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Integrated Approach to Advanced Machining

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Abstract

This is the final report of a one-year, Laboratory Directed Research and Development (LDRD) project at the Los Alamos National Laboratory ((LANL). The residual stress state induced by machining in a titanium alloy as a function of the cutting tool's sharpness and the depth of cut has been predicted and measured. The residual stresses were greater for the dull tool than for the sharp tool. X-ray diffraction measurements were used to measure the residual stress state of the material, and these measurements revealed that the hoop stress increased with depth of cut; however, the radial stress decreased with depth of cut. An elastic-plastic model provided a possible explanation for this behavior in that, for small depths of cut, the tool makes multiple passes through the damaged subsurface layer. This causes both residual stress components to increase, but the radial stress increases by a much greater amount than the hoop stress.

1. Background and Research Objectives

During a machining operation stress is induced in the part being machined. After the cutting tool is removed from the part an amount of residual stress remains near the machined surface. This residual stress can severely affect the part's performance and the dimensional accuracy of its final shape. There have been very few investigations that attempt to predict and measure the residual stress induced by a machining operation. This is surprising since most metallic precision components produced throughout the world are machined. It is the objective of this project to predict and measure the residual stresses induced in a machined surface under carefully controlled machining conditions.

2. Importance to LANL's Science and Technology Base and National R&D Needs

As mentioned above, machining is very important in the production of close tolerance metallic components. These components are used continuously throughout

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society in every part of the world. In 1978 machining costs in the United States were about \$115 billion. In today's production environment this figure could easily be 2 to 3 times this amount [1]. The increased drive towards product quality and the need for increased flexibility on the part of manufacturing has enhanced the importance of machining and the understanding of the machining process.

At LANL the production of very close tolerance, thin-walled metallic parts is very important. Since essentially all of these parts are machined, a better understanding of the machining process will enable LANL to produce these parts more accurately and economically.

Residual stresses induced by machining can cause high precision components to distort both during and after the machining operation. This distortion can be severe enough to cause the final part's shape to be out of the specified tolerance. Thus a very expensive part can become scrap without any warning of a problem in the machining operation. Since there has been very little work done in understanding the residual stress state and the important parameters in the machining process that contribute to a particular state, it was decided to perform closely controlled cutting experiments where the residual stress state is both modeled and measured. These investigations will enable us to determine how to better control the effect of residual stress in machined components.

3. Scientific Approach and Accomplishments

The specimen material chosen for use in these investigations was an alloy of titanium that contained 6% aluminum and 4% vanadium. A particular specimen of this alloy was chosen because its mechanical properties at strain rates comparable to those encountered in machining had been measured. Two cutting parameters were varied during these experiments these were the depth of cut and the sharpness of the tool being used. These two parameters were chosen because during a normal machining operation they are varied. The depth of cut is varied intentionally by the operator to a smaller value as the final part dimension is approached. The cutting tool's sharpness varies unintentionally as the result of wear. By independently changing these two parameters we can determine the contribution of each to the residual stress state.

The basic experimental approach was to: 1) heat treat the material to remove all residual stresses caused by the plate rolling process, 2) produce the final specimens to be machined out of the heat treated material, 3) measure the residual stress state in the specimens prior to machining, 4) measure the tool edge sharpness of the cutting tool prior

to machining, 5) machine the specimens at three different depths of cut with the sharp tool and the dull tool, and acquire cutting and thrust force measurements during the machining process, 6) measure the residual stresses after the machining process, 7) predict the residual stress state using an elastic-plastic model, and 8) compare the calculated stresses to the measured stresses.

Heat Treatment

An initial residual stress measurement of the material in the as-received condition revealed that an average residual stress of -400 MPa was present. A stress state of this magnitude is significant and could affect the machining results; therefore, it was decided to heat treat the material to attempt to remove this stress. The heat treatment used was identical to that of Follansbee and Gray [2] which consisted of: 1) a solution treatment at 800 °C for 1 hour followed by a water quench, and 2) an age at 500 °C for 8 hours.

After this, the specimens, which consisted of a set of rings, were cut out of the blank with a wire Electron Discharge Machine. The specimens were then etched with a 40% hydrofluoric acid solution to remove the scale.

The residual stress state of the specimens was measured after this procedure and determined to be stress free within the experimental uncertainty limits. The specimens were now ready to be machined.

Cutting Experiments

An orthogonal, i.e. two-dimensional, cutting geometry was chosen because it makes data analysis and modeling easier than a three-dimensional geometry. Much useful information can be gleaned from the orthogonal geometry so it was deemed to be a good framework in which to proceed. A schematic of this geometry is shown in Figure 1 with the description of some of the terminology used throughout this report. Each of the tools used in the set of experiments described had a rake angle of 0°.

The Precision Automated Turning System (PATs) was used as the testbed for doing the machining experiments. A Kistler three-axis dynamometer was used to measure the cutting and thrust forces. This particular dynamometer has six piezoelectric force transducers that are connected in such a way as to compensate for moments that may be encountered during the cutting process. Therefore the output from the dynamometer is a voltage that is proportional to a pure force along the measurement axis. The calibration of the dynamometer was performed before each experiment with a calibrated spring in the

thrust direction and a calibrated weight in the cutting direction. A Nicolet digital storage oscilloscope was used to simultaneously record the force data for each direction.

The cutting tools were supplied to LANL by Kennametal, Inc. [3]. Insert tooling was used, as is standard practice throughout industry, with the inserts being triangular in shape. Each edge of the triangle was approximately 12 mm long and each edge was sharpened according to our requested specifications. The tools consisted of a tungsten carbide substrate with a sputtered coating of titanium nitride; Kennametal's designation for this material is KC730. This is the tool material that was recommended by Kennametal for machining titanium alloys. It is also widely used throughout industry for machining a variety of metals and is currently being investigated as a material for machining uranium at LANL.

Tools with three different edge sharpnesses were supplied but only two were used. The tool edge geometry was measured using a WYKO RST optical surface profilometer. This instrument is a computerized optical interference microscope that makes three-dimensional measurements of surfaces. Its accuracy for measuring the shape of our tool edges is about 20 nm. The method used to characterize the sharpness of the tool was to fit a circle to a profile measurement of the tool. The sharp tools' average edge radius was 1.5 μm and the dull tools' average edge radius was measured to be 10 μm . These are representative of what would be in use for sharp and dull tools throughout industry.

The specimens were rings whose dimensions are summarized in Table 1. To achieve the orthogonal cutting geometry, each ring was rotated in the PATS spindle at a constant angular velocity and the cutting tool was fed into the end of the ring at a constant linear velocity. This procedure produced a constant depth of cut of a known amount. The spindle speed was adjusted for each ring to give a cutting velocity of 1 m/s for all tests. Three cutting depths were used: 1 μm , 4 μm , and 40 μm . A new tool edge was used for each depth of cut. Prior to recording the force data, 0.25 mm of material was removed with the virgin cutting edge to assure that the ring's face was flat. No lubricants or coolants were used for these tests.

Table 1. Ring Dimensions

Ring number	Inside diameter	Outside diameter	Wall thickness
2	60.35 mm	63.55 mm	3.20 mm
3	69.87 mm	73.10 mm	3.23 mm
4	82.38 mm	85.52 mm	3.14 mm

Figure 2 shows a plot of the cutting and thrust forces per unit width as a function of the depth of cut for each of the tools. The error bars show the maximum and minimum force for each experiment. A number of observations can be made from this plot. One is that both the cutting and thrust forces for the dull tool are always higher than for the sharp tool. Another is that this difference is greater for the thrust force than for the cutting force. Since the thrust force component is the one that should affect the residual stresses, this difference may have a significant effect on the residual stress state. As the depth of cut becomes smaller, the thrust component becomes larger than the cutting component of the total force. Thus for smaller depths of cut a greater portion of the machining energy goes into plowing the part's surface than in removing material which means that the machining process becomes less efficient at smaller depths of cut. The machining efficiency is better for a sharp tool than for a dull tool at all cutting depths. The crossover point for the transition from cutting dominance to plowing dominance is at a depth of cut of approximately 12 μm for each tool under these cutting conditions. Even though machining becomes more efficient at larger depths of cut the thrust force monotonically increases with cutting depth so it is anticipated that greater residual stresses will be induced as the depth of cut increases.

Residual Stress Measurements by X-ray Diffraction

X-ray diffraction measurements of residual stresses in the rings were made using the rotating anode located at the Los Alamos Neutron Scattering Center. The x-ray diffraction method measures strains in the surface layers of a material [4-7]. Briefly, the interplanar spacing of a specific set of planes is obtained from grains of different orientations to the surface normal. This is determined by tilting (by an angle of ψ) and rotating the specimen (by an angle of ϕ) with respect to the incident x-ray beam. The change in interplanar spacing as a function of orientation is due to surface strains and this can be related to the surface stresses by an analysis suggested by Noyan [3,4]. Accordingly, for known values of $d_{\phi\psi}$ (the interplanar spacing in the strained material) and d_0 (the interplanar spacing in the strain-free material), the average strain along the ϕ - ψ direction with respect to the surface normal can be written as

$$(\epsilon'_{33})_{\phi,\psi} = \frac{d_{\phi\psi} - d_0}{d_0} \quad , \quad (1)$$

where $(\epsilon'_{33})_{\phi,\psi}$ can be related to the surface stresses using the conventional elasticity equations. For a simple plane stress condition, the stress state in the sample can be determined by a linear relationship between $(\epsilon'_{33})_{\phi,\psi}$ and $\sin^2\psi$ [3]:

$$(\epsilon'_{33})_{\phi,\psi} = \frac{(1+\nu)}{E} \{ \sigma_{\phi} \} \sin^2\psi - \frac{\nu}{E} (\sigma_{11} + \sigma_{22}) \quad (2)$$

where

ν = Poisson's ratio

E = Elastic modulus

$\frac{(1+\nu)}{E}$ and $-\frac{\nu}{E}$ = x-ray elastic constants, and

$$\sigma_{11}\cos^2\phi + \sigma_{22}\sin^2\phi = \sigma_{\phi} \text{ (the stress in the direction of } \sigma_{\phi} \text{).}$$

Equation (2) represents the equation of a straight line with the slope proportional to σ_{ϕ} . A plot of $d_{\phi\psi}$ vs. $\sin^2\psi$ would yield a straight line with the slope of the line giving $\frac{(1+\nu)}{E} \{ \sigma_{\phi} \}$. Knowing the x-ray elastic constants from above, the stress in the direction of ϕ can be determined. The plot of $d_{\phi\psi}$ versus $\sin^2\psi$ is also called "regular" because of the linear behavior between the variables $d_{\phi\psi}$ and $\sin^2\psi$. This technique is also called the "sin² ψ " method of residual stress measurement. The advantage of this technique is that the value of d_0 , which is sometimes very difficult to obtain for want of stress-free material and which is very sensitive to the accuracy of residual stress measurement, is not required for this analysis. For this type of analysis, any consistent feature of the peak profile may be utilized to define a reference point whose position is monitored as a function of the ψ - tilt. In this study, the assumption of a bi-axial plane stress state is generally valid since the average depth of penetration of Cu K α x-rays in this alloy has been computed to be

between about 5 microns (at $\psi = 60^\circ$) and 12 microns (at $\psi = 0^\circ$). For the Ti-6Al-4V alloy, characterized by an hcp structure, the diffraction peak (8 4 12 3) at 143° of 2θ was examined in ψ angles of 0, 15, 30, 45, and 60° .

The stress σ_ϕ measured by the x-ray method contains both the macrostress and microstress component. The raw data consists of plots of d versus $\sin^2\psi$, which were fairly "linear" or "regular". The slope of the linear plots were used to determine the surface residual stress along the specific orientation, namely parallel to the circumference of the ring (hoop stress) and parallel to the radius of the ring (radial stress). For a "regular" behavior of d vs. $\sin^2\psi$ the stress σ_ϕ represent a uniform stress distribution in the measured area of the sample, implying that the microstress components cancel out due to equilibrium conditions within the diffracting volume of the depth of penetration of the x-rays. Therefore the remaining stress component is the macrostress. This macrostress component can be used for validating the predictions from analytical or finite-element models.

Figure 3 shows the measured and calculated values of the hoop and radial stresses for the sharp and dull tool. The negative sign for the stress values indicate that the residual stresses are compressive. A larger negative value means that the stress is greater than a smaller negative value. These data show that as the depth of cut increases the hoop stress also increases and that the hoop stress for the dull tool is greater than the sharp tool's value. These results are in line with what would be expected by an increasing thrust force with an increasing depth of cut. What is surprising is that the radial stress component shows opposite trends, i.e. that as the depth of cut increases the radial stress magnitude decreases. The dull tool does tend to have a higher radial stress, as is anticipated, but this trend is very slight.

To provide insight into what physical effects may be happening to explain these data, an elastic-plastic analysis of the plowing process was performed.

Elastic-Plastic Plowing Model

The basic assumption in the model was that the clearance face of the cutting tool would induce stress in the part as a loaded flat slider would induce stress in an elastic half-space. This assumption was also made by Lucca and Seo [8] and our method of analysis is identical to the one described in their paper. In addition to this basic assumption, it is

assumed that plane strain conditions apply (which is exactly what the orthogonal cutting geometry is attempting to achieve) and that the material is elastic-perfectly plastic, i.e. that it does not strain harden. These assumptions are somewhat idealized since our specimen material does strain harden. However, the amount of strain involved is very slight (the maximum value is calculated to be 0.02 in the specimen's surface) and strain rate effects are considered by using flow stress data from ref. [2] at the calculated strain rate of about 5000/s. The model should give a good approximation of the residual stress state and provide insight into what physical mechanisms are important in the production of this state. The approach is to first determine the elastic stresses in the half space, use these as the initial conditions for determining the plastic stresses, then elastically unload the material to determine the residual stress state. This procedure was first used by Merwin and Johnson [9] to analyze stresses induced by rolling contacts and is provided in flow chart form by Suh [10].

Equations (3) through (5) describe the elastic stress fields for the elastic half-space in dimensionless form. Figure 4 shows the relationship of the slider geometry to the tool geometry and gives the relationship of the dimensionless coordinates to the physical coordinates.

$$\sigma_{\xi\xi}(\xi, \eta) = \frac{2}{\pi G_1} [-4kIA + 2(2 - M^2)\mu_f IB + (M^2 + 2k^2)\{(j + \frac{1}{j})IC - 2\mu_f ID\}] \quad (3)$$

$$\sigma_{\eta\eta}(\xi, \eta) = \frac{2}{\pi G_1} [-4kIA - 2(2 - M^2)\mu_f IB - (2 - M^2)\{(j + \frac{1}{j})IC - 2\mu_f ID\}] \quad (4)$$

$$\sigma_{\xi\eta}(\xi, \eta) = \frac{2}{\pi G_1} [(j + \frac{1}{j})\{(2 - M^2)\mu_f IA + 2kIB\} + 2k\{-2\mu_f IC - (j + \frac{1}{j})ID\}] \quad (5)$$

where,

$$\sigma_{\xi\xi} = \frac{\sigma_{xx}}{P_0}, \quad \sigma_{\eta\eta} = \frac{\sigma_{yy}}{P_0}, \quad \sigma_{\xi\eta} = \frac{\sigma_{xy}}{P_0} \quad (6)$$

and

$$P_0 = \frac{F_t}{2Lw} \quad (7)$$

where F_t is the thrust force, L is half of the slider length, and w is the width of the slider.

The dimensionless coordinates are related to the physical dimensions by equation (8).

$$\xi = \frac{x}{L}, \quad \eta = \frac{y}{L} \quad (8)$$

The constants that appear in equations (3) - (5) relate the material properties, the velocity, and the friction coefficient. These are given by the following expressions:

$$G_1 = (j + \frac{1}{j})(2 - M^2) - 4k, \quad j^2 = 1 - M^2, \quad k^2 = 1 - \frac{M^2}{N^2}, \quad M = \frac{V}{C_s}, \quad N = \frac{C_d}{C_s} \quad (9)$$

$$C_d^2 = \frac{\lambda + 2\mu}{\rho}, \quad C_s^2 = \frac{\mu}{\rho} \quad (10)$$

where λ and μ are Lamé's constants, ρ is the mass density, and V is the velocity.

The expressions for IA, IB, IC, and ID are integrals of the following form:

$$IA = \int_0^\infty \frac{\sin s}{s} \cos(s\xi e^{-js\eta}) ds, \quad IB = \int_0^\infty \frac{\sin s}{s} \sin(s\xi e^{-js\eta}) ds, \\ IC = \int_0^\infty \frac{\sin s}{s} \cos(s\xi e^{-ks\eta}) ds, \quad ID = \int_0^\infty \frac{\sin s}{s} \sin(s\xi e^{-ks\eta}) ds \quad (11)$$

The functions within the integrals rapidly decay to a near zero amplitude so that upper limit can be set to 40 with an error introduction of only 2% which is much smaller than the combined experimental uncertainties.

The finite evaluation of these integrals was performed using Mathcad® on a personal computer. Equations (3) - (5) were used to calculate the dimensionless stresses for a grid where ξ ranged from -3 to 3 in 0.25 increments and η ranged from 0 to 1.2 in

0.1 increments. These values were stored on a disk. By calculating the stresses in dimensionless form, a variety of stress states could be rapidly evaluated by simply using equations (6) - (10) to determine the physical values.

Equations (3) - (5) give the elastic stress field; the Prandtl-Reuss equations give the plastic stresses. These equations are:

$$\begin{aligned}\frac{ds_x}{dt} &= 2G\left[\frac{de_x}{dt} + \frac{dW}{dt} \frac{s_x}{2k^2}\right], \text{ etc.} \\ \frac{d\sigma_{xy}}{dt} &= G\left[\frac{d\gamma_{xy}}{dt} + \frac{dW}{dt} \frac{\sigma_{xy}}{k^2}\right]\end{aligned}\quad (12)$$

where,

$$s_x = \sigma_{xx} - s, \quad e_x = \varepsilon_x - e, \quad \varepsilon_x = \frac{1}{E}[\sigma_{xx} - \nu(\sigma_{yy} + \sigma_{zz})], \text{ etc.}$$

$$\gamma_{xy} = \frac{\sigma_{xy}}{G} \quad (13)$$

$$s = \frac{(\sigma_{xx} + \sigma_{yy} + \sigma_{zz})}{3}, \quad e = \frac{(\varepsilon_{xx} + \varepsilon_{yy} + \varepsilon_{zz})}{3},$$

$$\frac{dW}{dt} = s_x \frac{de_x}{dt} + s_y \frac{de_y}{dt} + s_z \frac{de_z}{dt} + \sigma_{xy} \frac{d\gamma_{xy}}{dt} \quad (14)$$

G is the shear modulus, E is the elastic modulus, k is the flow stress in pure shear, and $\frac{dW}{dt}$ is the rate of plastic work. The Von Mises criterion was used to determine the yield condition which is:

$$J_2 = 0.5(s_x^2 + s_y^2 + s_z^2) + \sigma_{xy}^2 = k^2 \quad (15)$$

The Prandtl-Reuss equations give the plastic stresses as derivatives with respect to time. These can be transformed to derivatives with respect to x by simply dividing both sides of

equations (12) - (14) by the velocity. By fitting the values of the elastic stresses as a function of x to a polynomial, the derivatives and values can be easily evaluated at arbitrary values of x . To obtain the plastic stress values, the expressions in equation (12) must be integrated. This was done using a 4th order Runge-Kutta as described in Carnahan, Luther, and Wilkes [11]. At each depth the elastic stresses were evaluated at increasing ξ values to determine when the stress state becomes plastic according to the Von Mises criterion expressed in equation (15). When the plastic state is achieved, Runge-Kutta integration of equations (12) - (14) was performed at increments of $\Delta\xi = 0.001L$. When $J_2 < k^2$ or $\frac{dW}{dt} < 0$ the stress state becomes elastic and no further integration is required. After the plastic stresses are found they are relaxed elastically according to equations (16) and (17)

$$(\sigma_{xx})_r = (\sigma_{xx})_p - \nu \frac{(\sigma_{yy})_p}{(1 - \nu)} \quad (16)$$

$$(\sigma_{zz})_r = (\sigma_{zz})_p - \nu \frac{(\sigma_{yy})_p}{(1 - \nu)} \quad (17)$$

where $(\sigma_{xx})_r$ and $(\sigma_{zz})_r$ are the residual stresses and $(\sigma_{xx})_p$, $(\sigma_{yy})_p$, and $(\sigma_{zz})_p$ are the plastic stresses.

The equations were evaluated with $\lambda = 92$ GPa, $\mu = 46.7$ GPa, $G = 39.8$ GPa, $E = 124$ GPa and $k = 1$ GPa. The coefficient of friction, μ_f , was determined by taking a decreasing depth of cut on an unheat-treated ring. The ratio of the cutting force to the thrust should asymptotically approach the coefficient of friction. This method yielded a value of 0.4 for μ_f .

The other parameter needed was the slider length. An attempt was made to measure this value in a measuring microscope by measuring the distance from the rake face to the end of the wear marks that appear along the clearance face. Adherence of workpiece material to this face masked the marks so an accurate measurement could not be obtained. If the distance to end of the adhered material was used as the slider length, the stress was so low that no plastic deformation would take place resulting in no residual stress. The x-ray diffraction measurements clearly indicate that this is not the case, so another method for determining this value was adopted. If a value of $P_0 = 2k$ was chosen, and the slider

length calculated using equation (7), reasonable agreement with the x-ray data was achieved. This value is reasonable since a factor of $P_0 \approx 3k$ would result in chip formation and the ploughing stress must be less than this amount.

Discussion of Results

A plot of the elastic-plastic boundary, as determined by the model, for each of the machining cases is shown in Figure 5. These plots are in dimensionless form. To determine the physical dimensions the value of L for each case must be known; these values are provided in table 2.

Table 2. L -values for each of the machining tests.

	Sharp tool			Dull tool		
depth of cut, μm	1	4	40	1	4	40
L value, μm	7.82	8.99	13.48	9.00	10.04	16.92

The depth of the plastic zone is of particular interest in understanding how the residual stress state is developed. For the $40\ \mu\text{m}$ cutting depths the deepest penetration of the plastic zone is for the dull tool and is $5\ \mu\text{m}$. For the $4\ \mu\text{m}$ cutting depths the plastic zone is $3\ \mu\text{m}$ for the dull tool. This means that for each pass of the cutting tool at a particular location the tool is always plowing in virgin material. However for the $1\ \mu\text{m}$ cutting depths the plastic zone is $2.4\ \mu\text{m}$ for the sharp tool and $2.7\ \mu\text{m}$ for the dull tool. Thus each cut at a particular location is plowing in prestressed material so the effect of multiple passes through the material must be considered. This is the reason the plastic zone extends so far ahead of the cutting tool for the small depths of cut as seen in Figure 5a as opposed to the larger cutting depths as shown in Figure 5b.

Multiple passes through the prestressed material also changes the residual stress profiles as is seen in Figure 6. Plowing through prestressed material (as is the case for smaller depths of cut) tends to increase the values of both the hoop and the radial stresses; however, the effect is more pronounced for the radial stress than for the hoop stress. This helps to understand why the x-ray diffraction measurements showed an increase in radial stress with a decreasing depth of cut.

To compare the calculated residual stress state with the measured state, the x-ray interaction volume in the material must be considered. The stress value provided by the x-ray diffraction method is an average taken over the volume that the x-rays interact with the material. The x-ray penetration depth is estimated to be $12\ \mu\text{m}$ and its diameter at the surface is $2\ \text{mm}$. So the shape of the interaction volume over which the stress

measurement is taken was assumed to be a cone with a 2 mm base diameter and a 12 μm height. The stress profiles seen in Figure 6 were integrated over this conical volume and this average number is what is plotted in Figure 3.

When comparing the calculated stress values with the experimental values it is apparent that the calculations tend to overpredict the hoop stresses and underpredict the radial stresses. The profiles in Figure 6 indicate that the calculated residual stress values are much greater than the average value provided by the x-ray diffraction measurements. If the calculated values are accurate it could have a significant impact on part performance and its final shape accuracy. Additional modeling and experimentation will be required to bring the experimental and theoretical results into closer harmony.

Extrapolation to Three-Dimensional Machining

Most machining operations involve a three-dimensional cutting geometry. This is the case when producing hemispherical shells. In three-dimensional geometry the depth of cut varies from zero to the value of the tool feed per revolution and for typical finishing cuts each point on the part will see at least two tool passes and much of the part will see three or more depending upon the tool geometry and the feed per revolution.

For the case of producing a hemispherical shell, the radial stress in orthogonal geometry becomes the longitudinal stress and the hoop stress becomes the latitudinal stress. Since each point on the hemisphere will see multiple passes of the tool during the finishing operation, the longitudinal stress will be generally high and both stresses will be cyclical since the number of tool passes will vary as a function of polar angle. This complicated stress state could affect the final part shape especially for thin-walled shells.

Conclusions

The residual stress state in a titanium alloy as a function of the cutting tool's sharpness and the depth of cut has been both predicted and measured. The residual stresses were greater for the dull tool than for the sharp tool. The x-ray diffraction measurements showed that the hoop stress increased with depth of cut, however the radial stress decreased with depth of cut. The model gave a possible explanation for this behavior in that for small depths of cut the tool makes multiple passes through the damaged subsurface layer which causes the residual stresses to increase but the radial stress increased by a much greater amount than the hoop stress.

An extrapolation of these findings to three-dimensional cutting geometry predicts that the residual stress state of finished hemispherical parts will be cyclical with respect to the polar angle and of a high average magnitude because of numerous passes of the cutting.

tool over material that has been left in a prestressed state by previous passes of the cutting tool.

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Figure 1. Orthogonal cutting geometry.

Figure 2. Cutting and thrust forces for heat treated Ti-6Al-4V specimens. The average is plotted. The uncertainty bars indicate the maximum and minimum measured force values.

a)

b)

c)

d)

Figure 3. Measured and calculated residual stresses. The average value is plotted. The uncertainty bars for the measured residual stresses are the combination of the variation in the measurements at four locations around each ring and the measurement uncertainty. The uncertainty bars for the calculated values come from the variation in the force measurements that were used in the data analyses. a) sharp tool hoop stress, b) sharp tool radial stress, c) dull tool hoop stress, d) dull tool radial stress.

Figure 4. Relationship of loaded slider to cutting geometry.

a)

b)

Figure 5. Location and shape of calculated elastic-plastic boundary. a) Zone for 1 μm depth cut for both sharp and dull tools. b) Zone for 4 and 10 μm depths of cut for both sharp and dull tools. Units are dimensionless so multiplication by the L value for a specific cutting condition gives the physical dimensions of the plastic zone.

a)

b)

Figure 6. Hoop and radial residual stress profiles. a) Profile for sharp tool 4 μm and 40 μm depths of cut. b) Profile for sharp tool 1 μm depth of cut.