-piece material have failed and are therefore removed from the surface. Hence, studying damage initiation and failure can reveal more information about the wear mechanisms. Jiang et al. [9] correlated the crack patterns during scratching of polymers...

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with the stress fields calculated by FEM, without including damage in the simulations. It was shown that, depending on the material constitutive behaviour, ductile damage or brittle damage may be dominant. Tkaya et al. [10] constructed a 3D model in Abaqus/Explicit and observed that including damage behaviour can lead to improved accuracy in the calculation of the apparent coefficient of friction. This study used a constant value for equivalent plastic strain at failure for the damage initiation criteria. In a recent study [11], the Johnson-Cook plasticity and damage initiation formulation was applied to single and multiple asperity scratch tests and it was shown that the interaction between parallel scratches can affect the wear behaviour.

The above discussion has highlighted that a large amount of research over many years has been devoted to understanding abrasive wear behaviour, and that the deformation at the asperity tips of interacting surfaces is commonly explained via the well-known wear modes. FEM has been used in recent years to better understand the conditions at the asperity tips that cause these wear modes. However, the role of the damage model, and hence the appropriate choice of damage model, is not well understood. Therefore, the aim of this study is to provide a qualitative comparison of damage initiation patterns during single asperity scratch tests at different conditions and with different damage initiation laws. The effects of work-piece material strain hardening and interfacial coefficient of friction on pile-up geometry is evaluated and compared against results from the literature. Finally, the location and trends of damage initiation with Constant Fracture Strain, Johnson-Cook damage model and Lode-dependent Fracture Strain are compared.

2. FEM SIMULATION

2.1 Model description

The finite element analysis of the scratch test was performed using Abaqus/Explicit. Rigid conical indenters with a spherical tip of 60 µm with different apex angles of 40, 120, and 160 degrees - corresponding to attack angles (θ) of 70, 30, and 10 degrees - were considered.

Figure 1. Schematic view of FEM.

Figure shows a schematic of the model. The overall size of the deformable work-piece is 18 mm × 10 mm × 3.5 mm (length × width × thickness). This size was selected to be large enough to avoid boundary effects when scratching at the lowest attack angle θ [12]. The simulation was divided into two steps. At the first step, the indenter moves vertically along the negative Y-direction by 0.3 mm. Once the specified penetration depth is achieved, the indenter slides in the X-direction by 1.25 mm while the Y-position remains constant. Symmetry boundary conditions are applied on the XY-plane to save computation time. The top surface is free and all other four surfaces are fixed in all three translational directions.

A comprehensive mesh sensitivity analysis was performed to find the optimum mesh size for the contact region. It was found that the scratch geometry converges with an element side length of approximately 0.04 mm in the contact region. The contact region is meshed with this small element size, as shown in Figure. This core region is bonded to the rest of the model, which has a much coarser mesh size, using tie constraints. For the core region, 8-node brick elements with reduced integration points were selected, while 4-node linear tetrahedron elements were used for the remainder of the model.
Due to the relatively small element size, the stable time increment required by the solver is very small and consequently the simulation computational time is long. Hence semi-automatic mass scaling was used to speed up the solution, while ensuring that the kinetic energy was not more than 10 \% of the internal energy [13].

The contact pair method with surface-to-surface formulation was used to establish contact between the indenter and work-piece surface. Due to excessive element distortion, Arbitrary Lagrangian Eulerian (ALE) adaptive method was utilized to overcome the convergence issues. Moreover, double precession accuracy was selected for the solver to reduce the truncation error caused by the large number of iterations.

2.2 Material and Damage models

The sheet material is made of 2024-T351 aluminium alloy with elastic modulus \( E = 73 \text{ GPa} \) and Poisson’s ratio \( \nu = 0.33 \). Von Mises yield criteria with initial yield strength \( \sigma_0 = 320 \text{ MPa} \) and associate flow rule was assumed. Swift law for isotropic hardening was used to describe the strain hardening as in (1):

\[
\sigma_y = K(\varepsilon_0 + \varepsilon_p)^n
\]

(1)

where \( \sigma_y \) is the yield stress, \( K = 700, \varepsilon_0 = 5.42E - 3 \) and \( n = 0.15 \). To evaluate the effects of strain hardening on the scratch test, different values of hardening exponent \( n = 0.05, 0.2, 0.5 \) with constant \( \sigma_0 \) were used.

The capacity of the material to withstand load is limited as failure is caused by nucleation, growth and coalescence of micro-cavities and voids. One of the methods to predict damage initiation and failure in ductile materials is based on the calculation of the damage initiation parameter \( D \) given in (2):

\[
D = \sum \frac{\Delta \varepsilon^p_l}{\varepsilon^p_f}
\]

(2)

where \( \Delta \varepsilon^p_l \) is the increment in plastic strain at each step and \( \varepsilon^p_f \) is the equivalent plastic strain at the onset of damage initiation. Equation \( D = \sum \frac{\Delta \varepsilon^p_l}{\varepsilon^p_f} \) implies that upon further plastic deformation, the damage initiation parameter progressively increases until it reaches point C in Figure 2, when the material starts losing its loading capacity and damage initiates. After damage initiation, the ductile damage behaviour can be classified into two categories – coupled or uncoupled. In the coupled formulation, it is assumed that the material gradually loses its stiffness while the damage evolves until total failure occurs (section c-d in Figure 2). Conversely, in the uncoupled fracture behaviour, the material stiffness is not affected until the total failure occurs. In this study, our focus is on damage initiation and so damage evolution is not considered.

Figure 1. Ductile material behaviour with damage [13].

Equivalent plastic strain at failure \( \varepsilon^p_f \) can be a function of different parameters. The Constant Fracture Strain criteria (CFS) assumes that \( \varepsilon^p_f \) has a constant threshold, meaning when equivalent plastic strain
reaches this threshold, damage starts [14]. The Johnson-Cook Damage criteria (JCD) [15] expresses the dependency between stress triaxiality $\eta$, strain rate $\dot{\varepsilon}$, temperature $T$, and fracture strain,

$$\bar{\varepsilon}_f^{pl} = (d_1 + d_2 e^{d_3 \eta}) \left[1 + d_4 \ln \left(\frac{\bar{\varepsilon}_f^{pl}}{\varepsilon_0}\right)\right] \left[1 + d_5 \left(\frac{T-T_{room}}{T_{melt}-T_{room}}\right)\right]$$

where $d_1$ to $d_5$ are material constants and stress triaxiality is given as the ratio of hydrostatic stress $p$ to von Mises stress $q$, according to:

$$\eta = \frac{p}{q}$$

Due to the simplicity of the formulation, the ease of calibration, and the wide availability of material constants for many metals, the Johnson-Cook fracture model has found numerous applications in the literature. In this study we neglect the effects of temperature and strain rate as the process is assumed to be quasi-static at room temperature. It has been argued that failure is prohibited by high compressive hydrostatic stress at large negative values of triaxiality [16]. Lou et al. [17] suggested that fracture strain depends on lode angle $L$ as well as stress triaxiality, as shown in (5). A variable cut-off value for stress triaxiality at different lode angles was proposed to describe damage initiation at a variety of stress state situations, including compressive loading, and is such referred to as a Lode-dependent Fracture Strain model (LFS).

$$\bar{\varepsilon}_f^{pl} = C_3 \left(\frac{2}{\sqrt{L^2+3}}\right)^{-C_1} \left(\frac{3}{4} \eta + \frac{3-L}{3L^2+3} + \frac{1}{3}\right)^{-C_2}$$

where $C_1$ to $C_3$ are material constants and lode angle is defined as

$$L = \left(\frac{r}{q}\right)^3$$

where $r$ is the third invariant of deviatoric stress.

3 RESULTS AND DISCUSSIONS

3.1 Scratch Geometry

Figure 3 to Figure 5 illustrate the longitudinal cross section (along the XY-plane) of the pile-up in front of the indenter at the end of the scratch test, before unloading the indenter. Solid and dashed lines correspond to $n = 0.05$ and $n = 0.5$ respectively. The arrows indicates the direction of increase in interfacial coefficient of friction ($\mu$). By decreasing the hardening exponent, plastic deformation becomes more localized near the tip of the indenter and consequently more frontal pile-up occurs. It is also evident that higher $\mu$ leads to higher pile-up, as it increases the applied lateral force. As expected, sharper indenters (with larger $\theta$) initiate micro cutting mode due to the formation of micro-chips moving out of the contact zone. The results are compatible with previous studies [12,18].

![Figure 3](image-url)

**Figure 3.** Cross section of frontal pile-up with $\theta = 10^\circ$.

3.2 Damage initiation pattern
Details of the damage parameters used in the simulations are listed in Table 1. The damage initiation parameter \( D \) for \( \theta = 30^\circ \), \( \mu = 0.3 \), \( n = 0.15 \) considering three damage models was calculated. In Figure (a) and Figure (a) to Figure (a), contours of \( D \) at the onset of damage initiation (i.e. the time when \( D \) is first equal to unity), for each damage model is plotted. The white area in each figure indicates the damaged region. Two other instances after the initiation of damage are also shown in these figures, to show the progression of the damage parameter as scratching continues (b) and (c).

With the CFS model, damage starts beneath the indenter (Figure (a)) and propagates radially as sliding continues. The JCD model shows very early initiation of damage during the indentation step beneath the indenter. However the trend of damage propagation is very similar to CFS if \( d_1 \) and \( d_2 \) values are scaled to 1 (Figure). The LFS model, on the other hand, predicts that damage initiates in the wave in front of the indenter and propagates backward and forward with further sliding. Additionally, the damage initiates at the sheet surface at an angle of approximately 45 degrees relative to the sliding direction, and beneath the surface at 0 degrees (i.e. directly in front of the indenter), as shown in Figure (a).

![Figure 4](image4.png)

Figure 4. Cross section of frontal pile-up with \( \theta = 30^\circ \).

![Figure 5](image5.png)

Figure 5. Cross section of frontal pile-up with \( \theta = 70^\circ \).

In the CFS criteria, the damage initiation is determined directly from the limit value of plastic strain, and therefore the fracture strain does not depend on any other parameters. This causes the contours of equivalent plastic strain and damage initiation parameter to follow the same trend. As a result, damage initiates from beneath the indenter where the maximum plastic strain occurs.

Table 1. Damage Models Parameters.

<table>
<thead>
<tr>
<th>Model</th>
<th>Parameter</th>
</tr>
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<tbody>
<tr>
<td>CFS</td>
<td>( \varepsilon_{p} = 0.9 ) [19]</td>
</tr>
<tr>
<td>JCD</td>
<td>( d_1 = d_2 = 1 ), ( d_3 = -1.5 ), ( d_4 = d_5 = 0 ) [19]</td>
</tr>
<tr>
<td>LFS</td>
<td>( C_1 = 4.0983 ), ( C_2 = 0.4316 ), ( C_3 = 0.3914 ) [17]</td>
</tr>
</tbody>
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\( d_1 \) and \( d_2 \) are scaled for comparison purposes.
In the JCD model, fracture strain varies according to triaxiality. Larger negative values of triaxiality increase the fracture strain, but there is not a cut-off value for triaxiality to avoid failure at triaxilities less than a specific value. Moreover, it does not include dependency with lode angle. Hence the JCD and CFS models can be applied when stress triaxiality varies in a small range known to the user. In contrast, the LFS model prevents failure at very low triaxialities and also requires higher fracture strain for damage initiation at near zero and positive lode angles. This can explain why failure under the indenter is not predicted in this model, but instead is predicted to occur near the top of the pile-up around the indenter. This behaviour is observed during experiments by other researchers [4, 20].

4. CONCLUSIONS

A FEM model was constructed to simulate scratch tests of an elastic plastic material with conical rigid indenter. Von Mises yield criteria along with isotropic hardening was applied as material constitutive law. The effects of strain hardening exponent \( (n) \), interfacial coefficient of friction \( (\mu) \) and attack angle \( (\theta) \) on the scratch grove geometry was studied. It was found that reducing hardening, increasing attack angle and coefficient of friction increases the pile-up due to larger plastic deformation in the contact zone. These findings correlate well with results from the available literature. Then three different ductile fracture models – Johnson Cook Damage (JCD), Constant Fracture Strain (CFS) and Lode dependent Fracture Strain (LFS) – for aluminium alloy 2024-T351 was incorporated into the FEM to study the damage initiation during the scratch test.

**Figure 6.** Contours of the damage initiation parameter \( D \) with \( \theta = 30^\circ \) and \( \mu = 0.3 \) for CFS model.

**Figure 7.** Contours of the damage initiation parameter \( D \) with \( \theta = 30^\circ \) and \( \mu = 0.3 \) for JCD model.
It was observed that by using JCD and CFS models, damage initiates from the area beneath the indenter. Conversely, application of the LFS model causes the failure to start from the top of the piled-up material at the periphery of the indenter. The results suggest that a damage model should address the dependency of fracture strain to stress state during the scratch test. This study highlights the significant role of including a suitable damage model in capturing the material removal pattern during the scratch test. Experimental verification of these findings along with capturing different wear modes are the subjects of our current and future studies.

REFERENCES