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A non-local fatigue approach to quantify the Ti-10V-2Fe-3Al fretting cracking process: application to grinding and shot peening

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Abstract

This paper presents an experimental study on the tribological behaviour and cracking response of a Ti-10V-2Fe-3Al titanium alloy under fretting loading with a cylinder on plane configuration. Three types of surfaces were investigated: a polished one considered as the reference, a ground one and a shot peened surface. Surfaces were compared with respect to residual stress, hardness and roughness. The first step of this study was to determine sliding conditions and coefficient of friction of the three contact types. Next, fretting tests under stabilized partial slip regime were carried out to investigate crack nucleation and propagation. Results show that whatever surface roughness or residual stress in the material, tribological behaviour is the same. These latter confirm that sliding condition and coefficient of friction in partial slip regime is due to material effect and not to roughness or surface hardness. Then, residual stress induced by grinding or shot peening have no influence on the crack nucleation threshold under fretting solicitation because crack nucleation is only induced by a sufficient tangential loading. The crack nucleation threshold is formalized by applying the Crossland criterion taking into account the stress gradient and the ensuing “size effect”. As expected, cracks propagation is influenced by residual stress under the surface. Compared to the reference case, for a same loading parameters set, residual stress induced by grinding is not sufficient to decrease crack length reached whereas effects of shot peening decrease highly these latter. So, there is a threshold of residual stress from which residual stresses are useful against cracking.

Keywords: Fretting, Crack nucleation threshold, Crack depth, Grinding, Shot peening

Nomenclature

r	Radius of the cylindrical pad
Ra	Average roughness
P	Normal force
δ	Displacement amplitude
δ^*	Maximal displacement amplitude
δ_t	Displacement amplitude at the sliding transition
Q^*	Maximal tangential force
Q^*/P	Tangential force ratio
Q_{th}	Crack Nucleation Threshold
μ	Friction coefficient
μ_t	Friction coefficient at the sliding transition
μ_{PS}	Friction coefficient in partial slip regime
A	Energy criterion
E_d	Dissipated energy by the fretting loop
E_t	Total energy of the fretting loop
p_0	Maximum hertzian pressure
p_{ref}	Reference hertzian pressure
N	Number of cycles
L_c	Cracks length
$L_{c_{max}}$	Maximal crack length observed ($\sim 500 \mu\text{m}$)
μ_{i0}	Initial friction coefficient
β	Plastic interfacial shear stress component
τ_t	Interfacial shear stress at the sliding transition
ℓ	Length scale parameter
ℓ_c	Critical length scale parameter

1. Introduction

In some aeronautical applications, titanium alloys are excellent candidates owing to their high strength to weight ratio [1]. In helicopter's rotors, a β -titanium alloy, Ti-10V-2Fe-3Al, is chosen for critical components because it provides excellent mechanical properties. However, vibrations in the structure during flight generate small oscillatory displacements at interfaces. This phenomenon, so called fretting, can induce early crack nucleation and propagation, which reduces fatigue resistance [2, 3]. Fretting damage on the contacting surface is critically controlled by the amplitude of the sliding displacement [4]. Under large amplitude gross slip conditions, wear associated to debris formation and ejection dominates [5]. Under partial slip condition, initiation of fatigue cracks is generally a more significant concern than wear. In fretting fatigue, cracking processes are due to the addition of two loadings: fretting loading at the contact and bulk fatigue loading. Usually, cracking nucleation and early propagation in mode II are more due to fretting loading on the surface, and propagation in mode I is mainly controlled by bulk fatigue stress. In the last decade, the effects of several parameters on the fretting fatigue behaviour have been investigated, for instance, stress gradient [6, 7], sliding regime [8], roughness [9], microstructure [10, 11] and palliatives like coatings [12] or shot peening [13].

Shot peening is one of the most common surface treatments to enhance fatigue strength in metal components. This process produces a compressive residual stress in the near surface region of the shot peened component. The compressive residual stress on the surface can prevent crack initiation and propagation when they are within the depth of compressive zone. It has been shown in numerous previous studies on titanium alloys that shot-peened components have longer fatigue lives as compared to un-peened components under various fatigue situations [14, 15] as well as under fretting fatigue loadings [16, 17]. Machining processes, like milling for flying components or like grinding for fatigue and fretting fatigue specimens, could introduce tension or compressive residual stress on the surface and in the near region of the surface. In this paper, parameters used for polishing and grinding generate compressive residual stress on the surface of components. Compressive residual stress induced by grinding process on the Ti-10V-2Fe-3Al could give different fatigue and fretting fatigue strength on specimens than for components in flight which are milled and thus affects results of cracking predictions when the component is defined.

In this paper, we focused on the effect of a machining process: grinding and a surface treatment: shot peening, on the crack nucleation and propagation of the Ti-10V-2Fe-3Al titanium alloy when it is solicited in fretting. Comparison was made with a polished sample defined as reference. Surface roughness, hardness and compressive residual stress in the depth of the samples were investigated for each kind of surface in order to estimate effects of both compressive residual stress and roughness on fretting results. Then tribological behaviour and cracking response were studied through fretting tests in the same configuration for each surface. And last, the crack nucleation threshold was formalized using the Crossland multiaxial fatigue criterion.

2. Experimental procedure

2.1. Material and contact parameters

The chosen material for fretting samples and pads is a β -titanium alloy named Ti-10V-2Fe-3Al used in aeronautics for its excellent fatigue properties. The composition of this alloy, in accordance with ASNA 6117, is given in *Table 1* and its mechanical properties, in accordance with ASNA 6117, are shown in *Table 2*.

A single type of contact has been investigated using a cylinder on plane geometry with a constant radius r , which is considered as confidential data and a constant width perpendicular to the sliding direction high enough to have plane strain conditions at the centre of the fretting scar. The Hertzian's contact pressure was kept constant in the range of 280 MPa to 850 MPa for all tests. In the tested configurations, normal and tangential stresses stayed in the elastic domain of the material. The working frequency is equal to 10 Hz, limited by the actuator efficiency, but no major effect of the frequency has been observed in past studies [18]. Each test is interrupted at 10^6 cycles because our study is motivated by high cycle fatigue damage which corresponds to at least 10^6 cycles. Moreover, Muñoz [19] shows that crack arrest is reached in fretting wear at this number of cycles.

Table 1

Chemical composition of the Ti-10V-2Fe-3Al (wt. %)

V	Fe	Al	O	N	C	H	Ti
9-11	1.6-2.2	2.6-3.4	< 0.13	< 0.05	< 0.05	< 0.010	Balance

Table 2

Mechanical properties of the Ti-10V-2Fe-3Al

Elastic modulus (GPa)	110
Poisson ratio	0,32
Yield stress (MPa)	1000
Density (g/cm^3)	4,65
Hardness (HB)	320
Alternating bending fatigue limit, σ_d (MPa)	575
Alternating shear fatigue limit, τ_d (MPa)	330

2.2. Fretting wear test

Figure 1 shows a schematic drawing of the fretting wear test used in this study. A tension-compression hydraulic machine is used to impose the displacement between the plane and the cylindrical pad. Further details of this setup and experimental methods used can be found in [20].

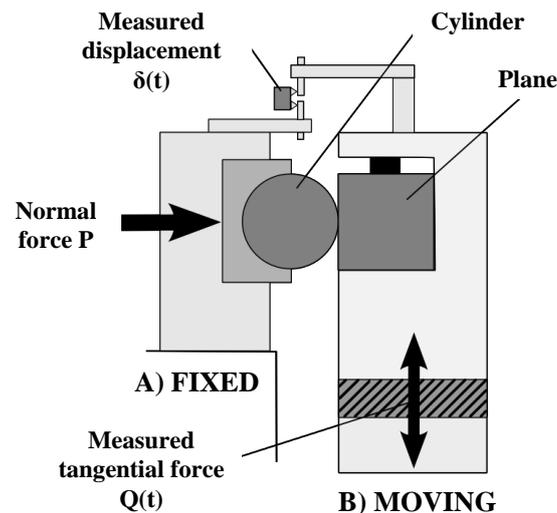


Figure 1 : Schematic diagram of the fretting system for the cylinder on plane configuration

A constant normal force P is applied. The cyclic sinusoidal displacement δ is then applied to generate an alternating tangential load Q on the contact. During these fretting tests, the

displacement δ , normal force P and tangential force Q are recorded. In partial slip regime, an increase of the displacement amplitude δ^* leads to an increase of the maximal tangential force Q^* . In gross slip regime, Q^*/P ratio stays constant for any displacement amplitude and is correlated to the friction coefficient μ , in the contact.

2.3. *Post-test analysis methodology*

All of the tests presented in this paper have been interrupted at 10^6 cycles and investigated with respect to the following methodology. Analyses consist of a determination of the crack depth through cross section observation. This crack analysis technique has been inspired by Proudhon *et al.* [9]. For all fretting tests, the sample is cut in the middle of the scars. Then, the new created surface is polished and observed with an optical microscope. The polishing and observation phase is repeated triple in order to evaluate the homogeneity of cracks depth. The polishing process gives spacing between two observation layouts of about $500\ \mu\text{m}$ (*Figure 2*). The scale of cracks depth studied here corresponds to homogeneous mode II crack propagation in the material. Therefore, the technique used is sufficient for obtaining proper quantification of crack depth.

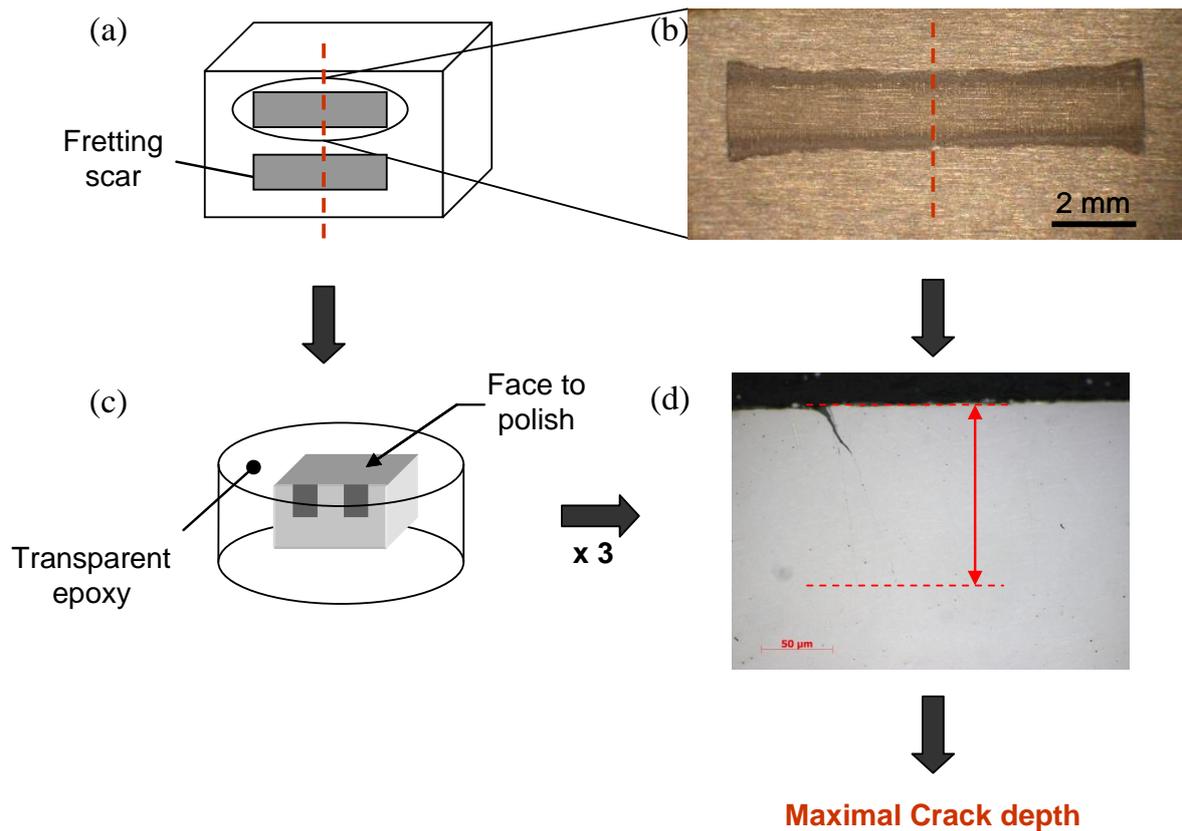


Figure 2 : Experimental method to investigate cracking after fretting test; see the text for details

3. Specimens investigation

Three surfaces have been investigated: polished, ground and shot peened. Before last machining or treatment process, overall geometries of samples were obtained by the same conventional milling process. Then, pads for polished and ground planes were ground to obtain a specific contact radius and a good planarity. Polished surfaces were obtained by manual polishing process. Ground specimens and pads were ground by using a water-cooled abrasive wheel. The work was performed to specification with an Eurocopter grinding mill in order to get a surface representative to Eurocopter fretting fatigue samples.

Concerning the shot peened specimens, both planes and pads were treated using conventional shot peening (balls of 0.4 mm in diameter) leading to an ALMEN intensity (French standard AFNOR NFL 06832) of 0.32 mm. The shot peening conditions used give an overlapping rate of 150%. Shot peening results in a modification of the surface roughness and in an introduction of compressive stresses near the surface region.

3.1. Roughness

First, samples were investigated in accordance with their surface roughness. *Figure 3* shows a 3-dimensional view of the surfaces, limited to a 4 mm * 4 mm zone. Scale for polished and ground planes is from $-5\mu\text{m}$ to $+5\mu\text{m}$ while the scale for the shot peened one is from $-10\mu\text{m}$ to $+10\mu\text{m}$. These results were obtained using a WYKO 3-dimensional profilometer. The three surface roughnesses R_a , given by the different achievement processes are about $0.4\mu\text{m}$ for the polished specimen, $0.5\mu\text{m}$ for the ground surface and $2.0\mu\text{m}$ for the shot peened one.

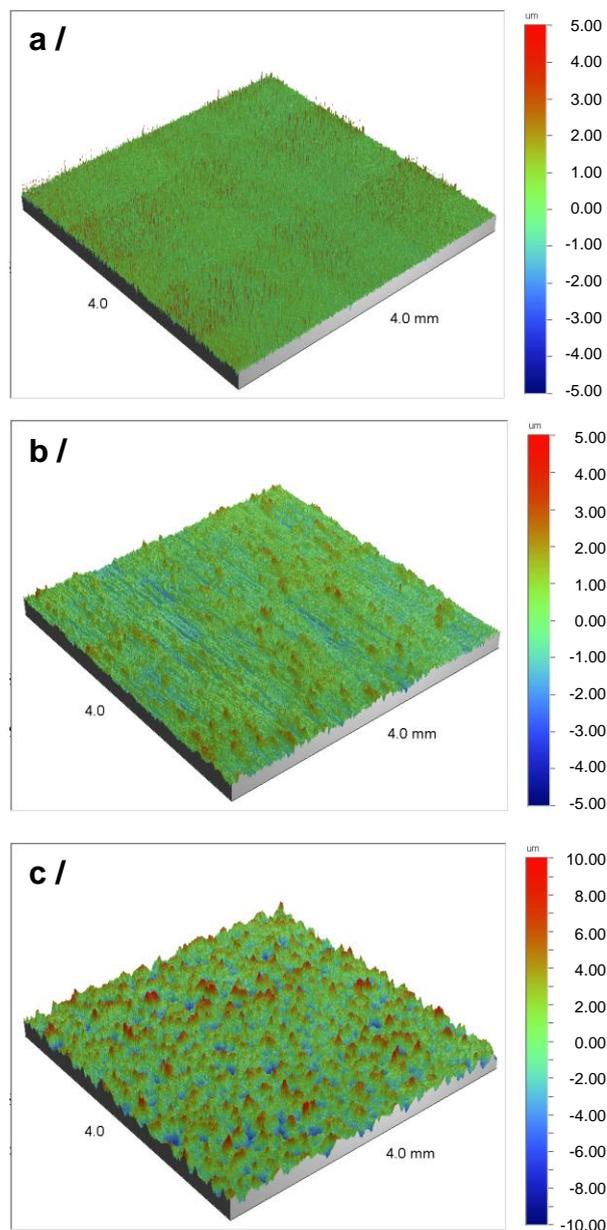


Figure 3 : 3-dimensionnal observation of the three surfaces; a) polished; b) ground; c) shot-peened

3.2. Residual stress

Shot peening induces plastic deformations and compressive residual stresses in the near surface region but machining processes induce stresses in the same way which may affect cracking processes during tests. In fact, shot peening could be assimilated to a machining process because it also involves strain and plasticity near the surface region. The tested surfaces were also characterized with respect to compressive residual stress. Residual stresses were measured by X-ray diffraction. Stress values were determined by comparing the resulting crystal lattice parameter to the known undistorted stress free state of the material. Results for shot peened specimen were presented in *Figure 4*. The polishing process used does not lead to compressive residual stress at the surface. Grinding induces compressive residual stresses but until a low depth from the surface. Maximal compressive stress was about -300 MPa at a deep of 50 μm . Shot peening on Ti-10V-2Fe-3Al introduces higher compressive stresses in a higher range of depth. This surface treatment introduces the maximum value of compressive residual stress with -700 MPa at the deep of 135 μm from the surface. An un-stress state of the shot peened specimens is obtained at about 300 μm in depth. Machining process does not induce much compressive stress compared to shot peening treatment. It is known that shot peened compressive stresses slow down fretting and fretting fatigue cracks. In this paper we also focused on lower compressive stresses obtained by grinding and we will study the effect of different values of compressive stress on the cracking response of the Ti-10V-2Fe-3Al under fretting loading.

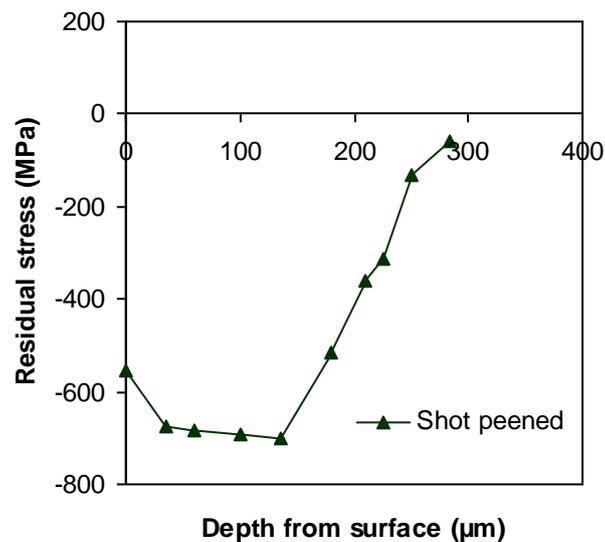


Figure 4: Depth profile of compressive residual stresses for shot peened specimen

3.3. Hardness

In order to have more information about machining and treatment effects on samples, Vickers hardness measurements were performed at the surface with a load of 300g. Hardness of the three samples was presented in *Figure 5*. Polished and ground specimens have the same surface hardness which means that maximum strain induced by the grinding process does not affect hardness compared to polishing process. Measured hardness for the shot peened specimen shows that only higher maximum strain and higher plastic deformations due to treatment affect the surface hardness. Grinding residual stress measured by x-ray diffraction could be not enough to affect material response in fretting because plastic deformations induced by grinding are similar to a polishing process. In opposition, shot peening treatment increases significantly surface hardness with higher maximal strain on the surface.

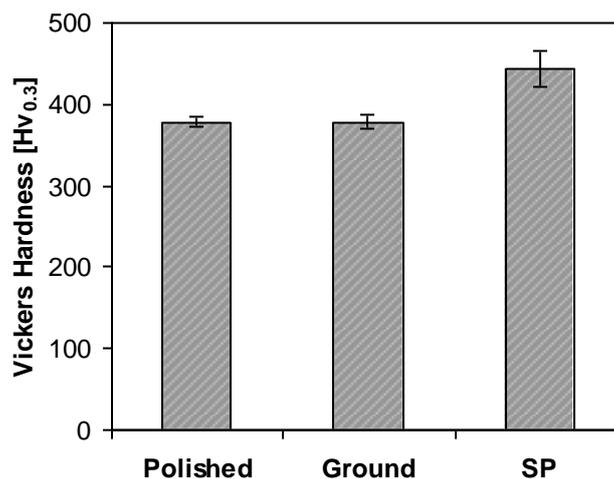


Figure 5. Surface Vickers Hardness

4. Tribological behaviour

4.1. Fretting regime analysis

In this part, the tribological behaviour of the three tested contacts was compared. The first analysis was the sliding regime condition. Sliding conditions were defined as a function of the displacement amplitude and the partial slip regime was dissociated to the gross slip regime for different contact pressures. The range of contact pressure is given in section 2.1. To determine the sliding condition of the studied contact, a variable displacement method, developed at

LTDS, was used [21]. *Figure 6* illustrates the method used. The normal force P is kept constant, while the relative displacement amplitude (δ^*) starts from a very low value, imposing a partial slip regime. When a stabilized condition is reached, δ^* is increased and maintained constant until a new constant displacement amplitude condition is reached. The imposed displacement δ^* is increased in this way, step by step, until the contact makes the transition to gross sliding. During this test, tangential force ratio Q^*/P and the energy ratio A were recorded and reported in *Figure 7* versus the displacement amplitude, δ^* . Then same tests are performed for others normal forces P .

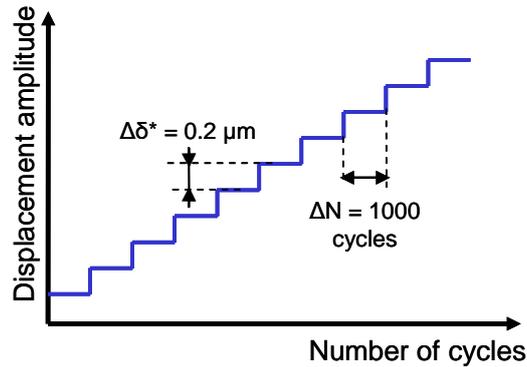


Figure 6 : Illustration of variable displacement method

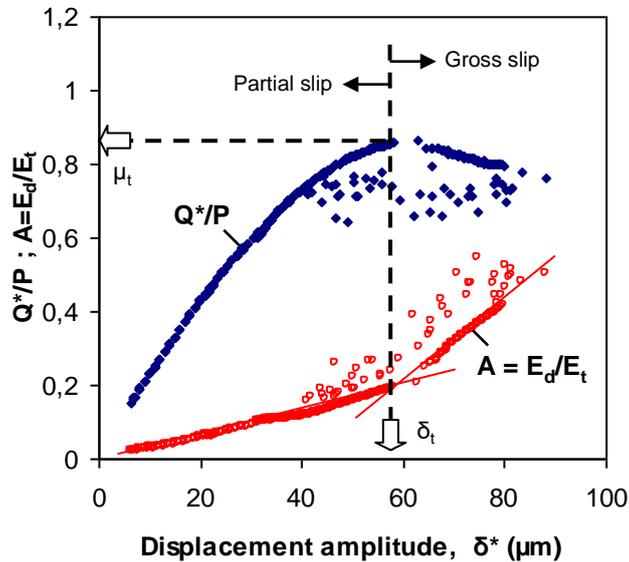


Figure 7 : results of a variable displacement test; $p_0 = p_{ref}$ on ground surface

An accurate value for δ^* at the transition is then calculated by computing the value of the energy sliding criterion $A = E_d/E_t$, where E_d is the frictional energy dissipated during the fretting cycle (area of the fretting loop), and $E_t = 4 \cdot Q^* \cdot \delta^*$ the total energy. It has been

shown that this ratio is a constant value for a sphere on plane configuration and related to a discontinuity in slope for the 2-Dimensional cylinder on plane configuration [22]. By identifying such a discontinuity the measured transition amplitude δ_t , and the friction coefficient at the sliding transition μ_t can be identified.

Sliding regime map for the three tested surfaces were established from results of variable displacement tests (*Figure 8*). In the range of tested contact pressure, the three tested surfaces give the same displacement amplitude at the transition. Machining process and surface treatment do not affect the sliding regime in fretting wear. Even though the roughness of plane sample is different, same adhesive mechanisms are observed in partial slip. Asperities induced by machining or treatment processes wore down due to local plastic deformation during first cycles of the test. Roughnesses do not affect displacement amplitude transition value because they vanished rapidly. Moreover residual stress does not change sliding transition from partial to gross slip regime.

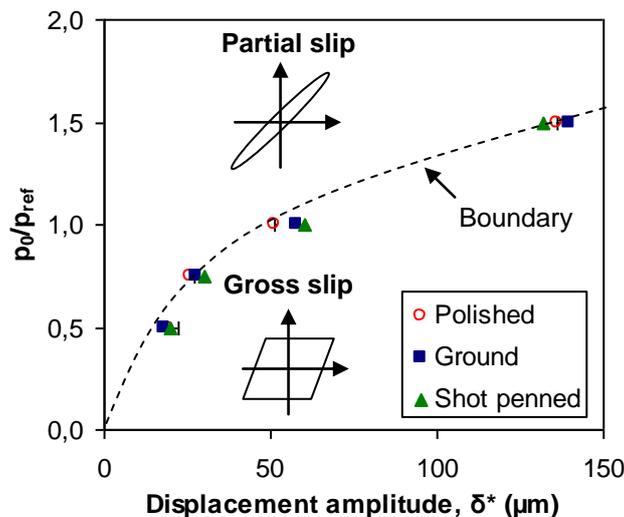


Figure 8 : Results of variable displacement tests: Sliding Regime Map in fretting. Boundary condition (---) between partial and gross slip regime as a function of the maximal pressure and the displacement amplitude, for polished, ground and shot peened contact

The sliding regime condition study concludes that machining process does not have any influence on the displacement amplitude at the transition point from partial slip to gross slip regime. This is observed because roughness has no effect on tribological behaviour. Adding a surface treatment like a shot peening on both the plane and the pad does not change the sliding regime map. It appears that tribological behaviour of the Ti-10V-2Fe-3Al is rather due

to material effect than to roughness or internal stresses effects, in the range of tested parameters.

4.2. Friction coefficient in partial slip

Tribological behaviour investigation was completed by friction coefficient analysis. Friction coefficient in partial slip regime was obtained by an expertise of variable displacement results. Q^*/P ratio at transitions were compared for each contact pressure tested in this part. *Figure 9* gives this parameter as a function of the maximal contact pressure for the three surfaces. First of all, neither the machining process nor the surface treatment influences the friction coefficient at the transition. Moreover Proudhon *et al.* [9] showed that the friction coefficient at the transition, μ_t , is associated with friction coefficient in partial slip regime μ_{ps} for aluminium alloys. Then we can postulate that friction coefficient of a homogenous Ti-10V-2Fe-3Al contact solicited in stabilized partial slip regime is independent of roughness of samples and compressive stresses under the surface, in the range of parameters used.

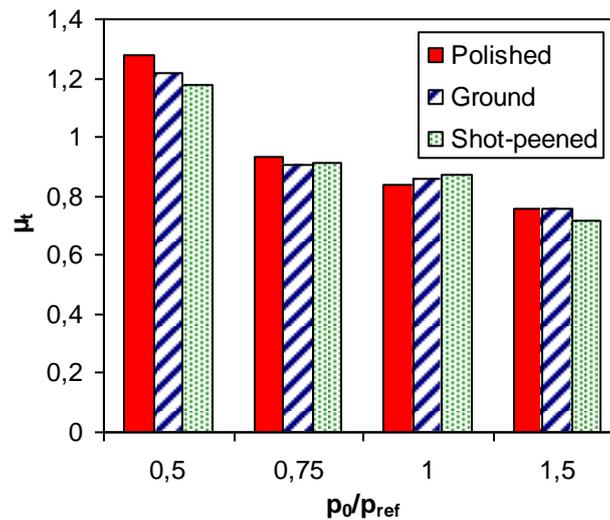


Figure 9 : Q^*/P ratio at the transition after variable displacement tests

For each contact pressure, the mean friction coefficient is calculated. *Figure 10* shows the evolution of the friction coefficient in the partial slip regime as a function of the inverse maximum pressure. The friction coefficient is not constant and tends to decrease with the applied pressure, thus confirming previous results obtained on Ti-6Al-4V titanium alloy

contacts [23]. An empirical composite friction law, including a pressure dependence component β ($\beta/p_{ref} \approx 0.5$) and a constant value μ_{t0} ($\mu_{t0} \approx 0.5$), can be considered for this titanium alloy.

$$\mu_t = \mu_{t0} + \frac{\beta}{p_0} \quad (1)$$

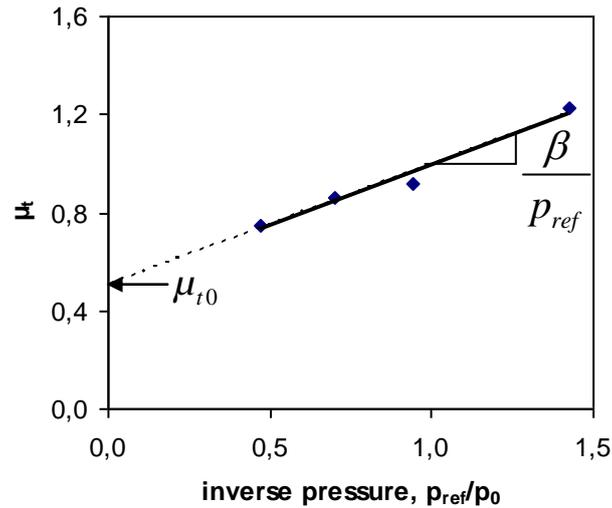


Figure 10 : Friction law of the Ti-10V-2Fe-3Al at the sliding transition

This pressure dependence suggests that the friction law near the sliding transition is partly controlled by adhesion and metal transfer phenomena. A constant plastic interfacial shear stress component (β) could be assumed so that the interfacial shear stress at the sliding transition is expressed by.

$$\tau_t = \frac{\mu_t}{p_0} = \frac{\mu_{t0}}{p_0} + \frac{\beta}{p_0^2} \quad (2)$$

5. Cracking analysis

The aim of this study is to observe the cracking behaviour without any fatigue load for three contacts. In this scenario, the contact configuration is fixed and the only varying parameter is the tangential force amplitude. In this cracking analysis, contact pressure is kept constant and

defined as $p_0 = p_{ref}$. Crack nucleation was characterized by the following methodology [24] represented in *Figure 11*. Several tests were performed at different tangential load values to find the correct crack nucleation threshold Q_c . In order to measure maximal crack depth, each test has been analysed following the methodology described in section 2.4

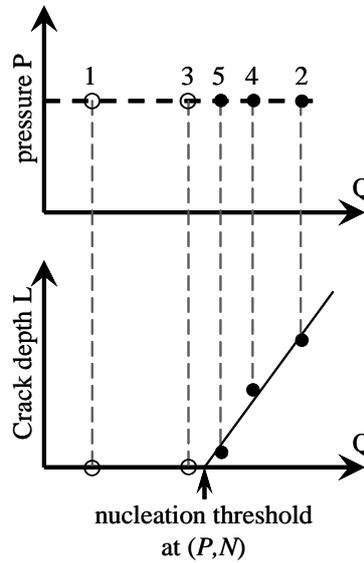


Figure 11 : Methodology used to determine the crack nucleation threshold

Figure 12 shows the maximal crack depth, normalized by the maximal crack depth observed in this paper, as a function of the normalized tangential force amplitude applied in the test. The value of the maximal crack depth observed in this work is about $500\mu\text{m}$ and tangential force is normalized in order to obtain $Q_{th}/(\mu_t \cdot P) = 1$ at the sliding transition. These results show that crack nucleation was obtained at $Q_{th}/(\mu_t \cdot P) = 0,56$ for the polished surface, where $Q_{th}/(\mu_t \cdot P)$ is the normalized tangential force amplitude at the crack nucleation threshold. Below this value, no cracks were observed after a 10^6 cycles fretting test. For tangential forces higher than the crack nucleation threshold, cracks depth increases with the applied force in the test. Grinding process studied in this paper has no significantly influence on the crack nucleation and propagation in fretting wear compared to polished surfaces. Depth of cracks is the same for both surfaces in the range of tangential force applied. Crack nucleation threshold for the ground surface is also defined as the same value as for polished contact. Even though there are compressive residual stresses induced under the surface by grinding process, they have no effect on the crack nucleation and are no sufficient to decrease cracks length in fretting wear.

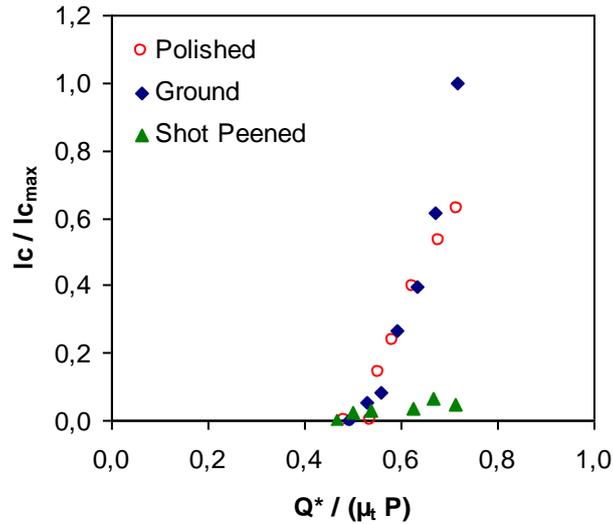


Figure 12 : Maximal cracks depth as a function of the tangential force, (contact pressure $p = p_{ref}$)

To the contrary compressive residual stresses induced by shot peening seems to be sufficient to decrease cracks length in fretting wear. Crack nucleation threshold for shot peened surface is the same than for polished and grinded ones. For a same tangential load, deep of cracks are lower for the shot peened sample than for the polished one. Compressive residual stresses induced under the surface are sufficient to decrease cracks depth in fretting. This can explain why fretting fatigue strength of shot-peened Ti-10V-2Fe-3Al specimens is higher than for grinded ones.

Machining process tested in this paper does not affect the cracking response of the Ti-10V-2Fe-3Al when it is solicited in fretting wear. Compressive residual stresses induced by machining are not sufficient to decrease cracks length but higher compressive residual stresses brought in by specific treatment processes decrease cracks length in fretting. We can suppose that this result can explain why shot peened components have higher fretting fatigue lifetime. This point will be developed in a future work.

6. Prediction of the crack nucleation

6.1. Crossland's multiaxial fatigue criterion

To predict the fretting-fatigue crack nucleation risk at the fatigue limit condition (i.e. 10^6 cycles), the Crossland's multiaxial fatigue criterion is applied [25]. The crack risk is expressed as a linear combination of the maximum amplitude of the second invariant of the stress deviator ($\sqrt{J_2(t)}$) denoted by ξ_a , and the maximum value of the hydrostatic pressure ($P_{h \max}$).

The non cracking condition is expressed by:

$$\xi_a + \alpha_c \cdot P_{h \max} < \tau_d \quad (3)$$

Where,

$$P_{h \max} = \max_{t \in T} \left(\frac{1}{3} \text{trace}(\Sigma(t)) \right) \quad (4)$$

$$\xi_a = \max_{t_0 \in T} \left\{ \max_{t \in T} \left[\frac{1}{2} (S(t) - S(t_0)) : (S(t) - S(t_0)) \right]^{1/2} \right\} \quad (5)$$

$$\alpha_c = \frac{\tau_d - \sigma_d / \sqrt{3}}{\sigma_d / 3} \quad (6)$$

With, S the deviatoric part of Σ , σ_d the alternating bending fatigue limit and τ_d the alternating shear fatigue limit.

The cracking risk can be quantified through a scalar variable:

$$d_c = \frac{\xi_a}{\tau_d - \alpha_c \cdot P_{h \max}} \quad (7)$$

The cracking condition is then expressed as:

- If d_c is greater or equal to 1, there is a cracking risk;
- If d_c remains less than 1, there is no risk of cracking.

6.2. Crack nucleation prediction on plain fretting condition

The first step of the methodology is to calibrate the model by computing the experimental crack nucleation limit defined from previous fretting tests. The multiaxial fatigue analysis is so performed for the threshold crack nucleation condition. Confirming the experiments, *Figure 13* shows that the maximum crack risk is located at the contact borders but the computed value is above 1. The current point stress analysis over-estimates the cracking risk. To take into account contact size and the associated stress gradient effect, one strategy consists in averaging the stress state over a representative “crack nucleation process volume”. The point stress identification is replaced by a mean loading state $\bar{\Sigma}(M, t)$ averaged over a micro volume $V(M)$ surrounding the point on which the fatigue analysis is performed ($M(x, y)$) *Figure 14* [26]. The mean stress state for the studied 2D cylinder on plane configuration is now identified by averaging the local stress state related to the 9 points defining the cubic square:

$$\bar{\Sigma}(M(x, y, z)) = \frac{1}{9} \cdot \Sigma \left(M \left(x + i \cdot \frac{\ell}{2}, y + j \cdot \frac{\ell}{2} \right) \right) \quad (8)$$

This micro volume of matter is approximated through a cubic volume, of edges are assimilated to the physical length scale ‘ ℓ ’. The critical length parameter is determined by successive iterations on ‘ ℓ ’ until it corresponds to the threshold condition ($d_c = 1$). Therefore, a critical length of $\ell_c = 6 \mu\text{m}$ seems to correlate the predictive model to experimental data, for the tested contact pressure, p_{ref} .

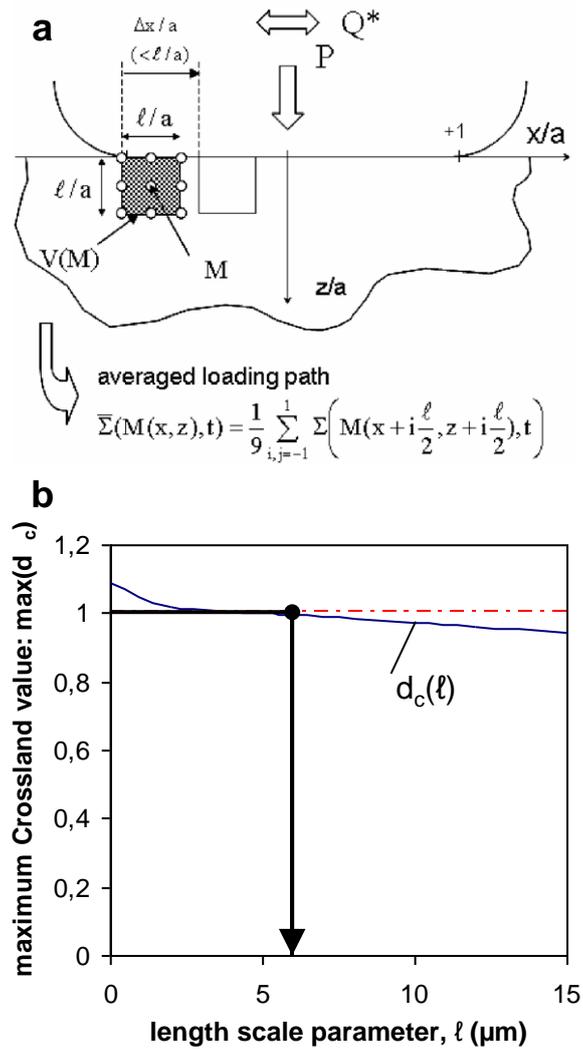


Figure 13 : Applied "stress area method": (a) illustration of the averaging procedure, (b) identification of the length scale parameter ℓ

7. Conclusion

In this paper, the tribological behaviour and the cracking response of the Ti-10V-2Fe-3Al titanium alloy were investigated through fretting test in partial slip regime. Three surfaces obtained by polishing, grinding and shot peening processes were compared regarding roughness, hardness and residual stress. Then they were tested in fretting.

Tribological behaviour was investigated through variable displacement tests. Each surface was tested in a maximal hertzian pressure range of 280 MPa to 850 MPa. In that range of tested pressure, roughness and compressive residual stress induce by machining and surface

treatment processes do not affect neither the sliding regime condition nor friction coefficient in the partial slip regime. We can conclude that tribological behaviour of a homogenous Ti-10V-2Fe-3Al contact solicited in partial slip regime is independent of roughness of sample and of residual stress under the surface.

Cracking response was studied with fretting tests in partial slip regime. Depths of cracks were measured through cross section observation. Crack nucleation threshold is independent of the roughness or the residual stress in samples. Cracks depth of tests where tangential force is higher than the crack nucleation threshold shows that residual stresses induced by grinding process are not sufficient to slow down cracks after the nucleation. This result confirms that grinding do not affect the cracking response of a Ti-10V-2Fe-3Al solicited in fretting.

Shot peening is known as one of the most used surface treatment to prevent from cracking process. The crack nucleation threshold do not seems to be affected by shot peening but cracks lengths are shallower than for polished specimens. Compressive residual stress induced by the shot peening are sufficient to slow down cracks and increase resistance of sample regarding to fatigue or fretting loadings.

The Crossland multiaxial fatigue criterion has been used to predict the cracking risk. A local approach over-estimated the cracking risk which constrains us to take into account the stress gradient effect into the contact. This can be achieved by considering a process volume methodology, where the stress state considered for the multiaxial fatigue analysis is averaged over a representative volume. This work shows that the process volume methodology, calibrated using plain fretting tests, works for our contact and gives a critical length scale parameter ℓ_c of $6\mu\text{m}$.

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