

Energy Dissipation in Modulation-Assisted Machining of Aerospace Alloys

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ABSTRACT

The beneficial effects of contact disruption in modulation-assisted machining of aerospace alloys have been well documented, but sources for such improvements are not well understood. This study explores the underlying nature of differences that occur in energy dissipation during conventional and modulation-assisted machining by characterizing the relationship between controllable process parameters and their effects on chip formation. Simultaneous *in situ* force and tool position measurements are used to show that the forces in modulation-assisted machining can be described by empirical force models in conventional machining conditions. These models are found to accurately describe plastic dissipation over a range of modulation conditions and configurations, including in cases where energy expenditure decreases with the application of modulation. These observations suggest that the underlying response in modulation-assisted machining is analogous to that of conventional machining. Implications for better understanding energy-related effects for modulated tool paths are briefly discussed.

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INTRODUCTION

Development of new methods for improving machinability of aerospace materials is critical toward supporting the introduction of new material systems with enhanced properties and performance characteristics. Examples of technologies recently introduced to improve machining of high performance materials in the aerospace sector include modulation assisted machining (MAM) [1,2,3,4,5,6,7,8,9,10,11] and cryogenic cooling. While studies have demonstrated enhanced machinability of aerospace materials using these routes, materials-focused explanation of the mechanisms underlying improvements has not been as well investigated. Fundamental understanding of the effects of these technologies and processing parameters on process performance is critical in the development and calibration of FE models used to predict, for example, elements pertaining to work surface integrity and tooling performance.

In the case of modulation-assisted machining, controlled modulation (or vibration) of the cutting tool is known to improve machining productivity through increased processing speeds

and enhanced tool life. The controlled tool motions in MAM have been applied in milling [1,2], turning [3,4,5], planing [4,6], grinding [7,8] and drilling [4,8,9,10,11]. While the precise mechanics depend on the specific modulation configuration, a common characteristic is periodic disruption of the tool-work contact. The benefits of controlled and repeated tool-work contact disruption are manifold. In drilling and turning configurations, periodic contact disruption leads to formation of discrete chips [4]. This is particularly important for deep-hole drilling applications in aerospace alloys, as continuous chip forms limit production rates even with the application of high-pressure cutting fluids typically applied to help break chips and clear the drilling zone [9]. The use of modulated tool paths has been shown to decrease friction coefficient, decrease average machining forces and lower work zone temperatures during cutting [4]. These effects are oft attributed to fundamental differences between thermo-mechanical response in modulation-assisted machining and conventional machining. These differences include, but are not limited to: lower yield stresses due to enhanced dislocation generation [13], reduced

friction due to enhanced contact lubrication [6], modified chip morphologies [4] and incipient strain in small-scale removal volumes [4,5,14].

While it is plausible that the underlying thermo-mechanical plastic response is modified by the presence of modulation or tool vibration, the modified distribution of machining parameters and its effects on power dissipation may itself be a major factor in the modified performance. For example, it is well known that power consumption in conventional machining is sensitive to changes in machining parameters such as cutting speed and undeformed chip thickness [15,16,17,18,19]. Such behaviors are important to consider in the case of modulated tool paths that alter the temporal nature of machining parameters that would otherwise be set to fixed values in conventional machining. The aim of this paper is to address how the character of functional relationships between measurable process responses (e.g., power) and controllable parameters (e.g., cutting velocity, feed/undeformed chip thickness) in conventional machining could be used to understand the role of modulated input parameters in affecting performance of modulation-assisted machining configurations. These results will be useful in calibrating predictive models for modulation-assisted machining of various aerospace alloys.

PARAMETER MODULATION IN MACHINING

Controlled application of modulation in machining processes can be applied in different orientations depending on the desired effect on the cutting process. The underlying mechanics of these processes have been discussed extensively in Refs. [2,4,10,20,21]. In the present work, we consider application of modulation for two cases: modulation parallel to feed (feed-modulation) and parallel to cutting velocity (velocity-modulation). The influence of controllable modulation and machining parameters on instantaneous/effective cutting parameters in chip formation is described for both of these cases.

The application of modulation involves periodic motion of the tool relative to the work at a specified modulation frequency (f_m) and amplitude (A). In velocity-modulation, the periodic motion occurs in the direction of the cutting velocity and produces a time-varying velocity ($V_m(t)$) that is superimposed on the steady cutting velocity (V_0), as depicted in Fig. 1. The effective displacement (D_{eff}) and effective velocity (V_{eff}) are functions of time and can be written in terms of time (t), the amplitude of modulation (A) and the modulation frequency (f_m) as:

$$D_{eff}(t) = V_0 t + A \sin(\omega t) \quad (1)$$

$$V_{eff}(t) = V_0 + V_m(t) = V_0 + \omega A \cos(\omega t) \quad (2)$$

The angular velocity is related to the modulation frequency as $\omega = 2\pi f_m$. From Eq. 2, the effective velocity takes values in the range $[V_0 - \omega A, V_0 + \omega A]$. It is clear that when $\omega A > V_0$, V_{eff} becomes negative during a single modulation cycle. This is referred to as a critical condition where the tool separates from the work and the contact is disrupted. The tool duty cycle (DC) is given by $DC = (1 - \Delta T) * f_m$, where $\Delta T = t_1 - t_2$ is the duration that the tool is disengaged from the work. The times corresponding to separation (t_1) and re-engagement (t_2) and the tool duty cycle can be determined by setting $V_{eff} = 0$ in Eq. 2, such that:

$$t_1 = \omega^{-1} \left[2\pi - \cos^{-1} \left(\frac{-V_0}{\omega A} \right) \right] \quad (3)$$

$$t_2 = \omega^{-1} \left[\cos^{-1} \left(\frac{-V_0}{\omega A} \right) \right] \quad (4)$$

$$DC = 1 - \pi^{-1} \cos^{-1} \left(\frac{-V_0}{\omega A} \right) \quad (5)$$

It is important to note that an expression for DC is also presented in Refs. [2,21], where duty cycle reaches zero as ωA increases. The result in Eq. 5 is substantially different, as the sinusoidal component of the time-varying velocity ($V_m(t)$) is not considered in Ref. [2,21]. In the present case, duty cycle does not approach zero due to the contribution of the cosine term.

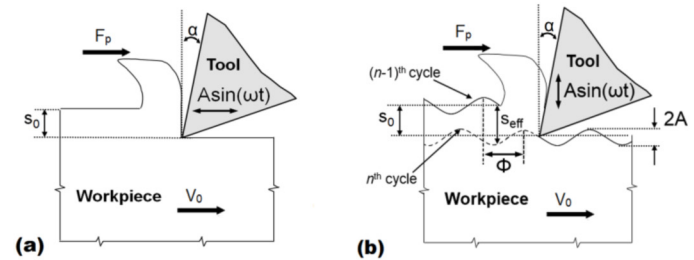


Figure 1. Orthogonal cutting geometry for (a) velocity-modulation and (b) feed-modulation ($0 < \phi < \pi$).

In feed-modulation configurations, the periodic motion occurs in the direction of undeformed chip thickness and produces a time-varying feed of the tool relative to the steady feed rate (s_0), as depicted in Fig. 1. Machining configurations with rotating workpieces or rotating tools (e.g., turning, drilling) typically employ feed-modulation due to the inherent difficulty involved in superimposing motion in the circumferential direction. While the rotational cutting velocity is essentially unchanged in feed-modulation, the periodic motion can influence the instantaneous value of the feed rate in different ways that depend on: (1) the phase difference between the modulation frequency (f_m) and the spindle rotation frequency (f_w) and (2) the relative magnitudes of the steady feed rate (s_0) and the modulation amplitude (A). The phase relationship between the modulation frequency and spindle rotation frequency is a primary factor in determining whether the undeformed chip thickness is constant or time-varying. It also determines the amplitude required for tool-work separation.

(e.g., critical condition). This phase angle (ϕ) is given by Eq. 6 and ranges over $[0, 2\pi]$, the resulting critical amplitude (A_{min}) is given by Eq. 7 and ranges over $(0.5s_0, \infty)$ [4].

$$\phi = 2\pi \left[\frac{f_m}{f_w} - \text{INT} \left(\frac{f_m}{f_w} \right) \right] \quad (6)$$

$$A_{min} = \frac{s_0}{2 \sin(\frac{\phi}{2})} \quad (7)$$

From Eq. 7, when $\phi=0$, successive machining passes are inphase and tool-work separation is impossible for any modulation amplitude (A). In contrast, when $\phi=\pi$, successive machining passes are perfectly out-of-phase and the amplitude necessary for tool-work separation occurs at a minimum of $A=0.5s_0$. Intermediate values of ϕ require increased amplitudes above this minimum. The effective undeformed chip thickness (s_{eff}) can be determined by taking the difference between successive machining passes given by Eq. 7 [4,20],

$$s_{eff}(t) = s_0 + s_m(t) = s_0 + 2A \cos \left(\omega t + \frac{\phi}{2} \right) \sin \left(\frac{\phi}{2} \right) \quad (8)$$

Detailed derivation of Eqs. 5, 6, 7 is provided in Refs. [4,20]. From Eq. 8, it is clear that effective undeformed chip thickness ranges from $[s_0-2A, s_0+2A]$ when both f_m and ϕ are non-zero. The tool duty cycle is again given by $DC=(1-\Delta T)f_m$, where $\Delta T=t_1-t_2$ is the duration that the tool is disengaged. The times corresponding to separation (t_1) and re-engagement (t_2) and the tool duty cycle can be solved analytically by setting $s_{eff}=0$ in Eq. 8, such that:

$$t_1 = \omega^{-1} \left[2\pi - \cos^{-1} \left(\frac{-s_0}{2A \sin(\phi/2)} \right) - \frac{\phi}{2} \right] \quad (9)$$

$$t_2 = \omega^{-1} \left[\cos^{-1} \left(\frac{-s_0}{2A \sin(\phi/2)} \right) - \frac{\phi}{2} \right] \quad (10)$$

$$DC = 1 - \pi^{-1} \left[\cos^{-1} \left(\frac{-s_0}{2A \sin(\phi/2)} \right) \right] \quad (11)$$

Eqs. 5 and 11 describe the relative duration of tool contact as a function of the modulation parameters in velocity-modulation and feed-modulation, respectively. Both have similar mathematical forms with a clear difference being inclusion of phase angle in the latter expression. Fig. 2 shows variation of tool duty cycle for velocity-modulation as function of $\omega A/V_0$ and for feed-modulation (at $\phi=\pi$) as a function of $2A/s_0$. Beyond the critical condition, the fraction of time the tool is engaged in cutting decreases asymptotically to 0.5. The notion that modulation can be used to effect lower tool duty cycles is an oft-cited reason for improvements vis-à-vis enhanced contact lubrication, lower machining temperatures and enhanced thermal energy dissipation [6,13,21,22]. While a lower duty

cycle can play a role in each of the above, it should also be considered that with decreased tool duty cycles, instantaneous machining parameters (e.g., $V_{eff}(t)$, $s_{eff}(t)$) take on maximum values far greater than the nominal settings (V_0 , s_0). In the ensuing, the effects that these variable machining parameters have on cutting forces and resulting power dissipation are studied.

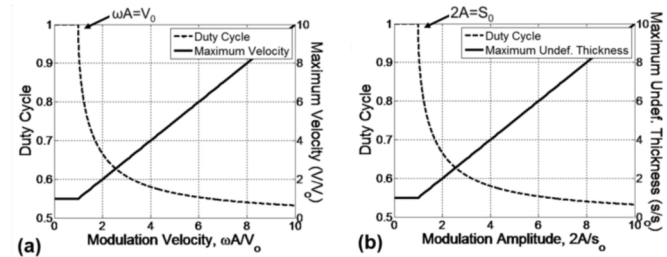


Figure 2. Duty cycle for (a) velocity-modulation and (b) feed-modulation ($\phi=\pi$).

EXPERIMENTAL

To study the effects of modulated cutting velocity, a two-stage motion platform for orthogonal cutting was utilized. The compound motion platform superimposes a time-varying displacement onto the linear motion of a workpiece. The linear motion of the workpiece (V_0) is carried out using a primary ball-screw linear bearing slide coupled to a closed-loop servo motor. The time-varying component of the motion ($V_m(t)$) was provided by a piezo-actuated linear bearing slide coupled to the primary ball-screw slide. This provided a platform to superimpose relative motion of the workpiece onto a primary linear motion. The cutting velocity (V_0) was monitored through a rotary encoder on the primary slide and the modulation velocity ($V_m(t)$) was monitored using a capacitance probe (Capacitex, HPC-40). The piezo-actuator was powered with a sinusoidal voltage by a waveform generator and power supply.

The cutting tool was a 12.7 mm $\times$ 25.4 mm $\times$ 177.8 mm uncoated HSS blank with an effective rake angle of 30° and relief angle of ~5°. The rake and flank surfaces were finish ground to produce a sharp cutting edge. The tool was mounted to a three-axis force transducer (Kistler, Model 9257A) with the cutting force F_p oriented as shown in Fig. 1. The encoder pulse counts, capacitance probe signals and force signals were acquired using simultaneous sampling at 10 kHz (NI cDAQ-9178). Synchronization of the channels was validated using equivalently-sourced signals in an offline test. AA6061-T6 workpieces were constructed from flat stock (20 mm $\times$ 30 mm $\times$ 3 mm). The undeformed chip thickness was 0.020 mm. Steady cutting velocities (V_0) of 0.05 to 1.00 mm/s were used to characterize forces as a function of velocity, with five trials per condition. A similar series of cutting tests were also conducted using the piezo-actuated slide at $f_m=10$ Hz, and $A=19$ V ($\omega A \sim 0.4$ mm/s) and the ball-screw speed at $V_0=0.50$ mm/s.