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# Fretting wear of alloy steels at the blade tip of steam turbines

M. Lavella<sup>\* (1)</sup>, D. Botto <sup>(1)</sup>

(1) Department of Mechanical and Aerospace Engineering, Politecnico di Torino Corso Duca degli Abruzzi, 24-10129 Torino – Italy

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#### Abstract

In order to reduce blade resonant vibration amplitude in turbomachinery, blades are assembled with a mutual interlocking at the tip. The aim of this study is to investigate the wear mechanism at the contact interface of the blade shroud in steam turbines. Experimental data are available concerning the wear mechanism at interfaces of aircraft engines blades, while the literature regarding the same effect on steam turbines is less rich. Moreover, the transposition of the results from the aero engine to the steam turbine is difficult, because materials and working conditions are different. To overcome this lack of knowledge an experimental campaign was set up to investigate this wear mechanism under the specific conditions and with the distinctive materials used in steam turbines.

Two base materials (alloy steels) were tested under different conditions: surface treatment (with and without laser 24 quenching), temperature and normal load. Dissipated energies were determined from the hysteresis loops measured 25 during the tests and were correlated to the test conditions. Profiles of worn surfaces were measured, and volume 26 losses were accurately computed with a procedure that takes into account the roughness of the surfaces.

Experiments were conducted both at room and low temperature (150 °C). At room temperature the surface 28 temperature increased to 60-70 °C, due to the heat generated in the wear process. Comparison of volume losses at 29 room and low temperature showed that at 150°C the volume losses decreased dramatically. This behaviour was 30 explained with a brittle-ductile transition. In other words, the same wear mechanism, adhesion and abrasion 31 respectively in stick and gross slip condition, give very different results for a small softening effect of the material.

32 Moreover, experimental results showed much more sensitive wear rates to the heat treatment than to the steel type.

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34 Keywords: fretting, wear, M152 steel, X20Cr13 steel, steam turbine, brittle-ductile transition.

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<sup>\*</sup>Corresponding author. Tel.: +39 011 0906935; fax: +39 011 0906999. E-mail address: mario.lavella@polito.it (Mario Lavella)

#### 1 1. Introduction

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3 Steam turbines are used as drivers of electrical generators, oil refinery, naval propulsion and many others important applications. Steam turbines cover a very wide variety of sizes (from 4 hundreds of kW to many GW) and configurations. As electrical energy is mainly generated by 5 6 steam turbines, breakdowns are detrimental for the economy. These applications are usually 7 strategic for life and safety. Maintenance programmes are carefully scheduled to evaluate the integrity of turbomachinery components, particularly of the blades, to decrease breakdown risks 8 9 in turbines. On the other hand, the steam turbine industry aims to increase the maintenance interval, to reduce the turbine out-service time. High cycle fatigue cracks in blades are nucleated 10 and propagated by turbine vibrations [1] that increase displacement amplitude of the blade and 11 related stress. Consequently, the identification techniques are continuously developed in order 12 to better characterize blade vibrations [2]. Fillets at the blade root and eroded wear zones are 13 14 typical sites of crack nucleation. For this reason many studies have been published to better understand the failure mechanism by erosion wear in steam turbine blades [3]-[7]. 15

16 A widely used method to reduce displacement amplitude of the blade is to increase the internal damping of the blade assembly. This is done by introducing under-platform dampers, 17 18 that are devices placed close to the blade shank. These devices dissipate part of the vibrational 19 energy in the contact between the damper and the blade platform [8]-[10]. A different way to 20 decrease the amplitude displacement of blades is to increase the stiffness of the assembly. Blades are fit with interference with the neighbouring blades. Interference fit, also denoted as 21 22 "interlocking", is usually given at the blade tip where a shroud is machined. If vibrations generate excessive relative displacement of adjacent blades, wear occurs at the contact surface of the 23 24 shrouds and causes loss of interlocking. Diminished interlocking decreases the assembly stiffness and then is less effective in reducing amplitude displacement of the blades. This 25 condition triggers a loop that could lead to catastrophic failure. Consequently, to improve the 26 dynamic design of the bladed disk and to schedule appropriate maintenance intervals, knowledge 27 of the contact parameters (friction coefficient and contact stiffness), wear mechanisms and wear 28 29 evolution are of paramount importance.

30 In the gas turbine, blade failure is commonly mitigated by reducing the vibration amplitude 31 of the blade using dampers and shrouds. Recently this strategy is also applied to the steam 1 turbine. These interfaces have already been studied for aircraft engines: some examples are [11]-

2 [13]. As the materials and the service condition of the aircraft engines are different, the results

3 obtained cannot be transferred to steam turbine.

4 This paper shows the methodology and the results of an experimental campaign performed 5 to investigate the wear mechanism and wear evolution of the typical materials used for blades in low-pressure stages in steam turbines. Test parameters were representative of the typical 6 7 operating conditions. High-precision test rig [14]-[15] with free approach of flat on flat contact 8 surfaces was used in order to have a rigorous measurements of wear evolution. These fretting 9 experiments are not affected by imprecisions due to the assembling-reassembling of specimens and test rig compliance. Assembling-reassembling operations are necessary for the topological 10 measurements of the contact surface. The test rig compliance affects the relative displacement 11 measurements introducing a difference between imposed and real displacement amplitude. Wear 12 volume measurements was performed considering the roughness of the contact surfaces. Novel 13 14 interpretations in terms of surface integrity and brittle-ductile transition of the oxides layers on 15 contact interfaces was proposed.

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### 17 **2.** Methods and materials

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19 The tribological properties of low pressure shroud blades for steam turbine applications were

20 evaluate by means of experimental fretting tests. The fretting wear on these mating surfaces, see

21 Fig. 1, is generate by the blade vibrations.



Fig. 1. Fretted mating surfaces at blade tip.

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1 Fretting tests were performed by means of the test rig illustrated in Fig. 2. All the

2 characteristic of test rig can be found in [14].

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Fig. 2. Test rig.

7 The main peculiarities of this test rig are: flat on flat contact with free approaching of contact 8 surfaces; displacement control; contact force and relative displacement measured continuously for each hysteresis loop; working temperature from Room Temperature (RT) to 1000 °C. The 9 10 relative displacement are measured and very near to the contact interfaces, substantially there is 11 no difference between the imposed sliding amplitude and the applied amplitude, about the importance of imposed and applied sliding amplitude [16]. In other words, there are no effect of 12 test rig compliance on the displacement measurements [17]. Moreover, because of the free 13 approaching and the fixture specimen's characteristic, the test rig allows the disassembling and 14 re-assembling with a 15 subsequent negligible impact on the fretting process. Disassembling – assembling operations are necessary to measure the 3D profile of the worn 16 surfaces and to determine the wear volume. A great advantage of this rig is that wear volume 17 measurements can be done on the same couple of specimens at different stage of the fretting 18 19 process.

The geometry of specimens is shown in Fig. 3. Two sliders, symmetrically located with 1 2 respect to the specimen centre, are machined on the specimen mounted on the fixed support. The specimen was assembled with the slider longitudinal axis parallel to the sliding direction. The 3 length of contact surfaces measured along the sliding direction was 5.4 mm while the width of 4 5 the contact surfaces measured along the orthogonal direction was 1 mm, Fig. 3. The specimen 6 mounted on the mobile support had only one contact surface. The length of this contact surface measured along the sliding direction was the same of the slider (5.4 mm). This design condition 7 8 allows to avoid the impact with the groove edge at the inversion motion points. Consequently, even the strain hardening effect at the inversion points of the hysteresis loops is avoided [11]. 9



10 11

Fig. 3. Fixed and mobile sample: SD Sliding Direction; length of contact surfaces measured along the sliding direction 5.4 mm, width of contact surfaces measured in the direction orthogonal to the sliding direction 1 mm.

The pressure distribution for a given normal load and the relative displacement associated to a tangential load can be simulated using the method proposed in [18]. In this method, a reduced order model is obtained from a full finite element model using a static reduction. The sliders give two nominally equal pairs of contact surfaces. Each pair had a surface 5.4 mm<sup>2</sup> while the overall contact surface of the couple of specimens was 10.8 mm<sup>2</sup>. These two pairs of contact surface

1 ware subject to the same fretting process but wear volume was measured independently to have

2 one repetition of the fretting process.

3 Experiments ware performed to investigate some alternative for the blade tip shrouds of steam turbine at stages L-2, L-1, L-0. Effects of material, heat treatment, normal load, and 4 temperature were analysed. Particularly, tests were performed on two stainless steels (M152 and 5 X20Cr13), two heat treatments (quenching – tempering with and without laser quenching), two 6 nominal contact pressures (25 and 15 MPa), two temperatures (RT and 150 °C), one stroke (50 7 um), one frequency (175 Hz). The durations of the wear process were  $25 \times 10^6$  cycles for all tests 8 except two which had a duration of  $15 \times 10^6$  cycles. Each wear test was divided in stint of  $5 \times 10^6$ 9 wear cycles and the worn surfaces profiles were evaluated at the end of each stint. Worn volumes 10 were computed using the topographic measurements of contact surfaces, measured in unworn 11 condition, and after  $5 \times 10^6$ ,  $10 \times 10^6$ ,  $15 \times 10^6$ ,  $20 \times 10^6$ ,  $25 \times 10^6$  wear cycles. 12

13 The wear volume was evaluated with a procedure that considers the roughness of the mating 14 surfaces [13]. It was measured by comparison of peaks and holes volume measurements of asperities at different steps of fretting process starting from unworn conditions. Peak and hole 15 16 volumes was defined using a reference plane which was set by means of last square interpolation of unworn area. Consequently, a positive wear volume is obtained if the sum of peaks volumes 17 18 decreases and the sum of holes volumes increases. In the case of the slides, the comparison was 19 made using the entire volume of one slider at each step of the wear process. Each slider and the 20 relative contact surface on the flat sample was evaluate without any influence of the other slider. This strategy was used in order to evaluate the repeatability of the measurements on each couple 21 22 of contact surfaces.

23 During the fretting processes the relative displacement and the tangential contact force were measured and stored. The tangential contact force plotted against the relative displacement gives 24 the hysteresis loop. The contact parameters, namely friction coefficient and contact stiffness, 25 were obtained by post-processing hysteresis loops. The area of the hysteresis loop is the energy 26 dissipated during a fretting cycle. In this work, the dissipated energy at each loops acquisition 27 was evaluated measuring by integration the areas on the hysteresis loops acquired during fretting 28 29 process. Twenty loops were stored per each acquisition. These loops were used for computing an average dissipated energy [13], [14] characteristic of the specific acquisition. The total 30 31 dissipated energy losses at a specific cycle during the fretting process is the sum of the energy

dissipated par each cycle from the first. Due to the huge amount of data, loops were not stored 1 2 continuously over the fretting process. As an acceptable alternative, the hysteresis loops were stored after a pre-planned number of cycles. The sum of dissipated energies were based on the 3 average energies computed at each loops acquisitions. Between two subsequent loop acquisitions 4 5 the dissipated energy for each cycle were obtained by linear interpolation. The loops data acquisitions were performed every  $0.25 \times 10^6$  cycles. During the beginning, before  $0.25 \times 10^6$ 6 cycles and the end a wear stage  $(4.75 \times 10^6 \div 5 \times 10^6 \text{ cycles})$  the acquisitions were more 7 8 frequent.

9

#### 10 **3. Results and discussion**

The detailed experimental plan is reported in Table 1. It details normal loads, materials, heat treatment, and test temperature. Fig. 4 illustrates the wear volume as a function of wear cycles for M152 and X20Cr13 steels at RT. The wear volume was obtained as the sum wear volume measured on both mating surfaces. There are two measurements of wear volumes for each number of cycles because of the two sliders. The wear volume relation as a function of wear cycles shows a linear evolution with a good repeatability.

17

Table 1: experimental plan.						
ID couple	Material	Heat	Temp,	Normal	Contact	Cycles,
		treating	С	Load,	Pressure,	$(\times 10^{6})$
				Ν	MPa (1)	
#01-M152	M152	Q-T	RT	271.8	25	25
#02-M152	M152	Q-T-LQ	RT	271.8	25	25
#03-X20Cr13	X20Cr13	Q-T	150	271.8	25	25
#04-X20Cr13	X20Cr13	Q-T-LQ	150	271.8	25	25
#05-X20Cr13	X20Cr13	Q-T	RT	271.8	25	25
#06-M152	M152	Q-T	RT	271.8	25	25
#09-X20Cr13	X20Cr13	Q-T	RT	163.7	15	15
#10-M152	M152	Q-T	RT	163.7	15	15
Common data and abbreviations:						
Stroke:	50 µm					
Frequency:	175 Hz					
Q-T:	Quenching and Tempering;					
Q-T-LQ:	Quenching, Tempering and Laser Quenching;					
-	-					

Temp: Temperature

RT: Room Temperature

(1) Nominal value Normal load/Contact surface

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17 Fig. 5. Effect of normal load, wear volume as a function dissipated energy for two normal loads: (a) M152; (b) 18 X20Cr13. 19

Energy, kJ

Energy, kJ

The relation between wear volume and dissipated energy is linear similarly to the relation 1 2 between volume and cycles, but the variation due to the different normal load is much less evident. Moreover, the difference of slope is slightly higher at lower normal load for M152 (Fig. 3 5 (a)) while it is slightly lower at lower normal load for the X20Cr13 (Fig. 5 (b)). This result 4 5 suggests that the relation between wear volume and dissipated energy is independent from the 6 normal load. As a consequence, experiments at low normal load can be included in the same volume – energy relation of those at higher normal load. The difference of the higher – lower 7 8 normal load slopes can be attributed to unavoidable measurement errors. The fretting regimes of 9 these tests were gross slip.

10 The influence on the wear rate of materials and heat treatment at RT is summarized in Fig. 11 6. This figure reports wear volume as a function of dissipated for M152 with and without laser 12 quenching and X20Cr13 at RT. These results indicate that the wear rate is more influenced from 13 the heat treatment than from the material. The X20Cr13 shows a wear rate lower than that of the 14 M152. On the other hand, the effect of the surface laser quenching on the X20Cr13 revels lower 15 wear rates than the difference observed by comparison of two steels without surface heat 16 treatment. Thus, the beneficial effect of the laser quenching is evident at RT.



17 18

Fig. 6. Materials and heat treatment comparison, wear volume as a function accumulated dissipated energy.



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Fig. 7. Plan/slider comparison, wear volume as a function accumulated dissipated energy (a) M152; (b) X20Cr13.

3 4

5 In this figure the volume measurements made on the plane specimens (Fig. 3) were reported with 6 solid markers (circular for the M152 and triangular for X20Cr13) while the measurements on 7 the sliders were reported using empty markers (circular for the M152 and triangular for XCr13). 8 The colours identify only the ID of the experiment. The first consideration is that the difference 9 of wear rate between M152 and X20Cr13 observed in Fig. 6 can essentially be attributed to the 10 sliders. The reason is that wear rate of the plane is substantially the same for both steels. On the other hand, the wear rate of the X20Cr13 sliders is lower than M152 sliders. The second and 11 12 more evident consideration is that the plane specimens have a wear rate that is much lower than the sliders. This result means that the surface completely in contact (slider) shows a higher wear 13 14 rate than the surface partially in contact (plane). Moreover, there is no difference between plane and slider surfaces along the fretting direction, the materials were the same with the same heat 15 16 treatment and both type of specimens were obtained by same type of fabrication process. It seems that the part of the surface which has no contact (transversal to the fretting direction) has an 17 18 influence on the wear rate. The fraction of the area without contact seems to reduce the wear rate 19 of the surface fraction which is in contact with the slider. In other words, it seems a sort of surface 20 integrity question. The fabrication process of the slider reduces the tribological properties more 21 than the same type of fabrication process applied to the flat surface. 22 The effect of the temperature on X20Cr13 is illustrated in figure Fig. 8. The figure compares

the wear volume by fretting process at RT with those at temperature of 150 °C with and without
laser quenching. It is evident that the wear rate at 150 °C drastically decreases if compared with
the RT.



Fig. 8. Wear volume as a function accumulated dissipated energy at different temperatures.

3 4

> 5 The beneficial effect of the laser quenching process on wear rate is confirmed at the temperature 6 of 150 °C if the dissipated energy is higher than 175 kJ. Under this threshold energy, the heat 7 treatment seems to have no important influence.

8 The dissipated energy at 150 °C is lower than that at RT. Moreover, the laser quenched 9 specimens show the lowest dissipated energy. This energy is slightly lower than the dissipated 10 energy by specimens without laser quenching. Considering that the number of fretting cycles at 11 RT was the same than at 150 °C, this means that the area of the hysteresis loops decreases, and 12 consequently the friction coefficient decreases as the stroke is the same. Thus, there is a softening 13 effect of the contact tangential force.

14 The temperatures of the contact interfaces were measured also during fretting experiments performed at RT by means of a thermocouple placed in the central position along the fretting 15 direction at the distance of 1 mm from the contact interfaces [13]. In general the temperature of 16 the contact interface during the fretting process at RT can reach about 60 - 70 °C for these 17 geometries and process parameters. The maximum temperatures measured by thermocouple 18 during the fretting process shown in Fig. 8 were 61 °C for the tribocouple #05 and 61 °C for the 19 20 #05. Fig. 9 shows temperature increment of contact surface and dissipated energy per cycles 21 measured at different fretting cycle. The temperature increment had the RT as a reference initial 22 temperature. It was 22 °C for the experiment #05 and 27 °C for the #09. The dissipated energy per cycle should not be confused with the sum of the energy dissipated from each fretting cycles 23 24 (accumulated dissipated energy) used before.



Fig. 9. Dissipated energy per cycle and contact temperature variation with reference to the room temperatures (22 °C for #05 and 27 °C for #09).

1 2 3

6 Fig. 9 evidences the coherence between the temperature increment and the dissipated energy per 7 cycle. Substantially energy and temperature have the same evolution as a function of the number of fretting cycles. Tests were stopped and restarted every  $5 \times 10^6$  cycle, consequently the 8 temperature increment started from zero and reached the thermal regime when the dissipated 9 10 energy balanced the heat transferred to the external environment. The initial increase in energy 11 per cycle and consequently in temperature is due to the evolution of the sliding amplitude. This is regulated by the test rig control software to reach the target displacement stroke as soon as 12 13 possible. Considering that the dissipated energy per cycle is strictly related to the stroke, a few 14 cycles are necessary to reach the target displacement stroke and consequently the thermal regime. 15 Moreover, from Fig. 9 it also evident that during tests performed at RT the contact temperature is higher ( $60 \div 70$  °C) than RT. This is a direct consequence of the energy dissipated by friction. 16 In other words, the real temperature difference between the tests was about  $80 \div 90$  °C ( $\cong 150$  -17 18  $60 \div 70$ ). This small temperature difference has a negligible effect on the mechanical properties 19 of the analysed stainless steel, particularly at low temperature (150 °C). In contrast, the wear rate 20 drastically decreases (Fig. 8). This small variation of the mechanical properties induces a drastic 21 variation of fretting wear behaviour and suggests that there may be a transition state activated by 22 an increasing in temperature. A large amount of literature, a representative list of works is 23 reported in [19]-[25], is available on the severe-mild wear transition of steels. Typically, steels 24 show a transition between severe to mild wear at temperature of about 80-220°C: beyond this 25 temperature the fretting wear rate shows a sharp decreasing. The transition temperature seems to

depend on the steel type: it was found 80 °C for the S45C/S45C and 200 °C for SUS304/SUS304 1 2 in [21], 220 °C for M152/A286 in [22] and 85 °C for super-CMV/super-CMV in [23]. These 3 transition temperature evokes a similar temperature for similar  $\alpha$ - $\gamma$  steel structures, in fact S45C. M152 and super-CMV have a  $\alpha$ -structure while SUS304, A286 have  $\gamma$ -structure. In other words, 4 5 the transition temperature seems to be about 80 °C for the alloy wit α-structure and about 200 °C 6 for the  $\gamma$ -structure. Early works [19]-[21] associated this transition to the change in chemical 7 composition and oxidation law of the oxides film built-up during the fretting process on the 8 mating surfaces. The composition evolves from  $\alpha$ -Fe<sub>2</sub>O<sub>3</sub> to Fe<sub>3</sub>O<sub>4</sub> while the oxide layer growth 9 changes from logarithmic to parabolic grown. In [22] and [23] the transition is explained in terms of accumulations of debris retained inside the wear groove that leads to the formation of a stable-10 load bearing bed. This debris bed is due to the tribo-sintering process described in [24], [25] and 11 evolves towards a glaze-layer as a consequence of a further temperature increasing. In [22] the 12 transition occurs at about 210 °C and the glaze-layer is completely formed at 285 °C; in [23] the 13 14 transition ensues at 85 °C and the glaze-layer is formed at 150 °C. According to this evidence, the severe-mild wear transition is usually located at the temperature of activation of the glaze-15 layer. However, the variation of temperature required by the wear rate transition is much lower 16 than the variation of temperature required by the formation of the glaze-layer [22], [23]. Because 17 18 of an increasing in temperature, the glaze-layer formation seems to be more gradual of the 19 severe-mild wear transition. On the other hand, the glaze-layer formation explains the typical 20 progressive reduction of the friction coefficient as a function of temperature reported in [12], [13], [22], [23]. This motivation seems to be less exhaustive if it refers to the wear rate transition. 21 22 The reason is that the wear rate transition is very sharp while the sintering process of debris as a function of temperature is a gradual phenomenon. These considerations suggest the hypotheses 23 24 that the evolution of the oxides layers by tribo-sintering process is coupled with a change of the damage mechanism from brittle to ductile. A brittle ductile transition may change significantly 25 26 the production of debris by fretting process. Usually the  $\alpha$ -structure shows brittle-ductile transition while  $\gamma$ -structure exhibit a ductile behaviour. Thus, when oxides with  $\alpha$ -structure are 27 28 predominant the damage mechanism could be brittle while when oxides with  $\gamma$ -structure are 29 predominant the damage mechanism is ductile. The hypothesis is also compatible with the 30 change of transition temperature of  $\alpha$ - $\gamma$  structures. In any case future research should verify this hypothesis of transition. 31

## 2 **4.** Conclusion

3 An experimental campaign was performed to investigate the fretting wear mechanism and 4 wear evolution of the typical materials used for blades in low-pressure stages in steam turbines. 5 The test parameters were representative of the typical operating conditions. A high-precision test rig with free approach of flat on flat contact surfaces was used in order to have rigorous 6 7 measurements of wear evolution. Fretting experiments were not affected by imprecisions due to 8 the assembling-reassembling of specimens and test rig compliance. Wear volume measurements 9 as a function of wear cycles and dissipated energy were performed considering the roughness of the contact surfaces. The influences of normal load, material, heat treatment, surface integrity 10 11 and temperature were illustrated. A wear rate as a function of the dissipated energy was found 12 independent from the normal load. The analysed steels showed that the wear rate was far 13 influenced by heat treatment than by the steel type. The beneficial effect of laser quenching was 14 observed. The novel effect of the surface integrity has been highlighted. The surface integrity 15 seems to play an important role and the plane surface shows a wear rate of about 60% of the 16 wear rate of the sliders. During the fretting process at room temperature, the contact surface 17 temperature increased up to 60-70 °C, due to the heat generated during the wear process. The 18 comparison of experiments conducted at room and low temperature (150 °C) showed that a small 19 variation of the test temperature originated a drastic variation of the wear rate. In other words, the same wear mechanism, adhesion and abrasion respectively in stick and gross slip condition, 20 21 originated very different results, in terms of wear volume, due to a small softening effect of the 22 material. This behaviour suggests a novel interpretation in terms of brittle-ductile transition of 23 the oxides layers formed on the contact interfaces.

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