Effects of tool wear on machined surface integrity during milling of Inconel 718

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Abstract
This study contributes to broadening the knowledge regarding the effects of cutting tool wear on the machined surface integrity characteristics during milling of Inconel 718. The surface roughness and topography, residual stress, microhardness, and microstructure of the resulting Inconel 718 after milling were evaluated under four flank wear conditions. In addition, tool wear morphology, tool lifetime, cutting force, and cutting temperature distribution were analyzed to further explain the mechanisms of surface integrity characteristics. The results of these studies show that the ball nose end mill achieves a tool lifetime of approximately 350 min. The cutting forces increase sharply with a greater tool flank wear width, while the highest cutting temperature shows a decreasing trend at a flank wear width of 0.3 mm. Higher tool flank wear width produces larger surface roughness and deteriorative surface topography. A high-amplitude (approximately −700 MPa) and deep layer (approximately 120 μm) of compressive residual stress are induced by a worn tool with flank wear width of 0.3 mm. The surface microhardness induced by the new tool is larger than that induced by worn tool. Plastic deformation and strain streamlines are observed within 10 μm depth beneath the surface. The results of this study provide an optimal tool wear criterion, integrating the surface integrity requirements and the tool lifetime for ball-end finish milling of Inconel 718.

Keywords Inconel 718 · Tool flank wear · Surface roughness · Residual stress · Microhardness · Microstructure

1 Introduction
Nickel-based superalloys are widely applied in aero-engine key components owing to their excellent chemical and mechanical properties at elevated temperatures [1]. Inconel 718, the most popular nickel-based superalloy, is usually used in manufacturing the hot sections of aero-engines, especially for turbine discs attributing to its superior strength at around 650°C. However, it is also regarded as one of the most difficult-to-cut materials owing to its low thermal conductivity and high-temperature strength and hardness [2, 3]. When machining Inconel 718, severe work hardening, high cutting forces, and temperatures often occur [4]. Besides, welding and adhesion between the alloy and tool material further exacerbate machining difficulties [5, 6]. As a result, cutting tools wear rapidly, thus affecting and damaging the machined surface integrity characteristics, including external topographical parameters, mechanical properties, and metallurgical states.

Various studies focused on investigating the effects of cutting parameters and tool wear on the machined surface integrity of nickel-based superalloys. Several articles reviewed and summarized the recent advances of surface integrity in machining of titanium and nickel alloys [7–9]. The alteration of surface integrity is well known to be associated with the coupled thermo–mechanical effects, which are affected by the cutting parameters such as, tool structure, tool material, and tool wear [10, 11]. Ren et al. [12] proposed a hybrid model coupled thermo–mechanical effects for predicting the residual stress and microstructure modification in turning of Inconel 718, indicating that both the microhardness and grain
size decreased at larger cutting forces. Arrazola et al. [13] found more surface defects in machining with worn tools because of the additional thermo–mechanical stresses, especially when the flank wear width exceeded 0.15 mm. Based on the chip formation, tool wear, and tool edge radius, a mechanistic force model was proposed by Orra et al. [14]. The simulation results showed that increase in the flank wear width led to a dramatic increase in the cutting force. Kamdani et al. [15] reported that during the turning of Inconel 718, a wavier and worse surface was generated from a worn coated carbide tool. Zhang et al. [17] established a simulation model considering the tool flank wear for the prediction of surface topography during the ball-end milling process. The experimental results concerning the milled surface topography are consistent with the model predictions. Jafari et al. [18] found that an increase in the machining time could increase the $R_s$ value when turning Inconel 718, and the effect of tool wear on $R_s$ could be negligible within 120 s turning time. Conversely, a lower $R_s$ value was also reported with slightly worn tools, because the wear scars on the tool flank surface reduce the peak heights of the machined surface profiles, acting as a wiper [19, 20]. In other words, slight tool wear would make the tool–workpiece contact surface more adaptable. Unfortunately, a dramatic increase in the $R_s$ value would inevitably occur at the end of the tool lifetime, attributing to the large tool wear and its fractured cutting edges.

During the machining process, the induced residual stress and work hardening are associated with the coupled thermo–mechanical effects [1]. High thermal gradients tend to generate tensile stress and softening phenomena at small depths below the surface, whereas the extrusion effect produced by mechanical loads induce compressive residual stress and work hardening [21–24]. The final residual stress and microhardness depend on the thermal or mechanical effects, out of which one plays the dominant role [25, 26]. Normally, tool wear increases the enhanced thermal effects more than the mechanical effect, thus tensile residual stresses are induced especially at the surface. Aspinwall et al. [27] and Soo et al. [28] found that when milling with new and worn tools, the surface tensile residual stress of Inconel 718 increased with tool wear because of the dominant thermal effects. Peng et al. [29] experimentally investigated the induced residual stress distributions with new, semi-worn, and worn tools, indicating that for worn tools, the tensile residual stresses distributed within approximately 50 μm beneath the machined surface, while the residual stresses distributed within the range 50–300 μm were of compressive. Niaki and Means obtained similar results when turning Inconel 718 [30], and large tensile residual stresses generated at a distance of 0.05 mm from the surface, followed by small compressive residual stress.

Several studies discussed the effects of tool wear on the microhardness of nickel-based superalloys. General trends indicate that the microhardness increases with tool wear due to severe plastic deformation, nose extrusion, and friction between tool–workpiece interface [31, 32]. Zhuang et al. [33] obtained the in-depth microhardness distributions under different flank wears when dry turning of Inconel 718, and a work-hardened layer of 0.23 mm thickness was found under the notch wear width and depth of 0.2 mm and 0.12 mm. Hood et al. [34] found that when turning RR1000 superalloy, compared with the new tool, the worn tools could produce a deeper work-hardened layer and a higher microhardness value of 50 HK. The microstructure alteration induced by tool wear was analyzed using different micro-examination techniques. M’Saoubi et al. [16] found that the misorientation value was larger when turning Inconel 718 using worn tools compared with new tools. Zhou et al. [35] found that when turning Inconel 718 with new and worn tools, the thickness of subsurface plastic deformation layer increases with the tool flank wear, and this could be explained by the increased thermo–mechanical loads. Agmell et al. [36] reported similar results, and the relative strain was large when using worn tools.

Most published works have focused on the surface integrity of Inconel 718 during turning process, but a few considered the high-speed end milling process. In this study, the effects of tool wear on the surface integrity during the milling of Inconel 718 were evaluated. The machined surface integrity characteristics under various flank wear conditions were examined based on the surface roughness and topography in the surface, residual stress, microhardness, and microstructure in the subsurface. Moreover, the cutting force and temperature were analyzed to further explain the mechanisms of surface integrity characteristics.

2 Experimental work

2.1 Workpiece material and tooling

The workpiece material used in this investigation was a nickel-based superalloy Inconel 718 with the following chemical composition: 16.5–21.0% Cr, 11.5–22.5% Fe, 4.5–5.85% Nb, 0.75–1.20% Ti, 2.5–3.5% Mo, 0.4–0.6% Al, <0.70% Si, <0.5% Mn, <0.1% C, <0.04% S, 0.005% B, and Ni balance (in wt%). Its mechanical properties are listed in Table 1. The heat treatment procedure of the Inconel 718 is as follows: at 720 °C for 8 h, furnace cooled to 620 °C, holding for 8 h, followed by air cooling [37]. Figure 1 presents the matrix microstructure of Inconel 718, consisting of spot-like δ phase (Ni3Nb) and some NbC carbides. The δ phases are irregularly scattered in the grains. The used workpiece material was cut into wedge block using a wire electrical discharge machine. The dimension and workpiece inclination angle are 50 × 50 ×
50 mm$^3$ ($L_0 \times W_0 \times H_0$) and 60°, respectively, as shown in Fig. 2. The PM-4B-R4.0 solid cemented carbide milling cutters (ZCC·CT, China) with four flutes and 8 mm diameter were used throughout the experiments, and their detailed parameters are listed in Table 2. The tool overhang length was kept as 30 mm.

### 2.2 Experimental procedures

All milling tests were performed using a VMC-850 machine with a spindle power of 25 kW, the rotational speeds of 8,000 rpm, and the linear feed rates of 5 m/min. In order to prepare cutting tools with different flank wears, the Inconel 718 workpiece was cut by the layer-to-layer cutting method with horizontal upward cutter path orientation. The milling parameters used were set as follows: cutting speed ($v_c$) 40 m/min; feed rate per flute ($f_z$) 0.02 mm/z; cutting axial depth ($a_p$) 0.4 mm; and cutting radial depth ($a_e$) 0.25 mm. Worn tools with the flank wear widths of 0, 0.1, 0.2, and 0.3 mm were used to cut a new material specimen. Down milling was performed for all the milling processes. Fluid cooling was applied by water-based Trim Microsol 585XT coolant, a semi-synthetic emulsion liquid with good cooling and lubrication performance. The cooling fluid was filtered and used several times. All detailed machining conditions are listed in Table 3.

### 2.3 Simulation procedure

To obtain the generated cutting temperature distribution beneath the surface when using worn tools, the orthogonal cutting process was simulated using commercial software AdvantEdge. A two-dimensional finite element-based machining model was established. The classical Johnson–Cook constitutive equation were applied for the numerical modeling, considering the large strain, high strain rate, and high temperature effect during the cutting process, as shown in Eq. (1) [38].

\[
\sigma = \left(1241 + 622\varepsilon^{0.6522}\right) \left(1 + 0.0134\ln\left(\frac{\dot{\varepsilon}}{\dot{\varepsilon}_0}\right)\right) \left(1-\left(\frac{T-T_{room}}{T_{melt}-T_{room}}\right)^{1.3}\right)
\]

where $\sigma$ is the flow stress, $\varepsilon$ is the plastic strain, $\dot{\varepsilon}$ is the plastic strain rate (s$^{-1}$), $\dot{\varepsilon}_0$ is the reference strain rate (s$^{-1}$), $T$ is the workpiece temperature (°C), $T_{room}$ is the room temperature (25 °C), and $T_{melt}$ is the melting temperature (1300 °C for Inconel 718).

The element distortion because of large deformations was corrected by Arbitrary Lagrangian-Eulerian approach and continuous adaptive remeshing technique. The workpiece and tool were meshed with six-node quadratic triangle elements with three corners and three midsize nodes. The initial element size of used was 0.01 mm, and the fine element size of 0.005 mm was defined near the tool–workpiece interface.

The work tool friction coefficient value is 0.4 and was modeled as per Coulomb friction law. Chip separation was modeled as a tensile fracture event. In the chip bending areas, the state of stress is compared against a maximum stress fracture criterion. If the maximum normal stress criterion exceeds, the chip is weakened and eventually breaks. The thermal conductivity is regarded as a property of the material, which therefore is unchanged by plastic deformation and was set as 1000 kW/m$^2$K.

Carbide-General was used as a cutting tool material, and was assumed to be rigid. The tool rake angle, clearance angle, helix angle, cutting edge radius, and flank wear width were set as the actual value. During the simulation process, the cutting tool is fixed to limit its freedom in the X and Y directions, and the workpiece moves horizontally relative to the cutting tool. The cutting parameters used in the simulation process were adjusted according to the workpiece inclination angle and the actual parameters used in the experiments.

### 2.4 Experimental tests

Cutting forces were measured during tool wear propagation using a force measuring system, comprising a piezoelectric

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**Table 1** Mechanical properties of Inconel 718

| Property              | Value   |
|-----------------------|---------|
| Tensile strength (MPa)| ≥1275   |
| Yield strength (MPa)  | ≥1035   |
| Elastic modulus (GPa) | 204     |
| Density (g/cm$^3$)    | 8.24    |
| Elongation (%)        | ≥12     |

**Table 2** Parameters of the used cutting tools

| Material | Manufacturer | Coating     | Rake angle | Clearance angle | Helix angle | cutting edge radius |
|----------|--------------|-------------|------------|-----------------|-------------|---------------------|
| carbide  | ZCC·CT       | Nano TiAlN of 4μm | 5          | 9               | 30          | 4                   |
dynamometer (9255B, Kistler, Switzerland), a charge amplifier (5080A, Kistler, Switzerland), a data acquisition card (Sirius, DEWESoft, Austria), and a computer with the DEWESoft software, as shown in Fig. 3. \( F_x, F_y, \) and \( F_z \) refer to the pick feed, feed, and axis directions of cutting force, respectively. The resulting force was calculated as the square root of the sum of the squares for the three cutting forces \( F_x, F_y, \) and \( F_z \).

The tool flank wear length of the cutting edges and its morphology were measured using a tool microscope (IF-EdgeMaster, Alicona, Austria). The tool lifetime in this study was determined when the average flank wear length reached the criterion of 0.3 mm. Four cutting flutes were measured, and the average flank wear width was calculated. After cutting for 20 min, the cutting tool was removed from the machine spindle to measure the tool flank wear. Thus, worn tools with the flank wear widths of 0.1, 0.2, and 0.3 mm were obtained under these cutting conditions.

A contact-type surface profiler (MarSurf XR 20, Mahr, Germany) was used to measure the surface roughness. The cutoff length, evaluation length, and measuring speed were 0.8 mm, 4 mm, and 0.5 mm/s, respectively. Five points were taken along the pick feed direction, and the average value was taken. The 3D surface topography was observed at a measuring interval of 0.5 \( \mu m \) over a measuring region of 1 mm × 1 mm.

The residual stress was determined using an X-ray stress analyzer (LXRD MG2000, Proto, Canada) by the \( \sin^2\psi \) method. The detailed measurement conditions used are as follows: Mn K\( _\alpha \) radiation, diffraction angle 151.88\(^\circ\), diffraction planes (311), X-ray beam diameter 1 mm, tube voltage 20 kV, tube current 20 mA, exposure time 1 s, and exposure number 10. The surface material was removed using a saturated solution of CH\( _3 \)OH/C\( _6 \)H\( _14 \)O\( _2 \)/HClO\( _4 \) (10:5:1 by volume) by electrolytic polishing technique. The layers were removed in the steps of approximately 20 \( \mu m \), and residual stress was measured alternatively, followed by determining the in-depth residual stress distribution.

The cross-sections of the specimens were prepared by cleaving, inlaying, sanding, polishing, and etching. The microhardness measurements along the depth direction were taken using a micro-Vickers hardness tester (FM-800, Future-Tech, Japan) with 25 gf load and 10 s holding time. To reduce the measurement errors, the distance between the two test points was more than twice that of the indentation diagonal line. The cross-sectional microstructure image was observed using a scanning electron microscope (TM4000puls, Hitachi, Japan). To ensure the microstructure was more discernible for microscopic observation, the test surfaces were etched for approximately 30 s using a chemical solution of HF/HCl/H\( _2 \)O\( _2 \) (5:2:1 by volume).
3 Results and discussion

3.1 Tool wear and tool lifetime

Figure 4 shows the measured function curve of flank wear propagation with cutting time. The tool flank wear width has three typical stages of initial running into wear, middle steady wear, and final sharp wear. The wear rate of the initial wear stage from 0–70 min is very high due to the small tool edge radius. The intermediate steady wear stage from 70 to 310 min accounts for most of the tool lifetime at a low wear rate. The final sharp wear stage from 310 to 380 min lasts for the shortest cutting time and has the highest wear rate. Similar phenomena were observed when whole end milling of Inconel 718 [39], high-speed turning of RR1000 [34], and ball nose end milling of TC17 [10]. When the cutting time is greater than 320 min, both the flank wear width (reaches 0.25 mm) and rate significantly enhanced. When the cutting time is greater than 350 min, the flank wear width reaches the service limit value (0.3 mm) for the ball nose end mill in practical engineering.

The tool wear morphology from the initial cut to VB 0.3 mm is presented in Fig. 5. Figure 5(a) shows a new sharp flank face. Figure 5(b)–(e) shows the non-uniform flank wear land, which is the dominant tool wear morphology. The cutting edges under different cutting stages have similar flank wear features. Figure 5(c) shows the formation of a bright strip during the tool flank wear propagation due to the exfoliation of the tool material. Wear groove was observed, as shown in Fig. 5(d), because the wear debris of the tool material is stuck on and taken away by the cutting chips. Figure 5(e) shows a rectangle flank wear area with a rough and nonuniform surface with large flaky exfoliation, proving that the cutting process is unstable. The tool side cutting edge angle is strongly affected by the flank wear, which could diminish the cutting aggressiveness of the tool [40].

3.2 Cutting force and temperature

The effects of tool flank wear on the three-axis force, resulting cutting force, and highest cutting temperature are shown in Fig. 6. With increasing tool flank wear width, the cutting force increases linearly. As the tool flank wear width reaches 0.3 mm, the measured resulting cutting force is up to 200 N, which is twice the initial value. Moreover, the simulated highest cutting temperature increases at higher flank wears until VB = 0.2 mm, where the highest temperature of approximately 560 °C is achieved. Further growth in the flank wear decreases the cutting temperature. The high cutting temperature can induce tensile residual stress, phase transformation, and microchip debris adhesion to the machined surface [41]. Fan et al. [42] indicated that at an optimal temperature of 650 °C, the tool has its highest strength ratio, and it reduces the material adhesion when machining Inconel 718. Compared to the simulated value, the measured cutting force is slightly smaller, because of the lubricative effect of the cutting fluid. The errors in the simulated cutting force are mostly in the range 7–19%, verifying the accuracy of the finite element method analysis.

The cutting temperature distribution under various flank wear conditions is shown in Fig. 7. A high-temperature area is developed at the workpiece–tool–chip contact interface.

\[ \text{Table 3: Machining conditions of experiments} \]

| Parameter          | Value |
|--------------------|-------|
| Machine tool       | VMC-850 |
| Workpiece          | Inconel 718 in 50 × 50 × 50 mm wedge block |
| Cutting tool       | PM-4B-R4.0 |
| Cutting parameters | \( v_c = 40 \text{ m/min}, f_z = 0.02 \text{ mm/rev}, a_p = 0.4 \text{ mm}, \text{ and } a_e = 0.25 \text{ mm} \) |
| Cutting method     | Down milling |
| Cooling method     | Fluid cooling |

Fig. 3 The cutting force measurement (a) experiment setup and (b) scheme of measuring cutting force
The highest cutting temperature is mostly in the range 483–559 °C, appearing at the tip of the cutting tool. According to Fan et al. [42], the cutting temperature corresponding to different normal loads with various cutting speeds for Inconel 718 is approximately in the range 520–727 °C, indicating that this finite element model can accurately predict the temperature distributions near the workpiece–tool–chip contact interface. The temperature of the serrated chip is significantly higher compared to that of the workpiece and tool, because the cutting chips dissipate the majority of the heat, a fraction of the heat dissipates in the air, and the residual heat transmits into the subsurface [18]. Moreover, the length of the cutting chips as induced by worn tools is smaller than that induced by new tools, where the thickness of the cutting chips also decreases, attributing to the enhanced cutting-edge radius and flattened flank face, also resulting in an enhanced area of workpiece–tool interface and enlarged volume of heat transfer and dissipation. Therefore, the highest cutting temperature decreases to 496 °C at a tool flank wear width of 0.3 mm.

The temperature data of the machined subsurface layer was extracted from the numerical work, and the in-depth temperature distributions for various tool flank wear widths were plotted, as shown in Fig. 8. A gradual heat-affected layer forms readily, and the cutting temperature decreases gradually with increasing depth beneath surface. The heat-affected layer depth induced by new tools is approximately 60 μm, which is slightly shallower compared to that induced by worn tools (80 μm), because more cutting heat is generated by worn tools and transfers deeper below the machined surface.

### 3.3 Surface integrity

#### 3.3.1 Surface roughness and topography

The $R_a$ and $S_a$ values of the specimens machined using the new and worn tools are shown in Fig. 9, indicating that during the initial cutting stage, a relatively large surface roughness appears due to the sharp tool tip. The surface roughness
decreases slightly at larger VB value until a specific width of
0.1 mm, reaching the lowest $R_a$ and $S_a$ values, because the
peaks of the tool marks are removed due to the enhanced flank
wear face and raised radius of the blunt tool tip, which eventu-
ally decreases the surface roughness. With further increase
in the VB value, the cutting edge becomes irregular, causing
obvious scratches on the surface. When the VB value reaches
0.2 mm, the $R_a$ value is more than 1.0 $\mu$m, and this can no
longer satisfy the design requirements. Thus, the worn tools
cannot be used, even though the tool flank wear width does
not reach the tool wear criterion. As the VB value is 0.3 mm,
the $R_a$ and $S_a$ values are more than 2.5 and 1.8 $\mu$m, respec-
tively, because of a loss in the tool cutting performance.
The surface topographies of the specimens machined with
new and worn tools are shown in Fig. 10. As shown in Fig.
10(a) and (b), the surface is relatively smooth and shows lim-
ited sharp peaks and grooves. No obvious distinction is ob-
served between the machined topographies induced by new
tools and slightly worn tools (VB = 0.1 mm). The results
indicate that during the initial wear stage, the tool flank wear
has a relatively less effect on the surface topography. With
further increase in the tool flank wear width, large grooves

![Fig. 6 Comparison of (a) measured three-axis force and (b) the resultant cutting force and temperature under various tool flank wear widths](image)

![Fig. 7 Cutting temperature distribution of workpiece–tool–chip interface for different tool flank wear widths of (a) 0 mm, (b) 0.1 mm, (c) 0.2 mm, and (d) 0.3 mm](image)
and folds occur due to the effects of extrusion. The depth of grooves is enhanced and the ridges become more irregular at a VB value of 0.3 mm. The observed surface topographies are consistent with the measured surface roughness. Notably, the tool flank wear width below 0.2 mm can guarantee the surface roughness and topography during ball nose end milling of Inconel 718.

3.3.2 In-depth residual stress distribution

Figure 11 shows the in-depth residual stress distributions when milling Inconel 718. The same spoon-shaped profiles of the residual stress were obtained under new and worn tools, which are maximized in compression at a 20–30 μm depth beneath the surface, then gradually decrease, and finally reach a steady value. The tensile residual stress in the range 100–250 MPa was detected on the surface at a VB value of 0.2 and 0.3 mm, while the compressive residual stress from -100 to -450 MPa was detected on the surface at the VB values of 0 and 0.1 mm. The surface tensile residual stress can be explained by the high cutting temperature gradients, as a result of severe rubbing and plowing between the tool–workpiece interface [43]. This indicates that the enhanced thermal effect is dominant under larger tool flank wear width resulting in tensile stress [29], especially along the feed direction at a VB value of 0.3 mm. However, the residual stress in the subsurface is comprehensive, and the affected layers of the compressive residual stress are in the 80–120 μm thickness range, regardless of the tool wear and measured directions. The maximum compressive residual stress increases with increasing tool flank wear width. A possible reason is that the increased cutting temperature only has a limited effect on the compressive residual stress affected layer. Aspinwall et al. [27] found that the surface residual stress depended on the workpiece angle and cutter orientation, while the thickness of the residual stress affected layer became significantly increased for larger tool wear width.

As shown in Fig. 11(b), the maximum surface compressive residual stress of -454 MPa was achieved with the lowest affected layer depth of 80 μm under a VB value of 0.1 mm. When the VB value reaches 0.2 mm, the thickness of the affected layer is 120 μm, and the maximum compressive residual stress is -647 MPa at 20 μm beneath the machined surface. As shown in Fig. 11(a), the maximum magnitude of compressive residual stresses is within the range from -500 to -700 MPa, which is larger than that measured along the feed direction (from -300 to -500 MPa), as shown in Fig. 11(b). A similar phenomenon was observed when ultrasonic surface rolling of TC17 alloy [44]. A possible reason for this phenomenon is that the pick feed force component increases more prominently compared to the feed force component.

3.3.3 In-depth microhardness distribution

Figure 12 presents the in-depth microhardness distributions under different VB values. Gradual work-hardened layers were observed under the VB values of 0 and 0.1 mm, and the surface microhardness value along the pick feed direction reaches up to 618 HV 0.025, which is approximately 28% greater than the microhardness in bulk material. Then the microhardness decreases rapidly within the range 0–40 μm and finally fluctuates from 450 to 500 HV 0.025. However, the microhardness induced under the VB values of 0.2 mm and 0.3 mm changes slightly. Comparing the microhardness shown in Fig. 12 indicates that the in-depth microhardness value along the pick feed direction is slightly higher than that along the pick feed direction, indicating a consistency between the residual stress and microhardness. A possible explanation could be due to different plastic deformations, dislocation densities, and grain refinements along the two directions.
As shown in Fig. 12(b), the surface microhardness produced with new tools (maximum value is 618 HV0.025) is larger than that from the worn tools. The drop in microhardness levels induced by the worn tools is mainly due to the strain relief caused from the high expected cutting temperatures. However, work hardening is more often observed in the worn tools compared to the new tools. Milling with worn tools increases the tool–workpiece contact area, due to reductions in the flank angle, thus escalating the thermo–mechanical loads and plastic deformation. High cutting temperatures in the cutting area induce a softening layer in the microhardness, whereas work hardening is generally indicative of high machining pressures and cutting forces during chip formation. The small thermal conductivity of Inconel 718 (18.3 W/m·°C, 400°C) causes cutting heat to accumulate on the surface area, decreasing the microhardness [37]. Similar results were described by Soo et al. [45], Ezugwu et al. [2], and Sharman et al. [19] when turning Inconel 718.

### 3.3.4 Microstructure observation

The images of the microstructure machined using the new and worn tools are shown in Fig. 13. During milling, the microstructure changes due to the interactions between the strain hardening and thermal softening. An uneven milled surface, broken lattice, and deformed grains are observed at a magnification of 2000×. A gradient microstructure is produced at the subsurface layer, and the plastic deformation decreases with increasing depth. The thickness of the plastic deformed layer varies within tens of micrometers, and increasing tool flank wear exaggerates the plastic deformation depth, and this can be explained by the severe rubbing. Agmell et al. [36] also found a gradient microstructure when turning Inconel 718.

A surface cavity was observed on the milled surface, as shown in Fig. 13(a), associated with the NbC particles. In general, carbide particles are hard and brittle, and can decrease the tool lifetime while inducing defects on machined surfaces. Flaky exfoliation of the workpiece surface material was also

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**Fig. 10** Surface topographies for different tool flank wear widths of (a) 0 mm, (b) 0.1 mm, (c) 0.2 mm, and (d) 0.3 mm

**Fig. 11** In-depth residual stress distributions for different tool flank wear widths along the (a) feed and (b) pick feed directions
observed. Small surface material particles were plucked and removed by the cutting chips. More flaking material was observed under more intense flank wear conditions. Material strain streamlines were observed, as shown in Fig. 13(a)–(d) likely due to the grains being elongated and rotated along the tool feed direction during milling. Moreover, the slope of the streamline increases with the flank wear. Figure 13(d) shows that the angle between the material strain streamlines and the feed direction is very small, and the streamlines have good consistency, indicating severe plastic flow at a VB value of 0.3 mm.

According to (ISO 8688–2, 1989), the average flank wear of 0.3 mm or the maximal wear of 0.5 mm was selected as the tool wear criterion. However, the tool wear criterion in this study is the requirement of machined surface integrity. For the finish milling process, (i) the surface roughness Rₐ must be smaller than 1.6 μm, (ii) surface compressive residual stress and slight surface work hardening should be suitable, and (iii) severe plastic deformation or white layer are undesired. These requirements of surface roughness, residual stress, microhardness, and microstructure alteration should be considered comprehensively to decide the tool to be still operative. The plots

![Fig. 12 In-depth microhardness distributions for different tool flank wear widths along the (a) feed and (b) pick feed directions](image)

![Fig. 13 Microstructure of Inconel 718 for different tool flank wear widths of (a) 0 mm, (b) 0.1 mm, (c) 0.2 mm, and (d) 0.3 mm](image)
of the tool flank wear and surface integrity characteristics indicate that at a flank wear of 0.3 mm, the surface roughness increases up to 2.5 μm, the surface residual stress is tensile in the range 100–250 MPa, and strain streamlines run nearly parallel to the feed direction. Therefore, to avoid the effect of catastrophic tool wear on surface integrity characteristics, the cutting tools should be used to cease the extensive tool wear before it reaches these conditions, and the proper tool wear criterion must be selected based on the surface integrity characteristics requirement. In this study, the flank wear criterion is approximately 0.2 mm for the finish ball-end milling Inconel 718, with the corresponding tool life of approximately 250 min. Comparing this with the flank wear criterion of 0.3 mm (tool lifetime of approximately 350 min), the tool lifetime decreases by 100 min, however the machined surface integrity characteristics are ensured. Thus, an optimal tool wear criterion can be defined as integrating the surface integrity requirements and the tool lifetime for ball-end finish milling of Inconel 718.

4 Conclusions

The cutting force, temperature, tool flank wear, and surface integrity characteristics were investigated during ball-end milling Inconel 718. The effects of tool flank wear on the surface integrity characteristics were analyzed. The conclusions of this study are summarized as follows:

1. When operating at down milling under fluid cooling at a cutting speed of 40 m/min, a feed rate per flute of 0.02 mm/z, a cutting axial depth of 0.4 mm, and a cutting radial depth of 0.25 mm, the tool lifetime was approximately 350 min based on a flank wear criterion of 0.3 mm. The cutting force increases with increasing VB value, while the simulated cutting temperature has a downward trend at a VB value of 0.3 mm. The simulated highest cutting temperature is in the range 480–560 °C, and the thickness of the heat-affected layer is approximately 80 μm.

2. A higher tool flank wear width increases the surface roughness, and more than Rz, 2.5 μm surface roughness is achieved when the VB value reaches 0.3 mm. Wavier surface sharp peaks and grooves are generated, and a distinct surface topography deterioration is observed for larger tool flank wear widths.

3. The in-depth residual stress distribution is directly affected by the tool flank wear. An increased tool flank wear causes a high-amplitude and deep layer of compressive residual stress (approximately −700 MPa and 120 μm). The surface microhardness induced by the new tools is 618 HV 0.025, which is larger than that induced by the worn tools (in the range 460–530 HV 0.025). The thickness of work-hardened layer is approximately 60 μm.

4. Uneven milled surfaces, broken lattices, and deformed grains were observed in the subsurface microstructure. The strain streamline with a slope towards deeper layers was observed within 10 μm below the surface. The strain streamlines have good consistency and are nearly parallel to the tool feed direction at a VB value of 0.3 mm.

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Data availability Not applicable.

Declarations

Ethics approval This paper does not require any ethical approval as the study does not involve human participants or animals.

Consent to participate Not applicable as this work does not involve human subjects.

Consent for publication The work described is accomplished in our laboratory and has not been published or under consideration for publication elsewhere. All authors have read and approved this version of the article, and due care has been taken to ensure the integrity of the work.

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