Scratching of a coated polymer and mechanical analysis of a scratch resistance solution

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Abstract

In the case of polymer scratching, there is no models at all which take into account the viscoelastic viscoplastic behaviour of the material and the ability of polymers to strain harden or soften. Progress has now been made using numerical simulation and a new experimental set-up. When a viscoelastic contact generates a viscoelastic groove, the recovery is sensitive to the high local strain introduced by a geometric discontinuity like the roughness of the grooving tip or the angle between two faces of the tip. The scratch resistance conferred by a coating is evident on both the macroscopic scale of the contact and the local scale of the roughness of the tip. On the local scale, the coating prevents the roughness of the tip from creating micro-scratches at the surface of the macro-groove. Therefore, since the absence of micro-scratches is a condition for relaxation of the macro-groove, the thickness of the coating must be greater than the roughness of the tip. On the macroscopic scale, the mechanical behaviour of the contact is modified by the decrease in the friction coefficient.

Keywords: Coating; Polymer; Scratching; Friction; Roughness

1. Introduction

Most polymeric glasses are sensitive to scratching and resistance to marring and scratching is desirable in industrial applications. Increasing the scratch resistance is equivalent to introducing an elastic contribution into a fully plastic behaviour or to increasing the elastic component in an elastic–plastic behaviour. There are three ways to improve the scratch resistance [1]:

1. by decreasing the ratio $E/\sigma_{\text{yield}}$, where $E$ is the Young’s modulus and $\sigma_{\text{yield}}$ the yield stress, although this carries the major risk of decreasing the Young’s modulus with subsequent loss of the macroscopic mechanical properties of the structure. One may note that an elastomeric material, which has a low $E/\sigma_{\text{yield}}$ ratio, is not sensitive to scratching but only to cutting, cracking and wear;
2. by introducing a strain-hardening effect into the stress–strain relationship of the bulk material, which is a means of increasing the elastic unloading in an elastic–plastic strain [2]. Such polymers are generally brittle and sensitive to the influence of a local geometrical flaw;
3. by coating the material. Coating is a common way of improving the scratch behaviour of polymeric glasses. The first solution found to reduce the scratch sensitivity was to deposit a mineral coating on the surface of the polymer. This procedure had however little success, at least partly due to the large difference between the elastic strain domains of the substrate and coating. A second generation of coatings used polysiloxane and acrylic materials, where the scratch resistance is given by the hardness of the coat and the coatings have elastic strain domains in the same range as the substrate. The most recent generation of protective coatings has employed nano-materials, in which an organic matrix is filled with nano-sized particles of silica. The idea behind this strategy is to associate the large elastic domain of an elastomeric polymer with the hardness of the filling.

The majority of existing studies describing the behaviour of coatings explore the boundary between sliding or ductile scratching and brittle contact. They generally use the concept of the critical load and the models developed to analyse the cracking after passage of a sliding tip may be viewed as improvements on work done in 1960s by Lawn et al. [3–10]. Thus, mechanical analyses have been performed assuming
that the interface is submitted to shear stress and the coating to compressive and buckling stress, however, the adhesion of the coating has not been very successfully correlated with the critical load. Several scratch-adhesion models proposed in the literature were recently compared with experimental data obtained for polymeric coatings [11]. Two of the models seem to give a reasonable description of the dependence of the critical load on the friction during scratch-adhesion testing at constant sliding speed. These models always predict that the first damage will appear behind or in front of the contact area and in most cases the normal load is linked to the crack energy, sometimes also taking into account the strain energy of the substrate. Still more recently, Bertand-Lambotte et al. [12] have proposed that the transition from ductile to brittle scratching of a coating is dependent on a double condition: a fracture energy criterion and a size criterion. Since the mechanical properties of polymers are time and temperature dependent, a single value of the critical load cannot describe the overall mechanical behaviour of a coating. Demirci et al. [1] have shown that the mechanical behaviour of a coating on a viscoelastic material should not be analysed in terms of the critical load, but in terms of the shape of the stress field, modified by the effect of the local friction between a scratching tip and the coat, where this local friction will depend on the roughness of the tip.

Polymeric materials display complex behaviour and a more refined analysis than for other classes of materials is required to understand the influence of material properties on the scratch resistance [13–20]. Briscoe and Thomas [13] and Gauthier and Schirrer [14] have shown that an analysis of the viscoplastic behaviour of the surface of a material during contact with a sliding tip requires an evaluation of the strain and strain rate. The average value of the mean contact strain rate \( \dot{\varepsilon} \) may generally be simply estimated as

\[
\frac{1}{2} \frac{d\varepsilon}{dt} = \frac{V_{\text{tip}}}{2a}
\]

(1)

where \( V_{\text{tip}} \) is the sliding speed and \( a \) the contact radius. The mean contact strain is proportional to the ratio of the radius of the surface contact area to the radius of the tip as originally defined by Tabor [16]. The mechanical properties of polymeric materials are usually stress and temperature activated and follow an Arrhenius law at temperatures below the glass transition [14]. It was previously shown [15] that the rear and front contact areas can be predicted in the case of plastic and elastic–plastic contacts on a polymer surface. The rear contact area is due to the elastic recovery of the polymer and depends on the plastic deformation around the contact. Even for a moving tip, the rear area can be almost identical to the front area if the contact is almost elastic and the mechanical loss factor low.

All previous studies have focused on the behaviour of the surface during the contact time. On the other hand, the major difference between polymeric and other classes of materials is the capacity of the groove left on the surface to recover and this capacity is one way to improve the scratch resistance of polymeric surfaces. The recovery has a time and temperature dependency and may be accelerated if the glass transition [14] has been crossed during the life time of the groove, just after contact. Conversely, the existence of creeping during normal contact is well known and experimental studies [16,21–23], mechanical analyses [24–27] and more recently a numerical analysis [28] exist. Historically, while the first creeping experiments were carried out on a macroscopic millimetre scale [21], the characteristic
length of the contact was later decreased by using a nanoindenter [24,29]. The creeping function was generally derived from data obtained by recording the vertical motion of the tip. This vertical motion is however not exactly equal to the depth of the imprint, which was found to be dependent on the contact behaviour and highly sensitive to an exact knowledge of the tip shape. An analysis of the recovery of an indentation imprint is more recent and more difficult to perform [29]. The major difficulty is the choice of a probe to quantify a phenomenon confined under a contact and the latest experiments used AFM or SPM probes [23,29]. Analysis of the recovery of the groove left on the surface has begun [30,31].

The aim of this paper is to present a mechanical analysis of the scratch behaviour and the recovery after grooving of organic coatings deposited on organic glasses.

2. Experimental procedures

2.1. Experimental set-up

The experimental device for the scratch test, called the ‘micro-visioscratch’, has been described previously [14,15]. It consists of a commercial servomechanism bearing a small, temperature controlled transparent box which contains the sample and the scratching tip. Control of the moving tip and recording of the normal \( F_n \) and tangential \( F_t \) loads, scratching speed \( V_{tip} \) and temperature \( T \) are computer driven. A built-in microscope allows in situ observation and measurement of the groove left on the surface. Scratching over a wide range of speeds \( (1\times10^3 \mu m/s) \) and within a temperature range covering the polymer relaxation peaks \( (-70 \text{ to } +120 \text{ °C}) \) are the main innovative features of the system. The normal load \( F_n \) applied to the moving tip can be selected from 0.05 to 35 N by adjusting the compression of a spring of low stiffness. In the present experiments, performed at 30 °C, the speed of the tip was kept constant at 0.03 mm/s. Two cone-shaped diamond tips with a spherical extremity were used, the first having an apex angle of 60°, a tip radius \( R_{tip} \) of 116 \( \mu m \) and a total roughness \( R_t \) of 0.6 \( \mu m \) and the second an apex angle of 90°, a tip radius of 110 \( \mu m \) and a total roughness of 2.5 \( \mu m \).

A standard procedure was used to carry out the friction tests. After cleaning the tip and the sample with alcohol and drying them, a preliminary test was performed to age the surface of the tip with the polymer, which is necessary to obtain reproducible measurements. The experiment was then carried out, starting at the lowest normal load and varying it stepwise in the range 0.1–2 N, within a single groove, in as many steps as required to explore the entire range of strain sensitivity. At each loading step and throughout the scratching process, in situ photographs were taken to record information on the shape of the true contact area and the beginning of the life span of the groove left on the surface. As the contact width was not constant in these tests, the mean strain rate ranged from 0.15 to 1 s\(^{-1}\).

Stress/strain curves were determined in compression tests. The experimental device for these tests is based on the moving cross head of an Instron 4502 tensile machine and the whole apparatus is enclosed in an Instron environmental chamber. Compression tests over a wide range of strain rates \( (10^{-4} \text{ to } 10^{-1} \text{ s}^{-1}) \), within a temperature range covering the \( \alpha \) and \( \beta \) relaxation peaks of common polymers \( (-70 \text{ to } +120 \text{ °C}) \) and measuring the longitudinal and radial strain are the main characteristics of this system. The longitudinal strain was limited to 20% during tests.

2.2. Materials

The organic glass was an amorphous thermoset polymer (diethylene glycol bis(allyl carbonate)) called CR39. The Young’s modulus of this resin is typically 2 GPa at 20 °C and 1 Hz. Cylindrical samples 12 mm long and 5 mm in diameter were used for compression tests, while scratch test samples were plates a few millimetres thick. The coating was a spin coating of a nano-composite material, a thermoset matrix filled to about 20% of its volume with sub-micron silica particles (about 10 nm in diameter). The Young’s modulus of this coating is about 4 GPa at 20 °C and 1 Hz. Since, it is partially filled with mineral particles, it does not have a very marked time or temperature dependency. Coatings of different thicknesses (1.1 and 4.38 \( \mu m \)) were selected to include the highest degree of roughness.

3. Experimental results

3.1. Contact area

Fig. 1 shows representative photographs of uncoated and thinly and thickly coated specimens scratched with the two tips. At a normal load of a few tenths of a Newton, the smooth tip slides over the surface of the polymeric materials and leaves a slight residual viscoelastic groove. As the normal load increases, the importance of the coating becomes clear: at a median load the recovery of the groove is more marked on the coated samples, while at the highest normal load the coated samples always display less lateral and frontal pad formation than the uncoated sample. The rough tip immediately reveals the role of the coating: after contact with the tip under a normal load of a few tenths of a Newton, the uncoated sample has a blemished surface with a few micro-scratches. Unlike for the 1.1 \( \mu m \) coating, for the 4.38 \( \mu m \) coating, there is no difference between scratching by the two tips. The importance of the ratio of the roughness of the tip to the thickness of the coating has been demonstrated previously [1] and is confirmed here at higher normal loads: the coating prevents the roughness of the diamond tip from creating micro-scratches at the surface of the macro-groove. Therefore, as the absence of micro-scratches seems to be a condition for relaxation of the macro-groove, the thickness of the coating must be greater than the roughness of the tip.
Since if cracking appears the geometry of the contact area may be modified, the following analysis concerns only contacts without cracks.

The contact area between a moving tip and a polymer surface has a front side and a rear side and its shape changes with the strain. A contact area which is entirely plastically deformed will be called for simplicity a ‘viscoplastic contact’. If the contact area is not entirely plastically deformed, it will be called a ‘viscoelastic–plastic contact’. It was previously shown [15] that the shape of the contact may be simply described by the rear angle or by the ratio of the rear length to the front length. The rear contact angle $\omega$ is due to the elastic recovery of the polymer and depends on the plastic deformation around the contact. This angle has been drawn on the upper right image of Fig. 1. Fig. 2 gives a schematic representation of the evolution of the rear angle as a function of the mean contact strain $\varepsilon$. The strain is assumed to be simply proportional to the ratio of the contact radius to the tip radius for a spherical tip and one may note that there exists no clear definition of the scratching mean strain linking the geometry of the contact and the friction coefficient. $\varepsilon_e$ and $\varepsilon_p$ denote the contact strains at the end of the elastic contact area and the beginning of the plastic contact area, respectively. In the case of a viscoelastic contact, the rear angle $\omega$ is equal to $\pi/2$ for elastic deformation. If the material displays strain hardening, the contact strain decreases and $\omega$ increases [32]. As the strain rate and temperature vary, even under constant loading, the contact area may also vary considerably and the strain near the contact may change from viscoelastic to viscoplastic.

The evolution of the rear contact angle as a function of the contact strain for polymer samples with no coating and a 4.38 mm coating is presented in Fig. 3. Whatever the roughness of the tip, the end of the elastic domain seems to remain constant. In contrast, the elastic–plastic domain seems to expand if a thick coating has been deposited on CR39, with the result that the plastic domain begins at a higher contact strain for thickly coated samples. The thin coating presents a less clear evolution (data not shown).

3.2. Contact pressure

The contact pressure is the ratio of the normal load to the true contact area, which is the sum of the front and rear areas. It may be called the scratch hardness only in the case of plastic contact. Fig. 4 shows the contact pressure as a function of the mean contact strain for all three types of polymer sample. As previously, there is no large difference in the responses to the two tips. On the uncoated material, scratched by the smooth tip, the contact pressure increases from 90 to 140 MPa and then seems to decline as the ratio $a/R_{tp}$ exceeds 0.4, whereas on the 4.38 mm coating the contact pressure increases continuously. In view of the evolution of the rear angle, one may assume that the contact behaviour of the thickly coated sample is essentially elastic–plastic.

3.3. Friction coefficients

Fig. 5 shows the evolution of the apparent friction coefficient

$$\mu_{app} = F_t/F_n$$  (2)
as a function of the ratio $a/R_{\text{tip}}$ for the two tips and all samples. In the case of the smooth tip, the effect of the thickness of the coating on the apparent friction coefficient is clear: the friction decreases as the thickness increases. The same tendency is observed for the rough tip provided the thickness is greater than the roughness of the tip. If the ratio of the local shear to the local pressure is termed the ‘true friction coefficient’ $\mu$, then the apparent friction coefficient may be written as [39]:

$$\frac{F_t}{F_n} = \mu_{\text{app}} = \frac{C + D\mu}{A + B\mu}$$

(3)

Solution of this relationship between the true and apparent friction coefficients requires calculation of the four integrals $A$, $B$, $C$ and $D$, which are the local pressure and shear elementary action integrals, together with a knowledge of the rear angle $\omega$, the real contact area and the geometry of the tip. $A$, $B$, $C$ and $D$ take into account the macroscopic contact shape. However, the true friction coefficient refers to a smooth tip. If the tip is rough, this coefficient of the true friction between tip and surface must be called the ‘local friction coefficient’ because the roughness effect cannot be removed. Fig. 6 presents the local friction coefficient as a function of the ratio $a/R_{\text{tip}}$ for all samples and the two tips. At higher values of the contact strain (greater than 0.4), the local friction coefficient does not significantly depend on the contact strain, is greater for the rough tip and smaller when the thickness of the coating is greater than the roughness of the tip.
3.4. Groove recovery

Since if cracking appears, the recovery of the groove may be hindered, this section relates to grooves which stay ductile after scratching. Comparison of the recovery of different grooves was thus made in a particular configuration: the normal load was fixed at 0.64 N to generate scratches with a minimum yield, so as to have the time to record the groove profiles without having a fully plastic contact. The tests selected were uncoated CR39 scratched by the two tips and CR39 with a 1.10 \( \mu \text{m} \) coating scratched by the rough tip. Under this normal load, the contact strain lies in the range 0.4–0.45, the local friction coefficient has its asymptotic value of 0.3–0.35 and cracking never appears. The recovery of samples with a 4.38 \( \mu \text{m} \) coating was not tip sensitive and it can be clearly seen in Fig. 1 that for such samples one cannot justify recording the groove profile for a minimum period of time. Experiments were performed at 30 °C, the first profile was recorded 9 min after scratching and six further profiles were recorded during the next 6000 min.

Fig. 7 shows the in situ contacts and the grooves initially created on the three samples. As previously, the number of micro-scratches on the imprint of the groove decreases if the sample has been coated, as does the size of the frontal push pad. Two important points should be noted: under the chosen experimental conditions, the width of the groove remains constant and its shape does not resemble a flame. Micro-scratches generated by the roughness of the tip can recover and the number of micro-scratches decreases with time (see arrows drawn on Fig. 8). The life time of a micro-scratch as recorded on this photograph is the ratio of the scratch length to the sliding speed

\[
* = \frac{l}{V_{\text{tip}}}
\]  

(4)

and is typically about 10 s.

After scratching, the size and shape of the groove left on a sample was analysed using a commercial mechanical profile recorder and the radius of the section of the groove was estimated by fitting the profile data. This radius increases with the life time of the groove as seen in Fig. 9. If at 10 min, the sections generated by the rough tip

![Fig. 7. In situ images of the contact areas and initial grooves on the three samples. At the top uncoated CR39 scratched by the smooth tip, in the centre the same sample scratched by the rough tip and at the bottom CR39 with a 1.10 \( \mu \text{m} \) coating scratched by the rough tip.](image)

![Fig. 8. Zoom image of the groove on the 1.10 \( \mu \text{m} \) coating showing the recovery of a micro-scratch. Just after the contact area, the arrows indicate (from top to bottom) the lateral pad and two micro-scratches. Ten seconds later, only one micro-scratch exists. The outlines of these geometrical parameters are drawn underneath.](image)
are of the same order of magnitude, at 6000 min the radius of the groove section is clearly higher on the coated sample. At both time points, the radius of the groove section is greater for the smooth tip. Fig. 9 also shows interferometric profiles of the grooves at two time points. These profiles clearly demonstrate that micro-scratches on the coating are more open than micro-scratches on the uncoated samples and that the radii increase with time.

4. Discussion

4.1. Analysis of contact results

In the case of the smooth tip and high contact strain, the local friction coefficient decreases as the thickness of the coating increases, while for the rough tip the tendency would seem to be the same, but is not as marked. During these tests at constant temperature and normal load, the mean strain rate varies over nearly one decade. It is well known that below the glass temperature, the yield stress of polymeric materials increases linearly with the logarithm of the strain rate as predicted by Eyring’s law. Just as the scratch hardness was classically written as a function of the yield stress, the contact pressure may be written as a function of the yield stress with a factor $c$ depending on the contact strain:

$$\frac{p_{\text{yield}}}{\sigma_y} = c(\varepsilon)$$

This ratio of the contact pressure to the yield stress, called the normalised contact pressure, is time and temperature independent and depends only on the strain in the contact area [34]. The major assumption made here is the decoupling of the strain dependency and the temperature and velocity dependency. In the case of an elastic or plastic contact, the factor $c$ is linked to the ratio $Ea/\sigma_{\text{yield}}R_{\text{tip}}$. The contact pressure $p(T, \dot{\varepsilon})$ should thus be normalised by the yield stress $\sigma_y(T, \dot{\varepsilon})$ for the same values of $(T, \dot{\varepsilon})$. Fig. 10 shows on the left the stress–strain curves at one temperature and several strain rates and on the right the yield stress estimated from these tests as a function of the strain rate. Using these values, an extrapolated function was derived which allows one to estimate the yield stress to a good approximation over a wide range of temperatures and scratching velocities. Thus, the yield stress was fitted with a second degree polynomial law to estimate the values at strain rates comparable to those in scratch tests ($10^{-2}$ to $10^{2}$ s$^{-1}$):

$$\sigma_y(\dot{\varepsilon}, T) = a(T) + b(T)\log \dot{\varepsilon} + c(T)(\log \dot{\varepsilon})^2$$

A thin polymeric coating deposited on a polymeric substrate (typically a few microns thick for a contact width of about 80 μm) will not modify the global mechanical response to an indenter. O’Sullivan et al. [36] have shown that for a spherical tip sliding over a layered elastic half-space, the radius of the contact zone and the pressure under the centre of the indenter differ significantly from the Hertzian case only when the Young’s modulus of the coating differs significantly from that of the substrate. Since in our
experiments, the ratio of the contact width to the thickness of the coating was greater than 10 for the lowest normal load and the Young’s moduli of the two materials were of the same order of magnitude, the coating should not have influenced the bulk behaviour, contact geometry or contact pressure. Consequently, all the contact pressures may be normalised by the extrapolated function of the yield stress for CR39 under compression. During elastic static contact, yield occurs when the normalised contact pressure is equal to 1.1 for a Poisson coefficient of 0.3 and perfectly elastic–plastic behaviour [35]. This result may be used as an upper boundary for a sliding test where the friction favours yielding. When the normalised contact pressure is plotted against the contact strain (Fig. 11), it is clear that the coating prevents yielding. At a given contact strain, the normalised contact pressure is lower if the sample has been coated.

The estimated local friction coefficient shown in Fig. 6 displays a large variation (0.15–0.4). At a ratio $a/R_{tip}$ of 0.4, the contact width is 90 μm and the depth of penetration of the tip almost 10 μm. As the coating has a Young’s modulus only twice that of the substrate, its improvement of the scratch resistance must be investigated primarily as an effect due to decreasing the friction coefficient.

### 4.2. Numerical simulation to identify the friction dependency

Although indentation or scratch tests permit easy determination of a mean contact pressure, interpretation of the stress–strain curves is often difficult. Hence, the contact behaviour was modelled using the CAST3M© code. The mesh procedure Demete version 2.0, beta© (CEA/SEMT-P Verpeaux) was employed and the finite element mesh was a right-angled parallelepiped. The domain elements were three-dimensional meshes with 10-node tetrahedra and the mesh was refined under the contact area. Elliptical contact pressure and shear stress distributions were used to model the contact between a spherical tip and the surface. During calculation, the normal load was increased step by step while the contact radius stayed constant.

Despite the fact that the elasticity of polymers is often non-linear at a given temperature and strain rate, the elastic behaviour was modelled by a linear incremental law defined by Young’s modulus $E$ and Poisson’s ratio $\nu$, both taken to be constant. $E$ and $\nu$ were determined in a compressive test. The flow stress was described by

![Fig. 10. True compressive stress versus true strain (left) and comparison of the extrapolated function with the experimental yield stress as a function of the strain rate over a range of temperatures and strain rates (right). The experimental data and extrapolated function correlate over a wide range of temperatures.](image1)

![Fig. 11. Evolution of the normalised contact pressure as a function of the mean contact strain for the two tips. In the 4.38 μm coating, the yielding seems to be retarded: the normalised contact pressure reaches a value of about 1.6 for a large value of the mean contact strain.](image2)
a G’sell–Jonas law [33]:

$$
\sigma = k e^{(\alpha_T/T)(1 - e^{-\omega vp})} \dot{\epsilon}_v e^{h g \dot{\epsilon}_v^2} m
$$

where $\dot{\epsilon}_v$ and $\epsilon_v$ are, respectively, the generalised viscoplastic strain rate and strain and $k$ is the consistency, $\alpha_T$ a thermal coefficient, $h_g$ the strain hardening coefficient and $m$ the sensitivity to the strain rate. In the formalism of G’sell and Jonas, the term $(1 - e^{-\omega vp})$ describes the viscoelastic behaviour under loading but does not model the elastic unloading of the deformation and hence this term was not considered in the present work i.e. $(1 - e^{-\omega vp}) = 1$. In our simulation, the elastic recovery was directly related to the ratio of the flow stress $\sigma$ to Young’s modulus $E$, thermal effects were neglected and $\alpha_T$ was equal to 0. Eq. (7) then becomes:

$$
\sigma = k \dot{\epsilon}_v e^{h g \dot{\epsilon}_v^2} m
$$

The three parameters $k$, $h_g$ and $m$ have been described previously [15] and were determined by an inverse method adapted to large deformations and based on interpretation of the force–penetration curves in indentation tests with two indenter shapes: $m=0.078$ and $h_g=4.5$. In our case, the strain rate was higher than in [15] and the consistency was adjusted: $k=87$ (MPa s$^{-m}$). Fig. 12 compares the experimental compressive stress–strain data with the numerical function used for the present simulations.

In the case of perfectly elastic–plastic behaviour, this contact model has been validated for coated and uncoated materials [38] by comparing it with all well-known elastic solutions [35–37].

Numerical simulations were performed to locate the boundaries between elastic and elastic–plastic contact and between elastic–plastic and plastic contact. The first boundary could be simply related to the first finite volume having a strain higher than the elastic strain, while the second was defined to occur when all the matter contained in the half spherical volume under the contact area flowed plastically. These two boundaries appear in Fig. 13, where the normalised contact pressure is plotted against the local friction coefficient. The results obtained for the first boundary agree with those reported by Johnson [35]. Experimental data for the smooth tip are plotted on the same figure and the in situ photography clearly shows that for a given normalised contact pressure, the size of the push pads and the contact yielding depend on the local friction coefficient.
pads and the contact yielding depend on the local friction coefficient.

Thus, for a given tip radius, the yielding of the contact depends on both the normal load which governs the ‘geometrical contact strain’ (i.e. the ratio $a/R_{\text{tip}}$) and the local friction coefficient. As a result, the ‘scratching contact strain’ must be defined as a function of these two variables:

$$
\bar{\varepsilon}_{\text{scratching}} = f(a/R_{\text{tip}})g(\mu_{\text{local}})
$$

If these two components are decoupled, for a given ratio $a/R_{\text{tip}}$, the ‘scratching contact strain’ may be considered to be simply proportional to the local friction coefficient. As our recovery experiments were performed at a constant ratio $a/R_{\text{tip}}$, the recovery of the groove must be analysed with regard to the local friction coefficient and the ratio of the thickness of the coating to the roughness of the tip.

4.3. Recovery of the groove

It was not easy to measure the radius of the groove section just after contact, at the beginning of the life span of the groove. Although the contact displayed yielding, if the unloading after contact was elastic and hence reversible, a second passage of the scratching tip along the groove with a section of concave radius $R_0$ did not increase the yielding.

The following analysis may be considered to transfer to scratching the results of previous work on indentation [35]. Provided the elastic sliding may be predicted with the elastic Hertz theory, the radius of the groove can be related to the radius of the tip through the equation:

$$
\frac{1}{R_0} = \frac{1}{R_{\text{tip}}} - \frac{3F_n}{4E^*a^3}
$$

where $E^*$ is the Hertz contact elastic modulus. During recovery, the edges of the groove lie parallel. The mean strain in the matter around the groove may be defined as:

$$
\varepsilon(t) = \frac{a}{R(t)}
$$

and the recovery of the groove is described by the ratio:

$$
\frac{\varepsilon(t)}{\varepsilon_0} = \frac{R_0}{R(t)}
$$

This recovery is plotted in Fig. 14. During contact, the recovery process was not freely stressed because contact between the tip and the surface existed. The start of recovery was defined to occur after contact, i.e. after the maximum strain had been imposed at the maximum contact width for a time $t_0$:

$$
t_0 = \frac{a}{V_{\text{tip}}}
$$

Results are in agreement with the previous analysis. A comparison of the two curves for the uncoated material indicates that the recovery increases if the tip is smooth, which is related to the fact that the local interfacial strain decreases if there are no micro-scratches. A comparison of the two curves for the rough tip indicates that the recovery increases if the sample has been coated. This is linked to a decrease in the yield in the elastic–plastic strain under the contact area as the local friction coefficient decreases.

5. Conclusions

Deposition of a scratch-resistant coating is a common way to improve the scratch behaviour of a polymeric surface. However, a thin scratch-resistant coating cannot prevent yielding on the macroscopic scale of the contact. In situ photographs show macro-grooves with parallel edges, which is an indication of the occurrence of yielding during contact. The mechanical behaviour of a coating on a viscoelastic material is easily described by considering the normalised contact pressure and should be analysed in terms of the shape of the stress field, modified by the effect of the local friction between a scratching tip and the surface, where this local friction will depend on the roughness of the tip and the presence of the coating. A coating decreases the yielding in the elastic–plastic behaviour of the contact if the local friction coefficient between the surface and the tip is low. The major benefit is the reduction of the ‘scratching contact strain’.

The ratio of the thickness of the coating to the roughness of the tip is confirmed to be a critical parameter, which enables one to increase the scratch resistance in the case of a thin coating.

The recovery of the groove left on the surface must be analysed in relation to the contact behaviour. If the local friction coefficient is low and there are no micro-scratches along the macro-groove, the recovery may be fast.

References