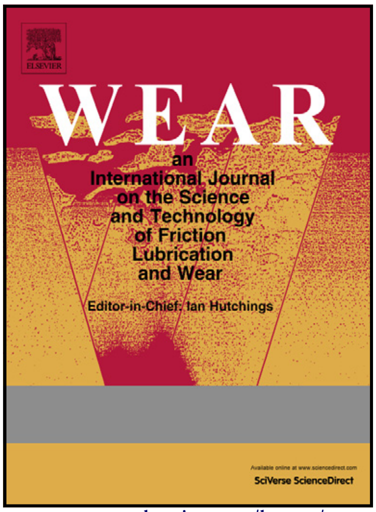
Author's Accepted Manuscript



www.elsevier.com/locate/wear

Tool life and wear mechanisms in laser assisted milling Ti-6Al-4V

M.J. Bermingham, W.M. Sim, D. Kent, S. Gardiner, M.S. Dargusch

PII: S0043-1648(14)00327-5
DOI: http://dx.doi.org/10.1016/j.wear.2014.11.001
Reference: WEA101135

To appear in: *Wear*

Received date: 9 July 2014
Revised date: 28 October 2014
Accepted date: 5 November 2014

Cite this article as: M.J. Bermingham, W.M. Sim, D. Kent, S. Gardiner, M.S. Dargusch, Tool life and wear mechanisms in laser assisted milling Ti-6Al-4V, *Wear,* http://dx.doi.org/10.1016/j.wear.2014.11.001

Tool life and wear mechanisms in laser assisted milling Ti-6Al-4V

M.J. Bermingham[1a], W.M. Sim[b], D. Kent[a], S. Gardiner[b], M.S. Dargusch[a]

[a]Queensland Centre for Advanced Materials Processing and Manufacturing (AMPAM), The University of Queensland, Australia

[b]Airbus Operations Ltd., Pegasus House, Aerospace Avenue, Filton, Bristol BS99 7AR, United Kingdom

Abstract

Thermally assisted machining processes are gaining popularity among researchers and engineers as a method for improving the machinability of difficult-to-cut materials such as titanium. The process of artificially introducing heat to the cutting zone is reported to have many benefits; however, it remains unclear whether the process offers any tool life improvements during milling Ti-6Al-4V when compared to conventional milling processes. This paper compares the tool life during laser assisted milling, dry milling, milling with flood emulsion, milling with minimum quantity lubrication (MQL) and a hybrid laser + MQL process. It is found that conventional coolants offer superior tool life at the standard cutting speeds recommended by the tooling manufacturer, but at higher speeds the coolant deteriorates tool life due to thermal shock/fatigue. Despite this, laser assisted machining performed poorly and exacerbated thermally related tool wear mechanisms such as adhesion, diffusion and attrition. Hybrid laser + MQL substantially improved tool life by suppressing the thermal wear processes while also preventing thermal fatigue on the cutting tool.

Keywords: Non-ferrous metals; Laser processing; Cutting tools; Wear testing

1.0 Introduction

Interest in localised thermally assisted machining (TAM) technologies is growing because it is believed that the process can enhance the machinability and improve productivity when machining difficult-to-cut materials. The process involves locally heating the work-piece ahead of the cutter which reduces the flow stress of the work-piece material and enables easier chip formation. The heat source varies (plasma, electric arc, gas torch, laser etc.) but the important characteristic is that heating is confined to shallow depths in the region immediately ahead of the cutter [1]. Therefore, if controlled correctly, the process will not produce heat affected zones or permanent metallurgical damage to the subsurface material which eventually forms the final component.

It is well established that thermally assisted machining of titanium alloys results in a large reduction of the cutting force, between 15-50% depending on the particular alloy and extent of heating [2-13]. The problem is that diffusion related wear mechanisms often dominate when high speed machining titanium [14], which means that adding heat to the cutting zone can exacerbate these wear process, despite lower cutting forces [11]. However, the bulk of research to date has focused on turning with

[1] Corresponding author. email address: m.bermingham@uq.edu.au

less tool life evaluation performed during milling. The cutting dynamics in milling are very different, and consequently, diffusion associated wear processes may not dominate. Li *et al*. [15] investigated the wear mechanisms during high speed milling of Ti-6Al-4V and reported that chipping was the dominate failure mechanism of PCD tooling. It is understood that tool chipping may occur during the oscillating impact and force fluctuations that are encountered during milling. With this in mind, it seems logical that thermally assisted machining may help prevent this wearing process by reducing the magnitude of the force fluctuations. There is some limited evidence to support this expectation. Ginta and co-workers [3] found that induction preheating of Ti-6Al-4V reduced the wear rate when milling with PCD tooling, most notably through a reduction in edge chipping. Sun *et al*. [5] also observed a tendency for less chipping during laser assisted milling of Ti-6Al-4V using carbide tooling, however, also noted that tool chipping was exacerbated in some cases when the heating temperature exceeded 330°C.

The exact role that temperature plays on the tendency of a tool to chip is still not clear, after all, 'chipping' is merely a description of what happens to the tool and does not indicate the precise mechanism that causes the tool to chip. During milling with PVD and CVD coated carbide tools, Jawaid *et al*. [16] reported that all investigated tools eventually failed by chipping but in the lead up to this point, evidence of attrition, diffusion, plastic deformation and cracking was found. It was believed that the chipping resulted from cracking which in turn was attributed to cyclic stresses associated with the dynamic cutting process, however, adhesion, attrition and diffusive processes also played a role in removing tool sections [16]. Others have also indicated that these are dominant tool wear mechanisms in milling titanium alloys with carbide tooling [17-19]. With this in mind it is probable that additional heat supplied during TAM could encourage more damage by these thermally related processes as has been found to occur in turning tools [11].

Another implication of cyclic thermal loading on the tool is the occurrence of thermal cracking. This is often reported in the milling literature [16, 17, 19] and is expected to occur from fluctuations of heating and cooling associated with the tool engaging and disengaging a cut. Potentially, TAM may exacerbate this cracking process by increasing the amplitude of the heating and cooling cycles (assuming that the maximum tool temperature increases during TAM). On the other hand, the fact that TAM reduces the work-piece flow stress inherently reduces the mechanical stresses and impact on the tool which may prevent mechanical wearing processes. So in essence it remains unclear exactly how these competing factors may influence the overall tool life of TAM. Already it has been suggested that thermally assisted milling of Ti-6Al-4V improves the tool life or reduces tool chipping susceptibility [3, 5], however a thorough investigation into the wear process is still lacking.

The purpose of this study is to compare the tool life and determine the wear mechanisms of coated carbide tools rated for milling titanium alloys under conditions of dry cutting, flood emulsion, Minimum Quantity Lubrication (MQL) and Laser Assisted Machining (LAM) at three practical cutting speeds. Furthermore, this study also includes a hybrid LAM-MQL process, which to the authors' best knowledge, has never been reported before. The hybrid process was performed under speculation that the lubricating and cooling action of MQL may benefit LAM and facilitate improved tool performance. The comparison of these technologies will help researchers and engineers evaluate the fundamental performance and suitability of each technology for industrial application.

2.0 Experimental Method

2.1 Experimental Design

Linear face milling tests were performed to evaluate the tool performance over a number of different cutting scenarios. These scenarios included dry milling, milling with flood coolant, milling with MQL, LAM and a hybrid combination (HYB- LAM+MQL). All machining was performed on a 5-axis HASS VF3 CNC in climb milling. Three different cutting speeds were selected with the slowest speed determined by the tooling manufacturer's recommended parameters (V_c=69m/min). The speed was increased by 30% and 50% respectively for additional tests to investigate if there are benefits of LAM at higher than usual speeds. The feed rate (also known as chip load) was kept constant for all tests (0.14mm/tooth) but naturally as cutting/spindle speed changes the table speed also changes. Hence, as cutting speed was increased the table speed also proportionally increased. Effectively this resulted in three different testing combinations: V_c=69m/min with 192mm/min table speed; V_c=90m/min with table speed 251m/min; and V_c=104m/min with table speed 290mm/min. Details and justification for the selected machining parameters are given in Table 1.

Table 1. Details of machining experiments

	Test Details	Justification
Cutter body	Seco R217.69-1616.3-09-2A	Ø=16mm twin insert square shoulder slot milling cutter
Tool Type	Seco XOEX090304FR-E05 F40M	PVD-coated (Ti,Al)N - TiN carbide grade for fine to medium rough milling, recommended for machining superalloys
Operation	Face milling (in climb)	Slot cutter was used in facing operation with shallow depth
Cutting Speed	V_c = 69, 90, 104 m/min	The recommended cutting condition is V_c = 55m/min, f=0.08mm/tooth at full width of cut (a_e=16mm). Seco recommend increasing speed & feed by 1.3x at 37% radial cutter engagement to maintain chip thickness. A single insert cutter was used per test to conserve material. The table speed is proportionally linked with cutting speed & spindle speed because the feed rate is constant. *Table speed (m/min) = Spindle Speed (RPM)x feed rate (mm/tooth) x number of inserts* and *Spindle Speed (RPM) = cutting speed (m/min) ÷ tool circumference*
Feed Rate	f = 0.14mm/tooth	
Table Speed	192, 251, 290mm/min	
Spindle Speed	1373, 1790, 2069 RPM	
Depth and width of cut	a_p = 1mm, a_e = 6mm	The width of cut is limited by the laser spot size for LAM trials. The depth of cut was selected to conserve material.
Dry Cutting	No coolant or compressed air used	
MQL	Two external delivery nozzles. Approximately 4ml/hr vegetable oil with 50 P.S.I. compressed air	
Flood Coolant	Two external delivery nozzles. Approximately 27L/min water based emulsion (Fuchs Ecocool Durant 20A diluted to 5%)	
TAM	2kW Laserline diode laser, Spot size approximately 4-5mm, Power 50W and 150W (Section 2.4)	
Hybrid TAM + MQL	Laser assisted machining at Power 50W in conjunction with MQL. MQL was delivered by two external nozzles to the back side of the tool (away from the laser heating spot ahead of the tool).	

The tool wear during each test was successively monitored at periodic intervals according to the *ISO8688-1 Tool life testing in milling* standard. According to this testing standard, the end of tool life is reached once the average flank wear reaches 200μm. The time it takes to reach this wear criterion is considered as the tool life. In summary, the testing procedure involved interrupting the machining process at various intervals, removing the tool from the machine and examining the wear using an optical microscope. The wear on the tool was measured and recorded, and the insert returned to the cutter body for continued testing. The cutting forces were measured at the beginning of testing with unworn tools using a rotating force dynamometer (Kistler 9124B) at a sampling rate of 1KHz.

2.2 Work-piece Material

Face milling was performed on Ti-6Al-4V Extra Low Interstitial (ASTM Grade 23) in the β-annealed heat treated condition (bulk hardness 315HV±17). The material was supplied as a 100x100x40mm block so each length of cut was 100mm. Milling was performed in one axis direction (positive X direction in Figure 1). After machining the 100mm length, a subsequent pass was made by moving across the appropriate step over (negative Y direction Figure 1), which was 6mm in this experiment. A width of cut approximately 1mm larger than the laser heating spot size (for LAM experiments) was selected as a 'tolerance' to ensure that all material directly heated is removed by the cutter. Although this may create a small temperature gradient across the cutting zone width, the author's believe that this will have negligible effect on the tool life and cutting force in light of the cooling period discussed in section 2.4. While beyond the scope of this work, future work should investigate if thermal gradients associated with using smaller laser heated zones than cutting widths influences the tool life or other machinability aspects.

2.3 Thermally Assisted Machining

A 2kW diode laser was used to preheat material directly ahead of the cutter. A specially designed system (supplied by Expektra Pty Ltd) contained the laser optic head inside the CNC. Further details of laser-milling machine integration can be found elsewhere [20]. The system is capable of limited 3-axis laser manoeuvrability. The laser optic to tool distance was 200mm and focused a near circular spot size of approximately ø=4-5mm on the work-piece, positioned approximately 2mm ahead of the cutter. The laser angle was set at approximately 45° in the Z-X plane and 12° in the Z-Y plane for all LAM trials (refer to Figure 1).

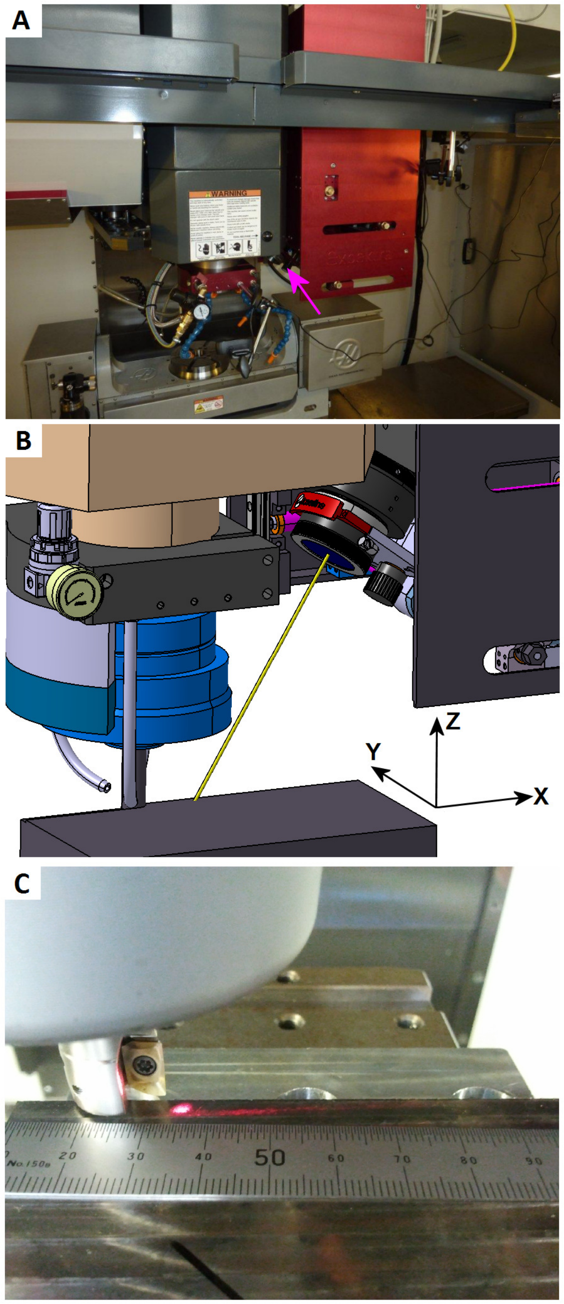


Figure 1. Pictures of laser installation onto milling machine. The laser optic is mounted onto the machine spindle (arrow in A) and the focus of the laser spot is positioned ahead of the milling cutter which travels in the positive X-direction relative to the work-piece for milling trials.

The effectiveness of TAM is reported to depend on the temperature that the material is heated to prior to cutting. Dandekar et al. [12] reported that the optimal temperature for thermally enhanced turning Ti-6Al-4V is approximately 250°C. In milling the same material, Sun *et al.* [5] reported that the ideal temperature ranged from 230°C-330°C depending on whether concurrent air cooling was delivered during TAM (with higher temperatures requiring concurrent air cooling). Others have reported that the 'optimum' temperature is much higher than this (above 500°C) [3, 7], whereas

Bermingham *et al*. [11] recently found that increased tool life (relative to dry cutting) in turning is achieved at low temperatures (150°C up to 250°C).

Based on this literature it was necessary to test two thermally assisted milling conditions, one at low temperature and one at high temperature. The goal of the low temperature testing was to heat the material to approximately 300°C and the goal of the high temperature testing was to heat the material to above 500°C. Since milling is an intermittent cutting process, the optimal temperature for machining Ti-6Al-4V is probably higher than in turning. To achieve the desired heating with the laser it was necessary to perform a thermal calibration test whereby the temperature achieved at various laser powers and table speeds was determined.

2.4 Calibration of laser power to achieve desired temperature in LAM

Calibration of the laser power to the predetermined temperature for each table speed was achieved by heating the work-piece material at particular laser power levels with thermocouples embedded in the work-piece to measure the temperature response. Four ø=0.81mm exposed junction k-type thermocouples were embedded in the work-piece at different depths from the surface (1, 2, 3 and 4mm). The thermocouples were horizontally offset by a distance of 10mm to eliminate any interaction in the natural heat conduction process that may otherwise occur if the thermocouples were stacked above and below each other in a vertical line from the surface (refer to Figure 2). Each thermocouple was inserted into a drilled hole (ø=0.9mm, 3mm deep) after being covered with a thermally conductive and electrically insulating paste to remove any air gap. The temperature was measured at different laser power levels starting at 50W (50W, 100W & 150W) for each table speed (see Table 1). To ensure steady state conditions the laser travelled a minimum distance of 60mm before passing over the first thermocouple and continued moving until at least 20mm past the final thermocouple. After each test the work-piece was allowed to cool to room temperature before initiating the subsequent test. Figure 2 shows the test piece used for calibrating the laser power.

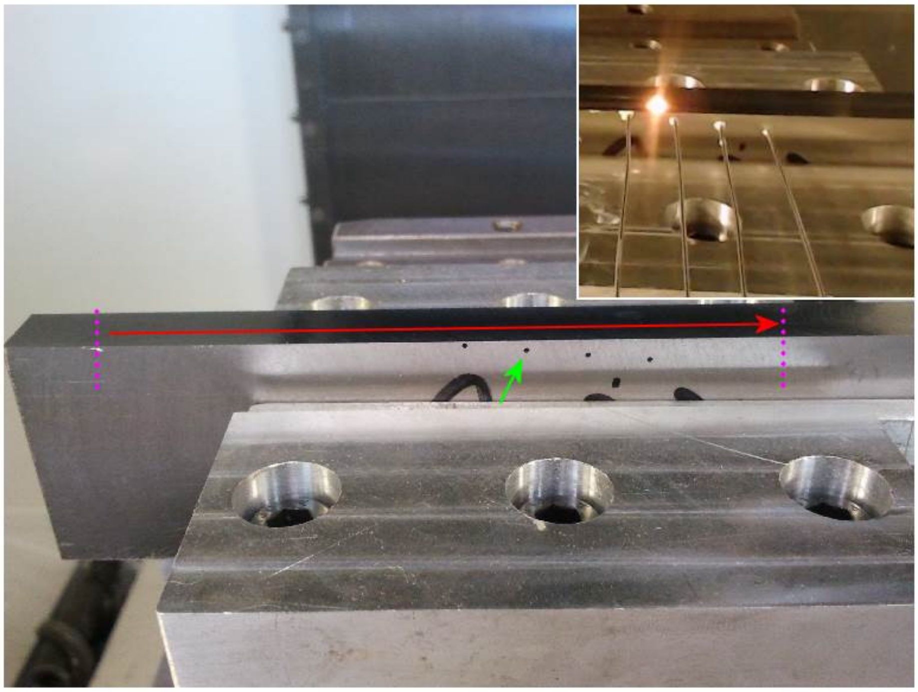


Figure 2. Thermocouples were inserted into predrilled holes (indicated by green arrow) horizontally offset from one another. The laser then traversed the surface of the work-piece from left to right (red arrow). The insert shows a snapshot of the calibration process occurring at 150W power.

During laser assisted machining there is a cooling period between when the laser passes and when the cutter reaches the heated area. The time it takes the cutter to reach the heated spot is 0.625s, 0.478s and 0.414s for 192, 251 and 290mm/min table speeds respectively, calculated for a 2mm laser spot-tool distance. Figure 3 shows an example of the cooling period as well as the temperatures encountered during one of the calibration tests. Although the peak temperature 1mm below the surface is close to 700°C, the material rapidly cools approximately 100°C by the time the cutter reaches this same location (0.414s later).

Figure 4 shows the temperature measured at 1mm depth from the surface for both the temperature as the laser passes and the temperature at the cutting tool after the cooling period has been considered. The calibration process revealed that the temperature in the work-piece is more influenced by the laser power rather than the table speed. It is found that a laser power of 50W results in a temperature at the cutter of approximately 275-320°C for each table speed which is close to the desired temperature and was therefore selected for the laser assisted milling trials. Additionally, setting the laser power to 150W resulted in temperatures above 500°C and hence, this power level was also used for second set of laser assisted milling trials.

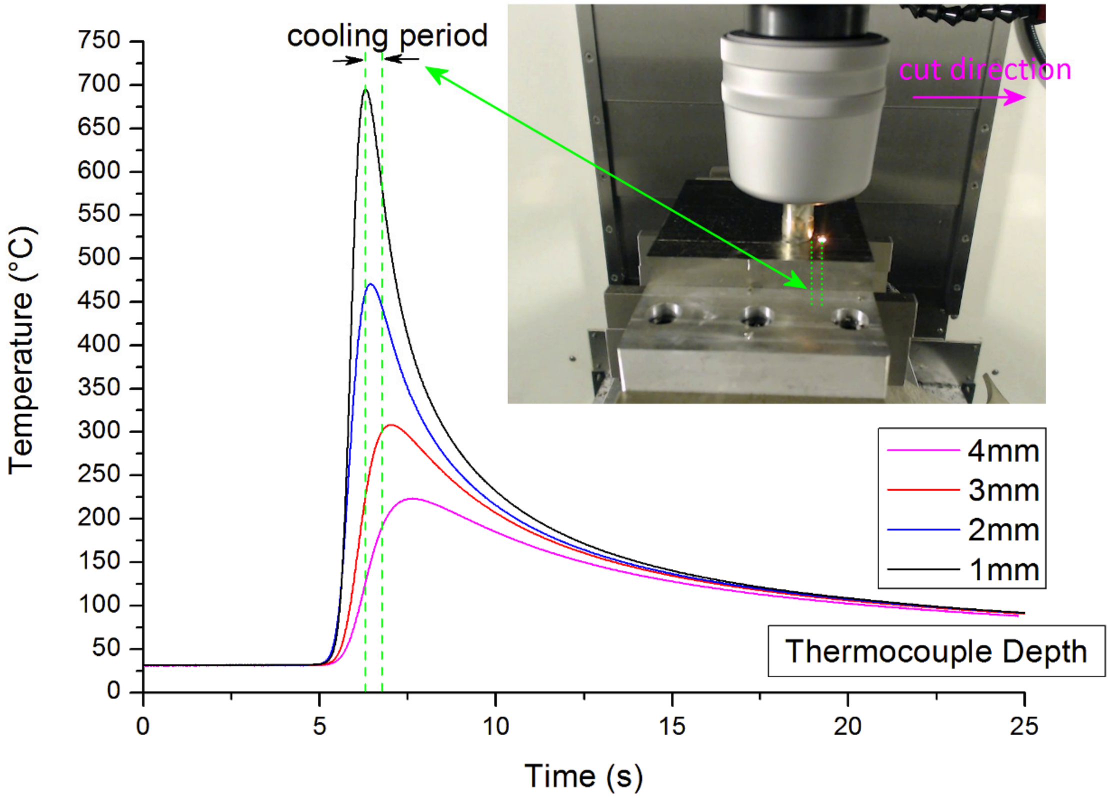


Figure 3. Temperature measurements obtained for laser heating condition P=150W f=290mm/min. This example shows the peak heating temperature corresponding to the moment that the laser passes, as well as cooling period between the laser heated spot and the cutter. In this example, the cooling period is 0.414s because the table speed is 290mm/min and the tool to laser heated spot distance is 2mm. Note that the 10mm horizontal distance between thermocouples has been considered and offset in this plot.

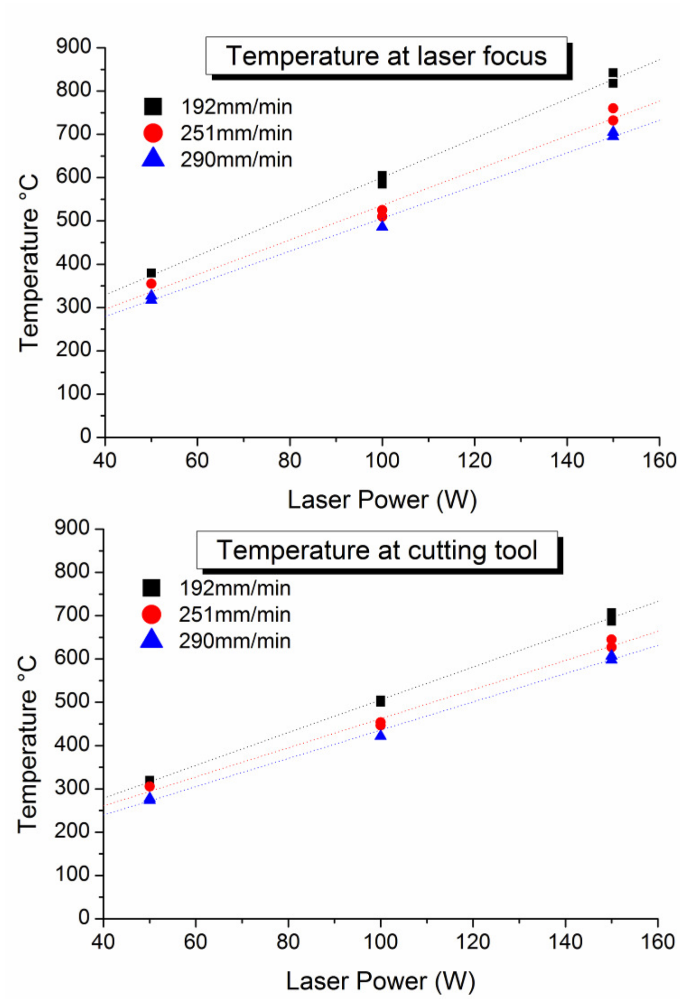


Figure 4. Results of laser power calibration process at 1mm depth below the surface. The temperature at the laser focus as well as the temperature at the cutting tool (after a cooling period has elapsed) is given for each feed speed. Multiple tests were performed to ensure repeatability. It is important to reiterate that the 'temperature at cutting tool' is the work-piece preheat temperature and is not the absolute cutting temperature which results from the combination of work-piece preheating and heat generated during cutting.

3.0 Results and Discussion

3.1 Tool Wear

The rate of tool wear during the milling trials was strongly influenced by the cutting speed and individual test circumstances. Relative to conventional machining processes, laser assisted milling was found to increase tool life at some cutting speeds and decrease tool life at other speeds. Figure 5 shows the average flank wear for each test condition. According to *ISO8688-1 Tool life testing in milling*, the end of life criterion is met when the average flank wear reaches 200µm. This criterion is included as the dashed line in Figure 5. Consequently, the tool life is considered as the time it takes to reach this criterion during testing. This criterion proved very useful because when testing continued beyond this point rapid tool deterioration would occur, occasionally resulting in catastrophic tool breakage (the entire cutting tip would fracture off the tool). A summary of the tool life for each test is given in Table 2.

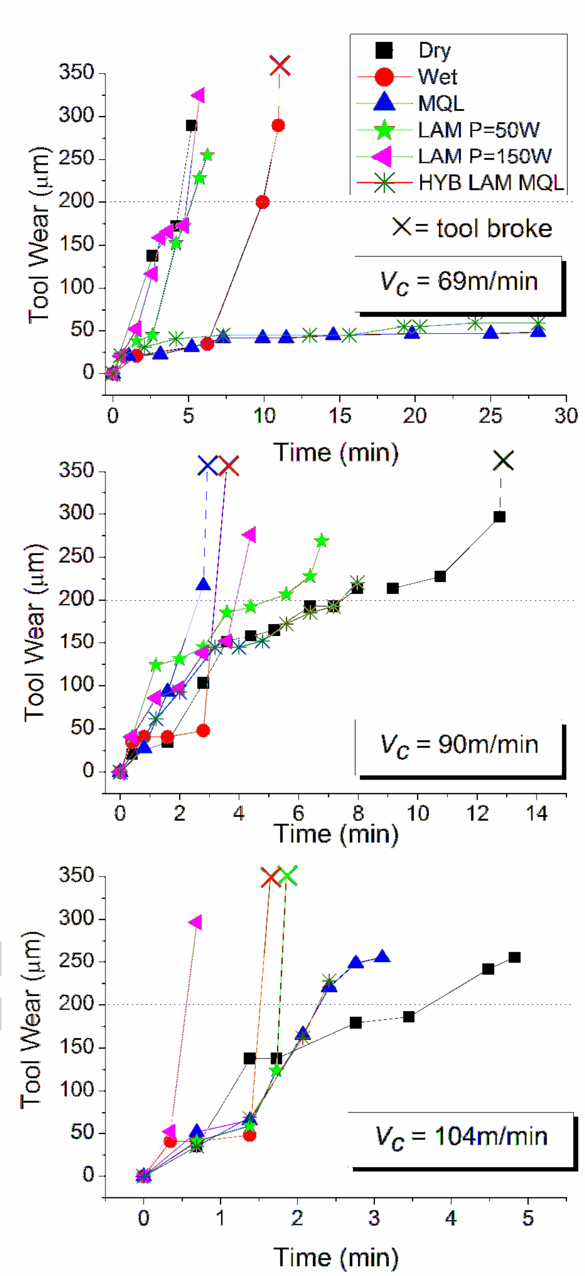


Figure 5. Average flank wear for each test condition. The dashed line represents the 200µm end of life wear criterion (end of life occurs when the tool wear reaches this point). ***Note that the tool life tests at V_c=69m/min MQL and HYBRID MQL-LAM were stopped after almost 30minutes of machining with negligible tool wear, so the actual tool life will be greater than this figure. The tests were stopped to conserve material.***

Table 2. Summary of tool life for each tested condition

Condition	69m/min	90m/min	104m/min
	(*minute:second*)		
Dry	4:25	7:25	3:45
Wet	9:55	3:10	1:30
MQL	>28min	2:35	2:15
LAM P=50W	5:10	5:05	1:45
LAM P=150W	5:00	3:55	0:35
Hybrid LAM-MQL	>28min	7:25	2:15

The effectiveness of laser assisted milling in terms of extending tool life compared to conventional machining processes depends strongly on the cutting conditions, including the cutting speed and the laser power. At low speed (V_c = 69m/min), LAM offered slightly better tool life than machining dry (up to 17%), but far shorter tool life compared to cutting with conventional coolants or hybrid LAM-MQL. At this speed the two different heating temperatures in LAM had negligible effect on the overall tool life. It is worthwhile noting that the MQL and Hybrid tests at V_c = 69m/min did not reach the tool wear end of life criterion, so the actual tool life will be greater than shown in Table 2. The rate of tool wear during MQL and hybrid testing was very low, and consequently, testing was ceased after 28 minutes of machining to conserve work-piece material.

As the cutting speed was increased the relative effectiveness of the coolants decreased and instead laser assisted machining occasionally provided longer tool life. The best example is at V_c = 90m/min where LAM at both P=50W and P=150W increased tool life relative to either conventional MQL or flood cooling. However, dry cutting and hybrid LAM-MQL improved the tool life even further. At the highest speed tested (V_c = 104m/min), the benefits of LAM reduced significantly with the shortest tool life occurring at LAM P=150W. Reducing the laser power to 50W did improve tool life more than conventional flood cooling, but MQL, dry and hybrid machining all provided longer tool life. Of important note is that the flood cooling condition at both medium and high speed milling tended to cause very sudden catastrophic tool failure, whereas all other conditions resulted in progressive/gradual wear processes.

The somewhat bizarre results do indicate that the temperature of the preheated work-piece is a significant factor influencing tool longevity during LAM. At low speed (V_c =69m/min) the role of the work-piece temperature on tool life was negligible (e.g. only small differences between dry, LAM P=50W & 150W). However, rapid tool degradation occurred with increasing cutting speed and work-piece preheating temperature. For example, at V_c = 104 m/min, LAM at high power (P=150W) produced the shortest tool life of any test. This means that an 'over heating' effect is clearly influencing the tool performance in milling. A very similar trend was recently observed in thermally assisted turning Ti-6Al-4V where work-piece preheating to 150°C offered a marginal 7% improvement in tool life compared to room temperature cutting, but overheating the work-piece to 350°C considerably shortened the tool life relative to room temperature cutting [11]. In the case of turning it was found that the overheating effect promoted increased diffusion and adhesion related wear processes.

3.2 Tool failure mode

In almost all testing conditions in the present study the tools eventually failed by depth of cut notch formation. The exceptions to this were the tools that failed catastrophically under flood cooling conditions at high speeds (V_c = 90 & 104 m/min flood) and the MQL and Hybrid LAM-MQL at V_c = 69m/min which are not considered to have 'failed' as the wear criterion was not reached during testing. Figure 6 shows the typical evolution of tool wear observed during testing where notching dominated and the wear criterion was reached. It was found that rapid tool degradation occurred upon the formation of a notch, so the experimental test conditions that most effectively suppressed notch formation provided the longest tool life. The best example of notch suppression resulting in long tool life was the case of using MQL and Hybrid LAM-MQL at V_c =69m/min. Up until testing was stopped, these particular combinations resisted notch formation, and consequently, the geometry of the tool remained in its original condition and an extremely long tool life ensured.

The exact factors and mechanisms that are responsible for depth of cut notch wear are still widely debated in the literature. Depth of cut notching is a major tool failure process encountered while machining nickel-based alloys, and consequently, significant research has been conducted to understand this phenomenon. Various factors including rubbing against heavily work-hardened material (such as burrs) [21-24], lateral chip flow [24], high stress gradients [25], chip adhesion and pull out [14, 21, 23], fatigue loading [14, 21, 22], attrition and diffusion processes [26, 27] are all believed to be responsible for notch formation. Considering the vast evidence available on the subject, it is almost certain that multiple mechanisms influence notch formation, depending on cutting tool and work-piece material combinations [28]. The precise mechanism(s) responsible for notch formation in carbide tooling during thermally assisted milling Ti-6Al-4V requires further investigation.

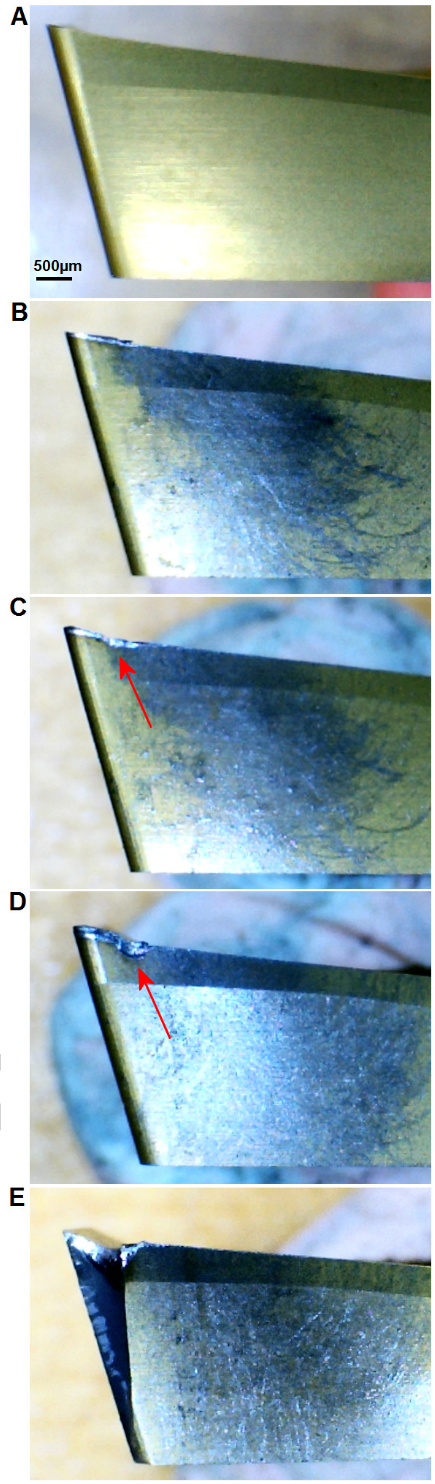


Figure 6. Evolution of tool wear typical of most test conditions. The tool initially wears evenly along the flank face but eventually uneven wear occurs away from the tool tip in the form of notch initiation (arrow in B). The depth of the notch grows quickly until it is sufficiently large to cause catastrophic tool failure. ***Image details: machining V_c = 90m/min with MQL after (B) 48 seconds, (C) 96 seconds, (D) 167 seconds (E) 203 seconds. The unworn tool is shown in (A).***

3.3 Observed wear mechanisms

Close examination of the worn tools in the SEM revealed evidence of work-piece adhesion to the tool, severe tool erosion, possible tool plasticity and cleaved sections of tool where layers of adhered work-piece material have been plucked away. Figure 8 shows how these factors appear to assist the formation of depth of cut notching typically observed on all tools except for the high speed flood coolant conditions. Firstly, localised instabilities in the form of depressions or pits on the cutting edge appear. It is unclear how these instabilities initially form as it may be tool plasticity or abrasive processes that destroy the sharp cutting edge of the tool. Kasim *et al*. [29] report that pitting, the precursor to notch wear, occurs by the removal of attached BUE at the depth of cut line during intermittent cutting and occurs in this region because maximum stress occurs here[2]. However, in the present study 90° square shoulder inserts were used with the DOC far greater than the nose radius of the tool (1.0mm vs 0.4mm). Therefore, the stress should be uniform along the perpendicular edge because the chip load is constant. From Figure 8a it appears that the damage could be caused by plastic deformation, which could occur from rubbing against work-hardened material (burrs) that formed during milling. Significant burr formation did occur during milling (Figure 7) and it is possible that rubbing against this work-hardened material damages the tool, as suggested by others [21-24].

Once the cutting edge has been damaged, localised notch formation quickly occurs and any exposed tool asperities are rapidly removed to grow the notch. A crater is observed to form behind the cutting edge. The formation mechanism of this crater is also unclear, but it is very likely that the mechanism is exacerbated at higher temperatures because large craters were observed to grow rapidly during high power LAM and/or during high speed machining where elevated temperatures are encountered. The final process that accelerates notch growth is work-piece adhesion and the subsequent removal of bonded tool and work-piece layers. The same process occurs on turning tools after strong chemical bonding between the tool and adhered titanium layers causes an entire section of material to be ripped out by flowing chip material [11]. The presence of this wear process is apparent in Figure 9 which shows a large crack that has formed on the cutting edge after the adhered layer has bonded to the tool. The crack clearly propagates along the tool face which probably would have resulted in the entire top surface of the tool being ripped out if machining had continued further and the top surface became caught on, or bonded to, the flowing chip. It is evident under all tested conditions that the proprietary PVD coating on the tools provided no barrier to adhered titanium as all tools developed regions covered in the titanium work-piece. Similar observations have been made by Hou and co-workers when end milling Ti-6Al-4V [30]. While high speed milling Inconel 718, Sharman *et al*. [31] also demonstrated that tool coatings offer little resistance to adhesive wear processes. Evidence of deep wear grooves expected from abrasive processes is also present on the tools. From Figure 8, it appears that the deep abrasions originate from exposed tool sections that have previously been ripped out by adhesion. It is probable that the deep grooves are caused by abrasive tool particles that have been ripped out by chip materials

[2] During milling with round inserts, maximum chip load occurs at the depth of cut because the chip thickness is greatest here. Kasim *et al*. used round inserts in their study.

because the wear grooves originate from locations where adhered layers have been removed (Figure 8D).

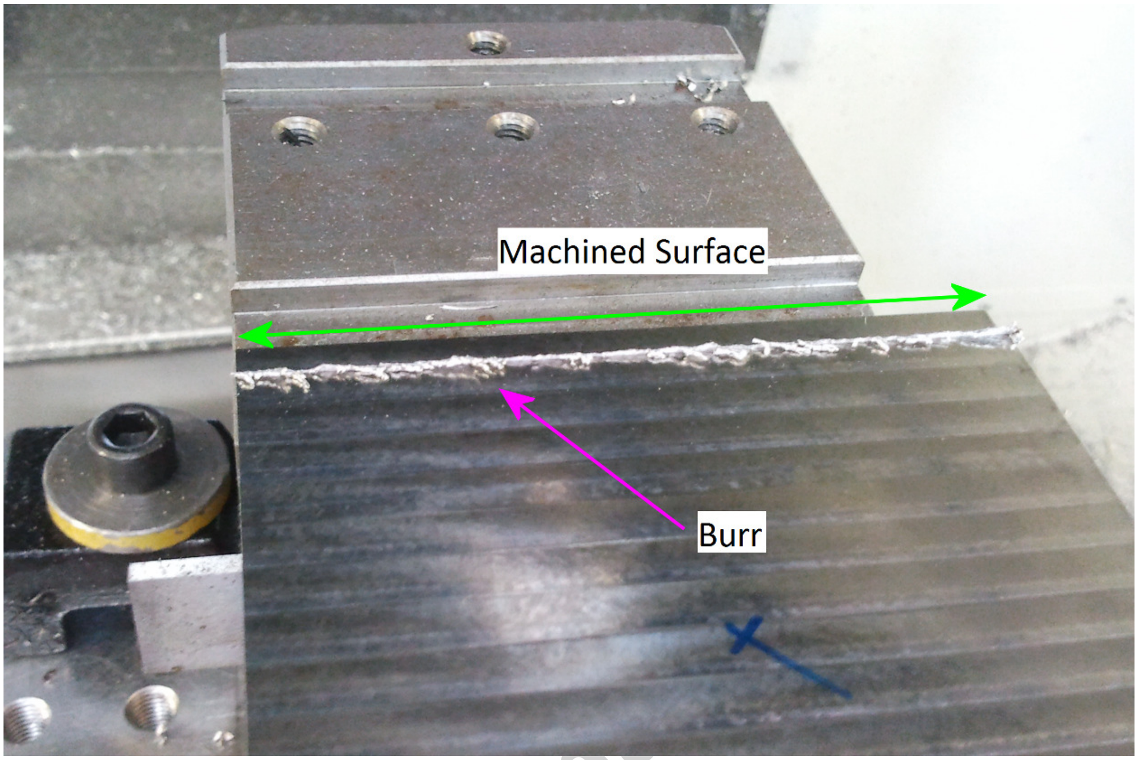


Figure 7. Example showing burr formed during milling test.

The effect of increasing the work-piece preheating temperature during LAM has a distinct effect on the notch wear characteristics. Figure 10 shows that notch wear still dominates during LAM in much the same way as described above, however, far more work-piece material/chip deposits on the tool compared to the tools shown in Figure 8. The plasticity of the deposited work-piece material is observed to increase at higher laser power, and effectively 'flows' along the flank and rake face of the tool. This material bonds strongly with the tool, and consequently, adhesive wear causes the removal of large sections of tool material.

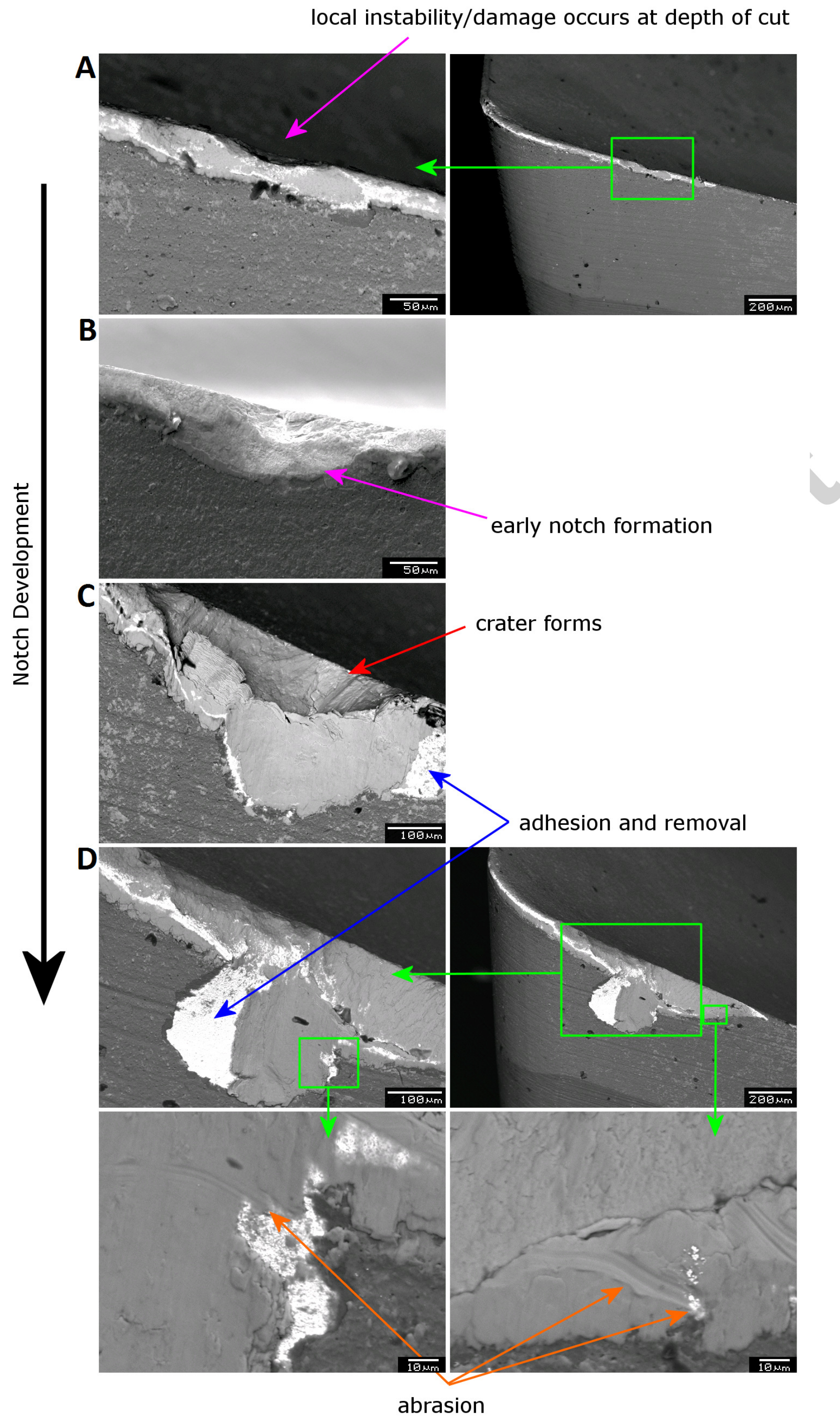


Figure 8. Observed process of notch formation on the cutting tools. This process typically occurred during test conditions of dry, MQL and HYB LAM-MQL. Images are a selection of various tools at different stages of notch formation. Since different testing conditions were used, the times to reach a given level of wear vary. (A) V_c = 69m/min MQL (28min), (B) V_c = 69m/min HYB MQL-LAM (28min), (C) V_c = 90m/min HYB MQL-LAM (8min), (D) V_c = 104m/min HYB MQL-LAM (2.5min). Note that the unlabelled images are different views (magnifications) of the listed images above.

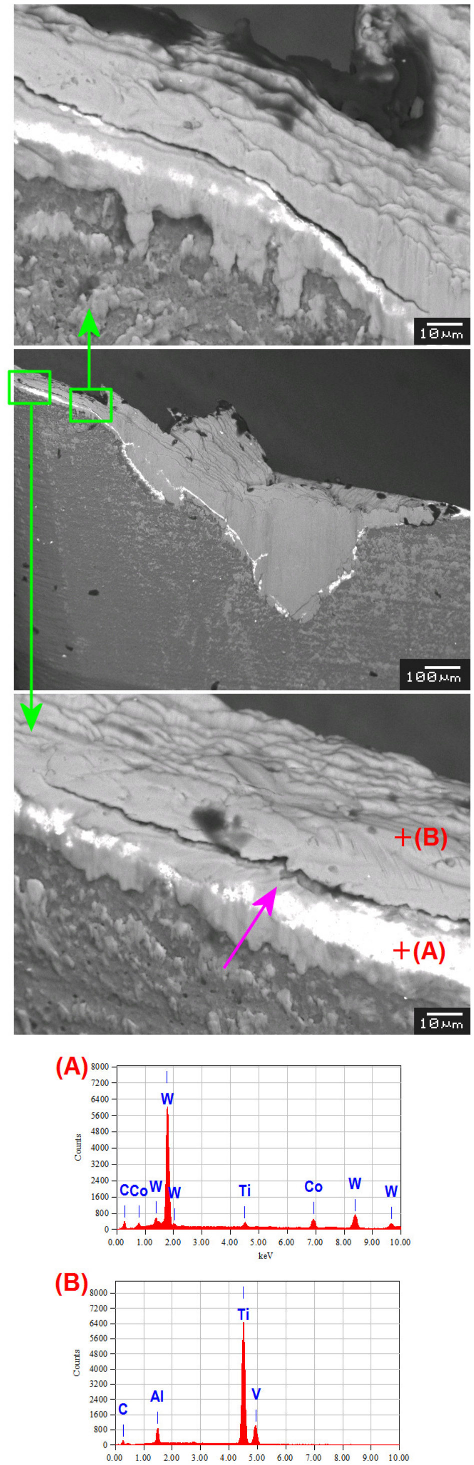


Figure 9. Evidence of adhesive wear: a layer of built up edge that has bonded to the tool is being ripped out and a large crack propagates the tool face (indicated by arrow). It is likely that if machining progressed further the entire top section of tool and BUE would have been removed. The EDS spectrum at two locations (A and B) is shown proving that the BUE is the titanium work-piece (Ti-6Al-4V) and the white layer is exposed tool (WC-Co) (*V_c = 69m/min dry condition*).

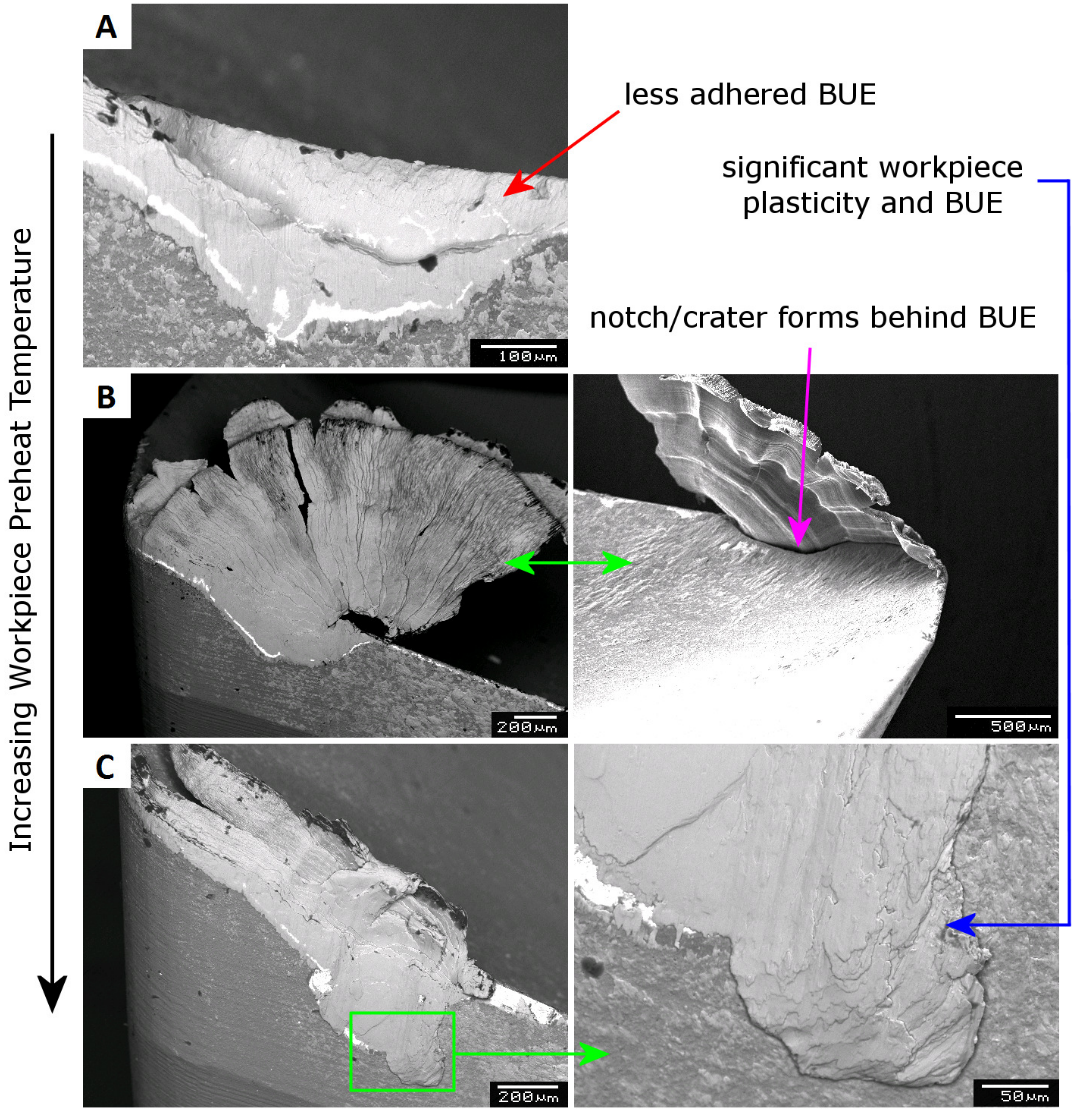


Figure 10. Effect of work-piece preheating temperature on notch formation. (A) V_c=90m/min LAM P=50W [preheating temperature approx. 300°C], (B) V_c=90m/min LAM P=150W [preheating temperature approx. 650°C], (C) V_c=69m/min LAM P=150W [preheating temperature approx. 700°C]. ***Refer to Figure 4 for the laser power and table speed temperature calibration.***

Unlike all other tools investigated in this study, tools subjected to flood coolant testing did not develop large notches prior to catastrophic failure. Instead the wear progression was more uniform along the flank until the entire tip of the tool would unpredictably break off. Figure 11 shows images of the failed tools, and although it doesn't show the tool condition immediately prior to failure, it is clear that deep notches (i.e >200µm) are not present at the depth of cut. What is also apparent is that prior to failure the thickness of the wear land on the flank face at the depth of cut reduced as the cutting speed increased. This is also reflected in the wear measurements presented in Figure 5 for V_c = 90 and 104m/min, where sudden failure occurred after an even wear land of approximately

50µm. This indicates that the susceptibility of the tool to sudden catastrophic failure increases with cutting speed, which is very likely to be related to thermal shock or fatigue.

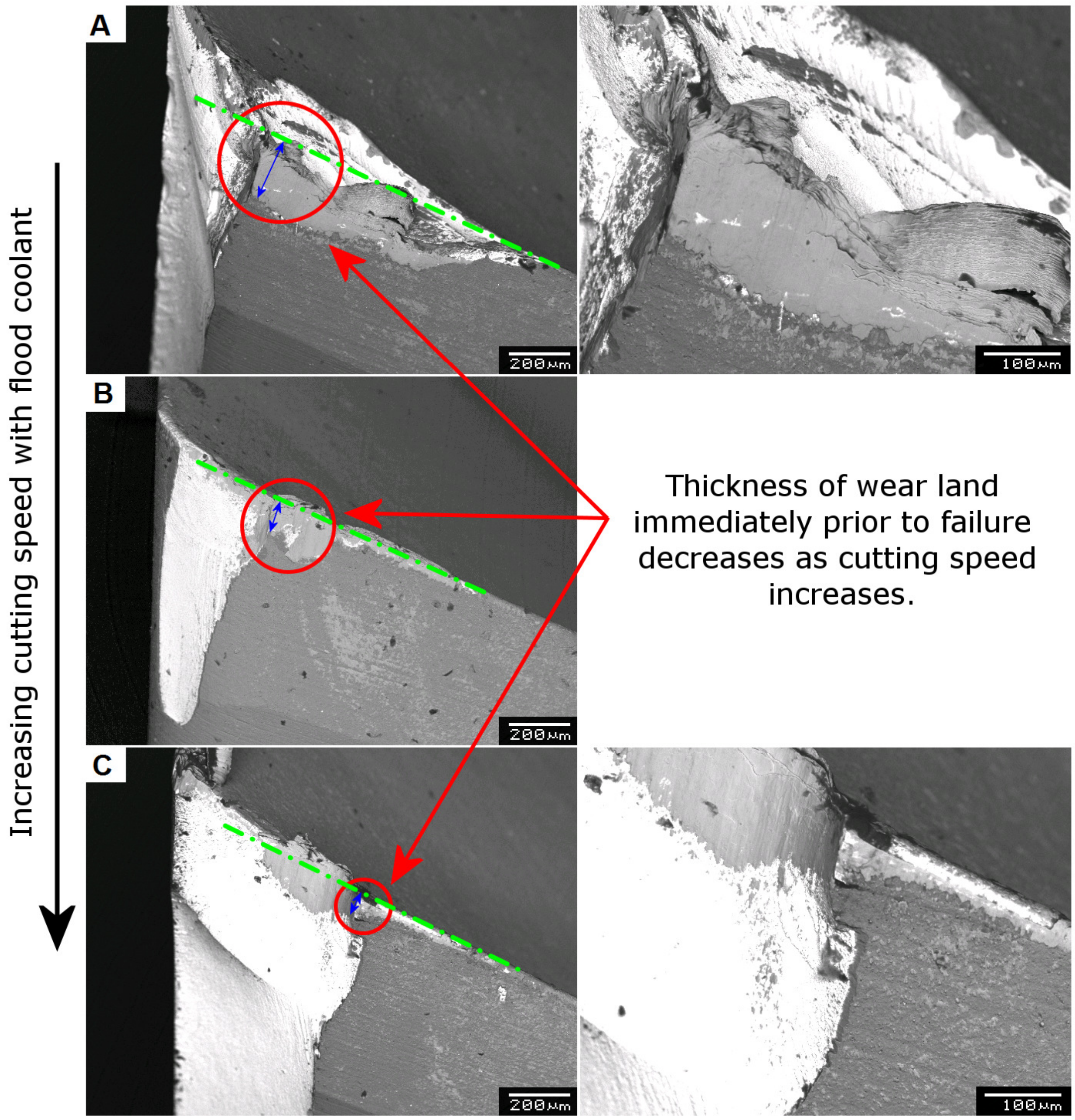


Figure 11. Effect of flood coolant on tool failure mode. As the cutting speed increases tool failure happens suddenly after very little wear has occurred along the flank face. The wear land adjacent to the fracture are indicated by the blue arrow and measures approximately 200µm in (A), 104µm in (B) and 66µm in (C). Although no cracks are visible, it is highly probable that thermal shock or thermal fatigue is contributing to tool failure. (A) V_c = 69m/min, (B) V_c = 90m/min, (C) V_c =104m/min.

Thermal shock is known to play an important role in influencing the life of milling tools. Due to the nature of intermittent cutting the tools are exposed to cyclic periods of heating and cooling [24]. The tensile stresses formed on the tool surface during the cooling period can generate small thermal cracks which eventually grow into larger comb cracks that cause tool fracture [16, 32-35]. Consequently, it is often reported that the use of coolants can accelerate this process and reduce tool life, especially at high speed [19, 33, 34, 36-38].

3.4 Other factors influencing tool life

Considering that overheating during high speed LAM can deteriorate tool life and that overcooling the tool can cause thermal shock, it is a reasonable assumption that the maximum tool life during high speed milling will occur when the temperature in the cutting zone and thermal fluctuations experienced 'out of cut' are minimised. Of all tested conditions this occurred during both dry machining and mostly during hybrid LAM-MQL where the added cooling and lubricating action of MQL delayed thermal wear processes compared to standard LAM. However, a peculiar observation is that dry machining at V_c =90m/min provided far longer tool life than dry machining at the other tested speeds. It is expected that this condition will perform better than machining at higher speeds due to less heat being generated, however, it is anomalous that machining at lower speed (V_c =69m/min) produced shorter tool life considering that the temperatures generated at this speed are almost certainly lower than at V_c =90m/min. Close examination of the cutting force measured during testing indicates that low frequency chatter may in part be responsible for shorter tool life. At both V_c =69 & 104m/min it is clear that the cutting force amplitude oscillates between 2-4Hz, whereas the cutting forces appear much more stable at V_c =90m/min. Although it is clear that traditional wear processes such as adhesion are ultimately responsible for tool failure, the vibrations and inherent stress fluctuations on the tool may play an important role in initiating damage at the depth of cut which eventually formed the notches. Kayhan and Budak [39] have shown that chatter has a strong influence on tool life when milling Ti-6Al-4V where severe chatter reduced tool life by up to 70% compared to stable cutting. Further investigation is required to understand the effects of low frequency chatter on tool life during titanium milling.

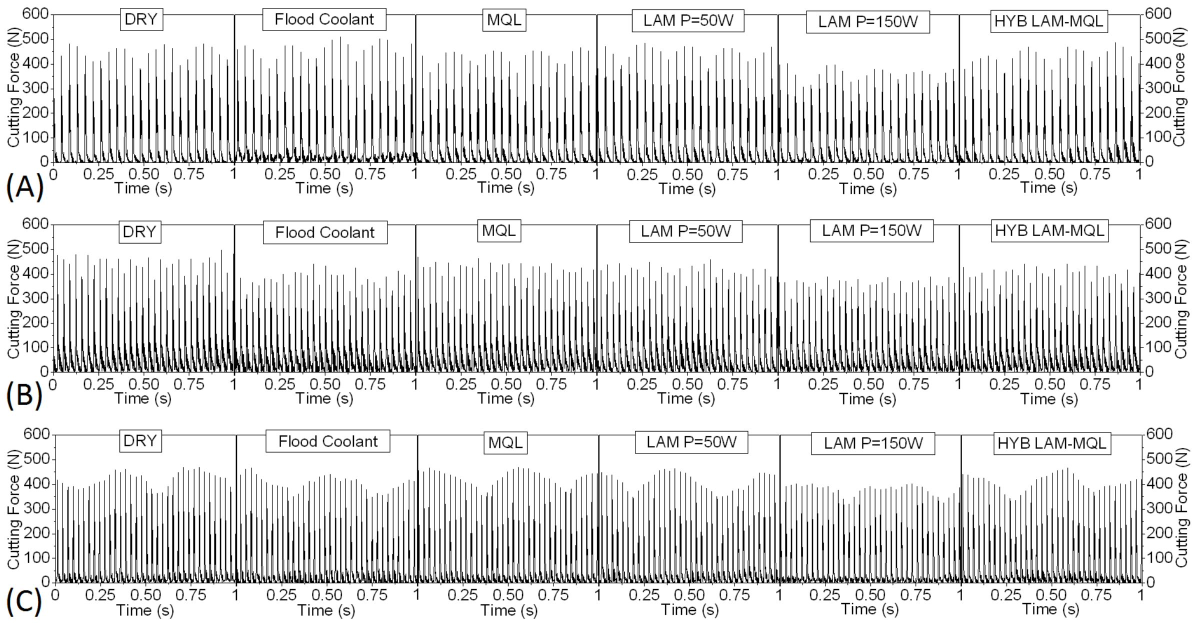


Figure 12. Cutting forces measured for each test condition over a 1s interval. (A) V_c=69m/min, (B) V_c=90m/min and (C) V_c=104m/min. The forces were measured within the first 10s of milling with a new tool, so the effects of tool wear/notching can be neglected. A low frequency fluctuation in the cutting forces is apparent in at V_c=69m/min and V_c=104m/min, whereas the forces are more stable at V_c=90m/min.

3.5 Heat Affected Zone during LAM

A major practical concern regarding LAM is the heat affected zone and the permanent metallurgical alteration of the machined titanium component. Ti-6Al-4V is a dual phase α/β alloy and it is important that the phase distribution and grain morphology of the billet material does not change during LAM, as this may affect the mechanical properties of the alloy. The dual phase microstructure of β-annealed Ti-6Al-4V is similar to the cast microstructure and is formed when the α-phase nucleates and grows within existing β-grains, which occurs when the material is cooled from the β-phase field into the α/β-phase field [40]. The temperature at which this transformation occurs is known as the β-transus temperature, and corresponds to approximately 1000°C in Ti-6Al-4V [41, 42]. Consequently, for microstructural change to occur in this alloy the temperature must exceed the β-transus temperature. In this event it is expected that α' martensite or acicular-α (needle like α) will form due to rapid cooling as the heat from the laser quickly dissipates into the bulk alloy.

Considering that the temperature during LAM never reached the critical temperature required to change the morphology of the existing α-phase it is expected that no HAZ will be present in any of the machined components (see Figure 4). To confirm this, the microstructure below the machined surface was examined. Figure 13 presents the microstructure after LAM for the condition most likely to form a HAZ (highest temperatures experienced) and compares it to conventional dry machining and the bulk material microstructures. All three microstructures contain identical features and no change to the α-phase morphology occurs after LAM. Since microhardness is known to be a better indicator of metallurgical damage than microstructural observation [43], a series of microhardness tests were also performed and confirmed that no statistical difference exists between LAM and conventional machining (Figure 14).

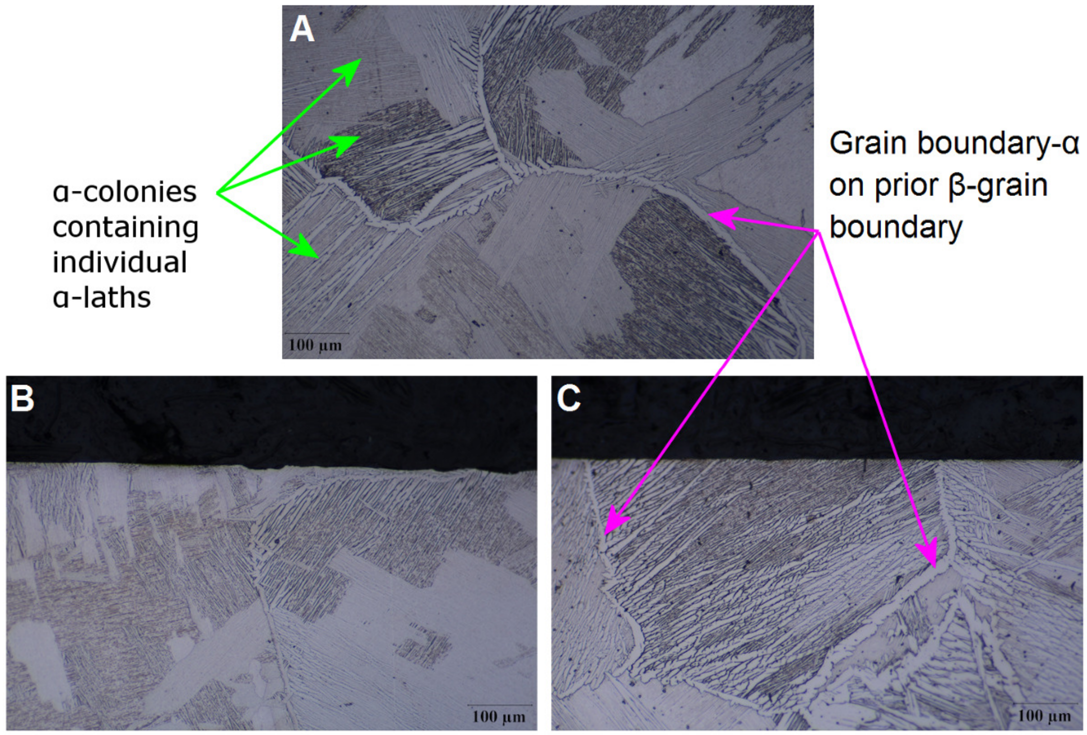


Figure 13. Microstructures of Ti-6Al-4V E.L.I. billet in the as-supplied condition (A) and microstructures at the machined surface after (B) room temperature dry machining at V_c=69m/min and (C) LAM P=150W at V_c=69m/min. Identical features exist in all microstructures suggesting that no heat affected zone exists after LAM.

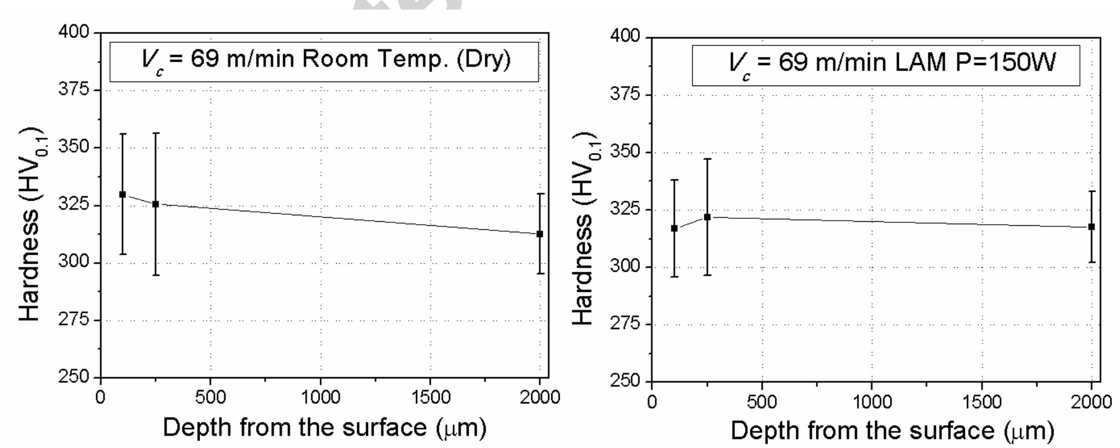


Figure 14. Microhardness results measured from the machined surface. Each data point represents the average of 20 hardness measurements. There is no statistical difference between the hardness of conventional room temperature machining and LAM, supporting visual observations that no heat affected zone exists after LAM.

4.0 Conclusions

This paper investigates and compares the tool life and wear processes while milling Ti-6Al-4V under dry conditions, milling with flood coolant, milling with minimum quantity lubrication (MQL), laser assisted milling and hybrid laser + MQL milling at three cutting speeds. The key findings of the work are:

- All tools, except those cooled with flood emulsion and under certain MQL conditions, failed by depth of cut notch formation. In general, longer tool life was achieved by delaying the onset of notching. The wear process on the tool flank was initially even, but as soon as a notch formed, localised wear accelerated in this region and ultimately tool fracture occurred. Under certain MQL conditions, notch formation was suppressed and these tools did not fail during testing.
- Notching formed from localised damage to the tool at the depth of cut. In this region adhesion and attrition processes were found to accelerate notch wear. Diffusion related wear processes and abrasion are also likely to contribute to tool failure. All wear processes are exacerbated at high temperatures.
- In some circumstances the use of flood coolant caused thermal shock and catastrophic tool failure; this tendency increased with cutting speed.
- LAM generally had a detrimental effect on tool life, particularly at high speed and high preheating temperature. However, at low cutting speeds, LAM offered a marginal improvement in tool life compared to room temperature dry machining.
- The hybrid process that combined MQL with laser assisted milling increased tool life by over 5 times compared to conventional laser assisted milling. Longer tool life occurred because the cooling action of MQL slowed the rate of thermally related wear processes without causing thermal shock.

Acknowledgements

The authors would like to acknowledge the support of the Queensland Centre for Advanced Materials Processing and Manufacturing (AMPAM) and Airbus for their interest and financial support to the collaborative work.

References

[1] S. Sun, M. Brandt, M.S. Dargusch, Thermally enhanced machining of hard-to-machine materials - A review, International Journal of Machine Tools & Manufacture, 50 (2010) 663-680.
[2] L.N. López De Lacalle, J.A. Sanchez, A. Lamikiz, A. Celaya, Plasma assisted milling of heat-resistant superalloys, Journal of Manufacturing Science and Engineering, Transactions of the ASME, 126 (2004) 274-285.
[3] T.L. Ginta, A.K.M. Nurul Amin, M.A. Lajis, A.N.M. Karim, H.C.D.M. Radzi, Improved tool life in end milling Ti-6Al-4V through workpiece preheating, European Journal of Scientific Research, 27 (2009) 384-391.
[4] S. Sun, J. Harris, M. Brandt, Parametric investigation of laser-assisted machining of commercially pure titanium, Advanced Engineering Materials, 10 (2008) 565-572.

[5] S. Sun, M. Brandt, J.E. Barnes, M.S. Dargusch, Experimental investigation of cutting forces and tool wear during laser-assisted milling of Ti-6Al-4V alloy, Proceedings of the Institution of Mechanical Engineers - Part B: Journal of Engineering Manufacture, 225 (2011) 1512-1527.
[6] S. Rajagopal, D.J. Plankenhorn, V.L. Hill, Machining aerospace alloys with the aid of a 15kW laser, Journal of Applied Metalworking, 2 (1982) 170-184.
[7] A.K.M. Nurul Amin, N.V. Talantov, Influence of the instability of chip formation and preheating of work on tool life in machining high temperature resistant steel and titanium alloys, Mechanical Engineering Research Bulletin, 9 (1986) 52-62.
[8] G. Germain, F. Morel, J.-L. Lebrun, A. Morel, Machinability and surface integrity for a bearing steel and a titanium alloy in laser assisted machining (opptimisation on LAM on two materials), Lasers in Engineering, 17 (2007) 329-344.
[9] R.A. Rahman Rashid, S. Sun, G. Wang, M.S. Dargusch, An investigation of cutting forces and cutting temperatures during laser-assisted machining of the Ti-6Cr-5Mo-5V-4Al beta titanium alloy, International Journal of Machine Tools and Manufacture, 63 (2012) 58-69.
[10] R.A. Rahman Rashid, S. Sun, G. Wang, M.S. Dargusch, The effect of Laser Power on the machinability of the Ti-6Cr-5Mo-5V-4Al Beta Titanium alloy during laser assisted machining, International Journal of Machine Tools and Manufacture, 63 (2012) 41-43.
[11] M.J. Bermingham, S. Palanisamy, M.S. Dargusch, Understanding the tool wear mechanism during thermally assisted machining Ti-6Al-4V, International Journal of Machine Tools and Manufacture, 62 (2012) 76-87.
[12] C.R. Dandekar, Y.C. Shin, J. Barnes, Machinability improvement of titanium alloy (Ti-6Al-4V) via LAM and hybrid machining, International Journal of Machine Tools and Manufacture, 50 (2010) 174-182.
[13] R.A. Rahman Rashid, M.J. Bermingham, S. Sun, G. Wang, M.S. Dargusch, The response of the high strength Ti-10V-2Fe-3Al beta titanium alloy to laser assisted cutting, Precision Engineering, 37 (2013) 461-472.
[14] P.A. Dearnley, A.N. Grearson, Evaluation of principal wear mechanisms of cemented carbides and ceramics used for machining titanium alloy IMI 318 Materials Science and Technology, 2 (1986) 47-58.
[15] A. Li, J. Zhao, D. Wang, Y. Dong, Failure mechanisms of a PCD tool in high-speed face milling of Ti-6Al-4V alloy, International Journal of Advanced Manufacturing Technology, 67 (2013) 1959-1966.
[16] A. Jawaid, S. Sharif, S. Koksal, Evaluation of wear mechanisms of coated carbide tools when face milling titanium alloy, Journal of Materials Processing Technology, 99 (2000) 266-274.
[17] A. Li, J. Zhao, H. Luo, Z. Pei, Z. Wang, Progressive tool failure in high-speed dry milling of Ti-6Al-4V with coated carbide tools, International Journal of Advanced Manufacturing Technology, 58 (2012) 465-478.
[18] Q. Niu, M. Chen, W. Ming, Q. An, Evaluation of the performance of coated carbide tools in face milling TC6 alloy under dry condition, International Journal of Advanced Manufacturing Technology, 64 (2012) 623-631.
[19] S.M. Yuan, L.T. Yan, W.D. Liu, Q. Liu, Effects of cooling air temperature on cryogenic machining of Ti-6Al-4V alloy, Journal of Materials Processing Technology, 211 (2011) 356-362.
[20] M.J. Bermingham, P. Schaffarzyk, S. Palanisamy, M.S. Dargusch, Laser-assisted milling strategies with different cutting tool paths, International Journal of Advanced Manufacturing Technology, IN PRESS DOI 10.1007/s00170-014-6093-z (2014).
[21] V. Bushlya, J. Zhou, P. Avdovic, J.-E. Stahl, Wear mechanisms of silicon carbide-whisker-reinforced alumina (Al_20_3-SiC_w) cutting tools when high-speed machining aged Alloy 718, International Journal of Advanced Manufacturing Technology, 68 (2013) 1083-1093.
[22] N. He, L. Li, Modeling notch wear of ceramic tool in high speed machining of Nickel-based superalloy, Journal of Wuhan University of Technology-Mater. Sci. Ed., 25 (2010) 78-83.

[23] A. Senthil Kumar, A. Raja Durai, T. Sornakuma, The effect of tool wear on tool life of alumina-based ceramic cutting tools while machining hardened martensitic stainless steel, Journal of Materials Processing Technology, 173 (2006) 151-156.
[24] T. Kitagawa, A. Kubo, K. Maekawa, Temperature and wear of cutting tools in high-speed machining of Inconel 718 and Ti-6Al-6V-2Sn, Wear, 202 (1997) 142-148.
[25] R.T. Coelho, L.R. Silva, A. Braghini, A.A. Bezerra, Some effects of cutting edge preparation and geometric modifications when turning Inconel 718 at high cutting speeds, Journal of Materials Processing Technology, 148 (2004) 147-153.
[26] S. Bhattacharyya, A. Jawaid, M. Lewis, J. Wallbank, Wear mechanisms of Syalon ceramic tools when machining nickel-based materials, Metals Technology, 10 (1983) 482-489.
[27] H. Khamsehzadeh, Behaviour of ceramic cutting tools when machining superalloys, PhD Thesis, University of Warwick, 1991.
[28] E.O. Ezugwu, Z.M. Wang, A.R. Machado, The machinability of nickel-based alloys: a review, Journal of Materials Processing Technology, 86 (1999) 1-16.
[29] M.S. Kasim, C.H. Che Haron, J.A. Ghani, M.A. Sulaiman, M.Z.A. Yazid, Wear mechanism and notch wear location prediction model in ball nose end milling of Inconel 718, Wear, 302 (2013) 1170-1179.
[30] J. Hou, W. Zhou, H. Duan, G. Yang, H. Xu, N. Zhao, Influence of cutting speed on cutting force, flank temperature, and tool wear in end milling of Ti-6Al-4V alloy, International Journal of Advanced Manufacturing Technology, 70 (2014) 1835-1845.
[31] A. Sharman, R.C. Dewes, D.K. Aspinwall, Tool life when high speed ball nose end milling Inconel 718, Journal of Materials Processing Technology, 118 (2001) 29-35.
[32] M. Nordin, R. Sundström, T.I. Selinder, S. Hogmark, Wear and failure mechanisms of multilayered PVD TiN/TaN coated tools when milling austenitic stainless steel, Surface and Coatings Technology, 133–134 (2000) 240-246.
[33] Y. Su, N. He, L. Li, X.L. Li, An experimental investigation of effects of cooling/lubrication conditions on tool wear in high-speed end milling of Ti-6Al-4V, Wear, 261 (2006) 760-766.
[34] J.M. Vieira, A.R. Machado, E.O. Ezugwu, Performance of cutting fluids during face milling of steels, Journal of Materials Processing Technology, 116 (2001) 244-251.
[35] M. Stanford, P.M. Lister, K.A. Kibble, Investigation into the effect of cutting environment on tool life during the milling of a BS970-080A15 (En32b) low carbon steel, Wear, 262 (2007) 1496-1503.
[36] B. Ozcelik, E. Kuram, B. Simsek, Comparison of dry and wet end milling of AISI 316 stainless steel, Materials and Manufacturing Processes, 26 (2011) 1041-1049.
[37] P.S. Sreejith, B.K.A. Ngoi, Dry machining: Machining of the future, Journal of Materials Processing Technology, 101 (2000) 287-291.
[38] S.W. Kim, D.W. Lee, M.C. Kang, J.S. Kim, Evaluation of machinability by cutting environments in high-speed milling of difficult-to-cut materials, Journal of Materials Processing Technology, 111 (2001) 256-260.
[39] M. Kayhan, E. Budak, An experimental investigation of chatter effects on tool life, Proceedings of the Institution of Mechanical Engineers, Part B: Journal of Engineering Manufacture, 223 (2009) 1455-1463.
[40] M.J. Bermingham, S.D. McDonald, M.S. Dargusch, D.H. St. John, Microstructures of cast titanium alloys, Materials Forum, 31 (2007) 84 - 89.
[41] S.L. Semiatin, P.N. Fagin, M.G. Glavicic, I.M. Sukonnik, O.M. Ivasishin, Influence on texture on beta grain growth during continuous annealing of Ti–6Al–4V, Materials Science and Engineering: A, 299 (2001) 225-234.
[42] S. Tamirisakandala, R.B. Bhat, D.B. Miracle, S. Boddapati, R. Bordia, R. Vanover, V.K. Vasudevan, Effect of boron on the beta transus of Ti-6Al-4V alloy, Scripta Materialia, 53 (2005) 217-222.

[43] W. Niu, M.J. Bermingham, P.S. Baburamani, S. Palanisamy, M.S. Dargusch, S. Turk, B. Grigson, P.K. Sharp, The effect of cutting speed and heat treatment on the fatigue life of Grade 5 and Grade 23 Ti-6Al-4V alloys, Materials and Design, 46 (2013) 640-644.

Highlights

- The tool life and wear mechanisms during Laser Assisted Milling (LAM) Ti-6Al-4V is investigated
- LAM is compared against conventional processes (MQL, coolants) as well as hybrid LAM-MQL process
- Adhesion, diffusion, attrition and notching wear generally increased during LAM, however, hybrid-LAM supressed these mechanisms
- Under some circumstances, flood coolant caused thermal shock and tool failure