Development of a virtual sensor for the comparison of heat partitions in milling under cryogenic cooling lubrication and high-pressure cutting fluid supply

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Abstract

Manufacturing high precision and high performance parts in aerospace, automotive and medical industries often requires machining of difficult-to-cut materials such as titanium, nickel and hardened alloyed steel alloys. Low productivity and environmental damage are major problems in cutting these materials, which vitally require optimized cooling strategies. High-pressure cutting fluid supply (HP CF) and cryogenic cooling lubrication (CRYO CL) are two of the most effective cooling lubrication approaches to increase tool life, productivity and avoid scrap production. The scientific and knowledge-based application of HP CF and CRYO CL had a pivotal role in improving the machining of difficult-to-cut materials, specifically in milling processes. In this context, the quantification of the cooling and lubrication effect of HP CF and CRYO CL is essential in order to adapt to the fluctuating heat generation at the cutting zone. The novel concept of a soft sensor for the quantification of the cooling and lubrication effect in the milling process is presented in this paper. This soft sensor integrates force measurements and transient temperature data from the process with the help of a mechanical model as well as an inverse temperature model. These models elevate the measured force and temperature signals to heat flows and power in the thermodynamic domain enabling an energy balancing in the real milling application. A telemetry system was used to measure the transient temperature in the milling tool with embedded thermocouples when milling 42CrMo4 and Ti-6Al-4V in αβ and β conditions. This way the separated cooling versus lubrication effect of high-pressure cutting fluid supply and a single channel cryogenic cooling lubrication based on carbon dioxide (CO2) and oil is investigated and compared with dry machining at various cutting parameters and proceeding tool wear.

Keywords: Milling, Heat partition, Cryogenic cooling lubrication, High-pressure cutting fluid supply
1. Introduction and State-of-the-Art

Customer demands and regulations have driven innovation to generate alternative technologies to enhance environmental and economic as well as social performance of manufacturing activities leading to a sustainable global industrial progress [1]. In the context of machining processes, high productivity and lower environmental impact are expected. This equated to efficient use of resources such as energy, material and water as well as generating less waste in terms of coolants, materials and scrap products. It also can translate into improved economy of the machining processes [2] or the use of alternative additive or removal process, such as electrochemical (ECM-based), electrophysical (EDM-based) or photonic (Laser-/EDM-based) [3][4]. Beside the energy consumption, the efficient use of tooling equipment in particular refers to the reduction of tool and machine tool wear and the increase of their lifetimes as well as the reduction of other consumables such as coolants and lubricants.

During machining, the cutting zone experiences high mechanical stresses and thermal loads. The latter can lead to high-temperatures at the cutting zone with numerous adverse consequences on the cutting tools as well as the surface integrity of the machined workpieces [5][6]. Therefore, cutting fluids are commonly used to reduce heat generation by reducing friction and improve heat removal, improving machining performance. For instance, in Germany alone, 85,000 metric tons of lubricants for cutting and forming processes are used annually [7], accounting to up to 15-20% of the total manufacturing cost [5][8]. On the other hand, cutting fluids are regulated products and can cause health problems and damage the environment. Cutting fluids can cause occupational diseases such as dermatitis and asthma as well as genetic diseases [9].

Although there are already industrial cases with the complete removal of cooling strategies (dry machining), this is not the case for difficult-to-cut materials such as stainless steels, nickel and titanium based superalloys, where a considerable amount of heat must be effectively evacuated from the cutting zone. While it is not feasible to completely eradicate the use of cutting fluids, it is possible to maximize their efficiency or develop sustainable alternatives.

In these terms, high-pressure cutting fluid (HP CF) [10][11] and cryogenic cooling lubrication (CRYO CL) [12][13] have proven to be effective alternatives. The HP CF strategy uses cutting fluids at high pressures and flow rates focused towards the cutting edge in order to maximize their effectiveness [10]. CRYO CL, on the other hand, replaces water-based cutting fluids with a mix of oil and cryogens, combining lubrication with cooling effects derived through combined convection and phase transformations of the medium [12].

For both cooling lubrication technologies, a scientific understanding of their cooling lubrication mechanisms, thermal and tribological effects determines their exploitable potential for an efficient machining of difficult-to-cut materials [10][13]. An essential first step in this scientific analysis is the quantification of the cooling effect of HP CF and CRYO CL in relation to the dissipated heat from the cutting zone in order to maximizing metal-cutting performance and optimize tool designs [11][14]. Both scientific knowledge regarding the underlying mechanisms, as well as the practical quantification of cooling effect are mostly missing for milling applications. Measuring the temperatures under HP CF and CRYO CL conditions during the process is a significant challenge. The major limitation is rooted in the kinematics of milling, with tool rotation limiting the direct application of temperature sensors inside the cutting edges. Additionally, infrared thermal cameras and pyrometers are restricted in their accessibility to the relevant areas in the cutting zone and may be significantly disturbed by the cooling-lubrication media.
This paper presents an extended measuring methodology and its systematic application for evaluating cutting temperatures and heat partitions when milling Ti-6Al-4V titanium alloy as well as quenched and tempered 42CrMo4 steel under dry, HP CF and CO2 and oil cooling lubrication conditions at varying process parameters. A special telemetry system has been used to transmit temperature data from a thermocouple embedded inside a rotating milling tool. At the same time a synchronized piezoelectric dynamometer has been used to measure the cutting force components in order to calculate a mechanical process energy. These data were subsequently processed with an analytical transient temperature model based on Green’s functions in order to inversely estimate the heat flow as well as a heat partition into the cutting tool.

In the following subsections, a brief overview of the state of the art in HP CF and CRYO CL assisted machining is presented as well as previous work related to the quantification of cooling effect in machining. The state of the art finishes with a brief discussion of relevant temperature models and temperature measurement methodologies conducted in former publications for the milling process. The experimental setup is described in section 2. In section 3 the post processing of temperature and force signal is described. The results are presented in section 4 and a conclusion is given in section 5.

1.1. State-of-the-art in HP CF and CRYO CL

In HP CF focused jets of coolant, at pressures of up to 360 MPa [15], are used to suppress vapor barrier formation and improve chip segmentation and penetrability at the tool-chip interface [16][17]. HP CF has been reported to increase tool life by up to 700% when turning austenitic and ferritic steels [18] and even more was reported in case of machining advanced superalloys [19]. According to Klocke et al. [10], HP CF can reduce tool temperature by 25% when turning Ti-6Al-4V, and a similar result was observed for 42CrMo4+QT steel [20]. In contrast to turning, where pressure stages of more than 15 MPA are applicable [21], the rotary lead through and the more complex piping system in milling machines limit the industrial applicable pressure to 8 Mpa. Hence, within this work Pressure levels of more than 2.5 MPa are considered as HP CF, according to the original definition of Pigott and Colwell [22], which were the first investigation HP CF.

Conversely, milling steels under HP CF is strongly influenced by process parameters [23] as well as jet angles [11]. The influence of the HP CF on the cutting forces strongly depends on the impact point of the cutting fluid and the supply pressure. Below a supply pressure of p = 1000 bar, the exerted force of the cutting fluid jet is not sufficient to change the conditions of friction significantly. Therefore, the changes of the cutting force when using HP CF compared to flood cooling are relatively small [11][24].

An alternative to HP CF is CRYO CL. The latter is based on the synergetic combination of cryogenic machining and minimum quantity lubrication (MQL). Many researchers [25][26][27] reported that, in CRYO CL, the relative merits of both technologies might be harnessed. With CRYO CL, simultaneous application of oil droplets and a cryogen such as liquid nitrogen (LN2) or CO2 at the cutting edge suppressed adhesion and prevented diffusion resulting in extended tool life [9]. For instance, Shokrani et al. [13] registered more than 4 h tool life when machining Ti-6Al-4V at 90 m/min cutting speed under CRYO CL conditions. According to Pereira et al. [28], CRYO CL is the most suitable solution as a medium-short term substitute for hazardous cutting fluids in workshops. They also proposed a CRYO CL system with a CO2 internal Cryolubrication [28] and a CRYO CL nozzle optimized through CFD simulations [29]. In particular, they observed that CRYO CL enhanced surface integrity over dry machining [12], specifically with regards
to surface roughness in machining AISI 304 [9]. Allu et al. [30] observed that CRYO CL reduced cutting temperature, cutting force, and plastic deformation of the material. Although there is a number of reports explaining the increased tool life and machining performance as a result of CRYO CL and HP CF, there are minimal studies on the underlying physics of the enhancements. A possible reason for the missing explanations of effects is the restricted transparency of the actual cooling effect and its influence on the thermo-mechanical properties in the process. In addition, minimal effort has been reported to optimize the delivery and composition of these cooling-lubrication techniques.

1.2. Empirical investigation of cooling-lubrication effects in cutting processes

The three main tasks of cutting fluid are (i) the removal of process heat, (ii) the reduction of friction between tool, chip and workpiece and (iii) the removal of chips [31]. Due to different cutting fluids, volumetric flow rates, supply pressures and nozzle orientations, etc., the cooling and the lubricating effect between different cooling lubrication strategies and parameters varies significantly. Especially, when CRYO CL strategies are compared to conventional cooling lubrication strategies such as flood cooling or HP CF, clear differences in cooling and lubrication effects occur. The detailed understanding of the effects of different strategies allows to influence the alternating thermo-mechanical tool load. Therefore, it will be possible to realize an adaptive process and material specific strategy, which lead to an optimized tool wear behavior and productivity with the desired part quality. However, the interaction between the cooling lubrication strategies and the alternating thermo-mechanical tool load is not well established to realize a systematic and knowledge-based design of innovative cooling lubrication techniques. Previous work examined the cooling lubrication effect based on different approaches.

Krämer experimentally investigated the cooling effect of different cutting fluids and identified that the cooling effect is influenced by supply pressure, beam length and the impact angle [32]. The highest cooling effect was achieved with a high supply pressure, short nozzle distance and a small impact angle. The temperature difference between cutting fluid and the hot surface is decisive. Krämer concluded that LN$_2$ has the highest cooling effect per mass flow, followed by CO$_2$, water and air. In this work the cooling effect was not evaluated in form of a calculated heat partition but rather by its influence on transient temperature fields.

Rech et al. introduced a fast method to identify friction and heat partition models based on special tribometer tests [33]. Due to the special design of the parameter, the method allows to simulate wide ranges of contact pressures and sliding velocities. The friction and heat partition models, which use the experimental tribometer data, contribute to an improvement in simulation of thermal aspects in numerical cutting simulations.

Hriberšek et al. analyzed the thermal influence of LN$_2$ on workpiece material Inconel718 to solve the inverse problem for determining the surface heat transfer coefficient between LN$_2$ and the workpiece depending on the temperature difference [34]. Therefore, temperature measurements were performed in the workpiece material while the nozzle was moved with different speeds. Based on a fitting of the numerical simulation with the experimental data the characteristic of the heat transfer coefficient was determined. A maximum surface heat transfer coefficient of $h = 75000 \text{ W/(m}^2\text{K})$ was determined around a temperature difference of $\Delta T = 196 ^\circ\text{C}$.

Furthermore, Lequien et al. investigated the influence of LN$_2$ jet parameters on the temperature distribution of a Ti6Al4V plate and presented a methodology for determining the surface heat transfer coefficient between LN$_2$ and the plate [35]. Additionally, Lequien et al. analyzed the influence of LN$_2$ cooling on the tool temperatures and the cutting forces in interrupted cutting of TiAl6V4 alloy and compared it to dry machining and flood cooling [36].
Praetzas et al. measured the core temperature of the tool and active force in milling processes under flood cooling, CO$_2$, MQL and dry conditions [39]. The cooling lubrication strategy showed minor influence on active force and a major influence on the tool’s core temperature. Due to the best-balanced cooling performance, CO$_2$ conditions resulted in the highest tool life. All cutting fluids were supplied through external nozzles.

The review of published literature indicated that majority of the investigations are focused on continuous cutting processes e.g. turning. Only a few researchers performed investigations in analogy trails, which reproduce interrupted cutting conditions such as in milling experiments. In most cases a thermodynamic heat partition in the cutting process as well as the affiliated cooling effect was not quantified and separated from the lubrication effect lowering the entire total dissipated energy in the process. Thus, a main deficit can be seen in the lacking integration of the thermal and mechanical domains of the cutting process, which is necessary for a comprehensive understanding. Preferably, such an integration should be constituted by first principle physics-based modelling using data provided by in situ process state measurements for forces and temperatures. The overall literature study (which included much more than the cited publications) shows for the majority of the experimental investigations in the past the following drawbacks:

- Most of them do not calculate a real heat partition into the tool as heat flow into the tool related to the total cutting power. (This makes the interpretation more difficult and less comparable)
- If heat partitions were calculated, then typically the heat partition into the workpiece.
- They are in most cases restricted to dry cutting conditions.
- If cooling effects were considered, not real cutting process but rather simplified experiments were carried out.
- This cooling effect was often not calculated as a change in heat partition, which is most interesting, but rather as an effect on temperature.
- The cooling effect was typically not separated from a lubrication effect.

In this work, the approach was based on a temperature measurement by tool integrated thermocouple as well as a Green’s function model [40] adopted to investigate the effects of various cooling lubrication strategies and process parameters in the milling process. The measured cutting temperatures together with measured cutting forces have been used to analytically determine the heat flow into the cutting tool and workpiece for various CL strategies. In addition, the cooling effect can be resolved and separated from the lubrication effect by calculating a heat partition into the tool in relation to the process power. The following section 1.3 describes the analytical basis for the model of transient temperatures within the tool.

### 1.3. Modelling transient temperature fields in the milling process by means of Green’s functions

Stephenson and Ali [40] first used an analytical model based on the Green’s function approximating the tool through a semi-infinite corner for interrupted cutting. The model was validated by them with the tool-workpiece thermocouple method for interrupted cutting with carbide and high speed steel tools of cast iron BHN 200-230 and aluminum 2024T351 cylinders. The point transient temperatures in the tool during interrupted cutting was modelled by approaching the tool geometry with a semi-end rectangular corner. For this corner, the temperature rise can be obtained by a momentary point source at the time $\tau$ at the surface point $y = y_p$, $z = z_p$, $x = 0$ assuming adiabatic conditions at the boundaries by multiplying the Green’s function for three perpendicular momentary plane sources in semi-undefined half-spaces intersecting an eighth space or an eighth corner, as shown in Figure 1.
The \( y, z \) plane represents the rake face of the tool, where the planar heat source, which extends over the contact length \( L_y \) of the rake tool and the cutting depth \( L_z \), is applied. The clearance surface is represented by the \( x, z \) plane and the side surface of the tool by the \( x, y \) plane. Thus, the temperature field \( T(x, y, z, t) \) at a point \( x, y, z \) in the corner at time \( t \) can be represented as a solution of [40]:

\[
\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} = \frac{1}{a} \frac{\partial T}{\partial t}
\]

\[-\lambda \frac{\partial T}{\partial x} = \dot{q}''(y, z, t) \quad 0 \leq y \leq L_y; \quad 0 \leq z \leq L_z; \quad x = 0
\]

\[
T(x, y, z, t) = \frac{a}{\lambda} \int_0^t \int_0^{L_y} \int_0^{L_z} \theta_G (x, y, z, t, x_p = 0, y_p, z_p, D) \cdot \dot{q}''(y_p, z_p, \tau) \, dy_p \, dz_p \, d\tau
\]

The Green’s function of the semi-infinite corner is [40]:

\[
\theta_G (x, y, z, y_p, z_p, 0, D) = \frac{2}{(\sqrt{\pi} \cdot D)^3} \cdot \exp \left( \frac{-x^2}{D^2} \right) \cdot \left[ \exp \left( \frac{-(y + y_p)^2}{D^2} \right) + \exp \left( \frac{-(y - y_p)^2}{D^2} \right) \right]
\]

\[
\cdot \left[ \exp \left( \frac{-(z + z_p)^2}{D^2} \right) + \exp \left( \frac{-(z - z_p)^2}{D^2} \right) \right]
\]

\[
D = 2 \cdot \sqrt{a(t - \tau)}.
\]

The term \( \dot{q}''(y_p, z_p, \tau) \) describes the spatial and temporal distribution of the heat flow of the instantaneous, point heat sources. The size \( L_y, L_z \) and the temporal \( \tau \) distribution of the heat source are of particular importance for the resulting temperature field, especially for the maximum temperature generated. For a spatially uniform heat source, the Green’s function \( \theta_G (x, y, z, t, y_p, z_p, 0, D) \) can be approximated for a momentary heat pulse over \( 0 \leq y \leq L_y; \quad 0 \leq z \leq L_z; \quad x = 0 \) by
\[
\theta_G(x, y, z, L_x, L_y, D) = \int_0^{L_y} \int_0^{L_x} \theta_G(x, y, z, y_p, z_p, 0, D) \cdot dy_p dz_p \\
= \frac{1}{2 \sqrt{\pi} \cdot D} \cdot \exp \left( \frac{-x^2}{D^2} \right) \cdot \theta_{GU}(y, L_y, D) \cdot \theta_{GU}(z, L_z, D)
\]

\[
\theta_{GU}(u, L, D) = \text{erf} \left( \frac{L+u}{D} \right) + \text{erf} \left( \frac{L-u}{D} \right).
\]

In their investigations, Stephenson and Ali examined the influence of uniform, linear and exponential heat source distributions along the chip-tool interface as well as time-variable heat source intensities in the form of rectangular heat inputs. Sato et al. also used the Green’s function model in order to predict punctual temperatures in the milling tool [41], which they validated by a tool integrated ratio pyrometer measurement setup [42] in a distance from 0.1 to 0.5 mm to the rake face. For the spatial heat flux distribution over time a varying tool-chip contact length was approximated by an analytical function of the cutter rotation angle and the undeformed chip thickness. The calculation of the heat flux per unit area at the rake face was calculated by the measured cutting force and assumed as uniformly distributed. The heat partition ratio of the heat flux into the tool \(B_{tool}\) was determined experimentally by comparing the calculated peak temperature to the measured peak temperature at a depth \(z = 0.1\) mm. The ratios were given as 12.2% and 12.1% in up and down milling respectively.

Cui et al., computationally and analytically investigated the impact of feed rate and depth of cut on cutting temperature in face milling and identified that heat flux reduces as the tool-chip contact area increase by increasing the uncut chip thickness [37]. Karaguzel et al. developed a model for cutting temperature in face milling using Green’s function and assuming that all the energy used for cutting is transformed into heat at the cutting zone [38]. They identified a heat partition into the tool in relation to the measured cutting force of roughly about 10%.

Augspurger [43] used the Green’s function model in order to simulate volumetric transient temperature in the tool for dry orthogonal milling process validated by infrared camera. However, these investigations were restricted to a dry solely circumferential milling processes with single tooth cutter tools. The tools without helix angle carried out simplified milling kinematics enabling an observation of the planar transient temperature field of the tool’s lateral side for an extended validation of the Green’s function model in the two-dimensional space with thermography. Core of the work was the modelling of transient heat source intensity depending of process parameters and not the modification of the Green’s function model itself. There he empirically and analytically determined heat partitions into the tool between 5 and 20% increasing with cutting velocity for cutting AISi 1045 and Inconel 718. The experimental approach was further developed [44] in order to use the data generated by thermocouples integrated into cutting tool inserts. In this case a milling cutter with inserts for the industrial application was used to carry out a more realistic manufacturing process, however also under dry conditions since a ratio pyrometer was used to measure the temperature of the chips front side and estimate its heat partition in relation to the measured cutting force. The goal of the experiment was the inverse iteration of the heat flow into the tool and into the chip mainly depending on the cutting velocity. The heat flow into the tool was inversely iterated by comparing modelled transient temperature trends with the modelled ones similar to this publication. The focus of the cooling effect Augspurger et al. [45] demonstrated a methodology, which uses the analytical model combined with tool integrated thermocouples in order to estimate the heat flow and heat partition into the tool under CRYO CL. The process was carried out under realistic condition with a face milling cutter. However, the method on the analytical side did not include the transient time series of force signals as the transient boundary
condition for the Green’s function model and thus did not close the loop between acquired process data and comprehensive thermodynamic energy balancing following a first principle approach as desired. Finally, it was demonstrated that cooling effects can be taken into account by the model and the cooling efficiency may be estimated. The results of the mentioned publications imply a principal usability of the Green’s function model in order to model transient temperature fields in the milling cutter, which were mainly validated by tool-integrated punctual temperature measurements. However, a systematic investigation about the usability of the model combined with integrated temperature measurement in order to estimate heat partitions under process parameters, different cooling conditions and strategies was not yet carried out.

This paper builds on the mentioned previous works by using the Green’s functions temperature model however does not validate or further develop the model. Our approach rather uses the already validated model in order to estimate the heat flow into the tool in relation to the governing mechanic process power. I contrast to the former investigations this process power was derived from transient, transformed force measurements as well as the affiliated cutting velocity and then to a partition $B_{tool}$ fed into the Green’s function model as transient tool heat input $q''(y_p, z_p, \tau)$. Therefore, the temporal fluctuations of the heat source affecting the tool are considered in a realistic manner for an industrial milling process with its affiliated cooling conditions. By comparing the measured mean temperature in the tool at a steady state milling process and the tool integrated temperature measurement $B_{tool}$ was iteratively estimated under a broad and systematic variation of cooling strategies, cooling and process parameters as well as material variations. Thus, the development of a novel methodology of a soft sensor and their systematic practical application in order quantify the heat flow into the tool in relation to the mechanic process power under different realistic cooling conditions is the focus. The novelty of this methodology in contrast to the described existing observers in the state of the art is the full analytical and empirical integration of the energy transformation process between measured transient force, transient heat flows and temperature fields in the tool. This enables firstly the systematic investigation of cooling effects under real cutting conditions and their separation form lubrication effects for different cooling strategies and process parameters. The term soft sensor in this context describes a measurement setup of one or multiple sensors, which further uses models to transform, elevate or integrate measured signals for a superior interpretation in the physical domain. In this case force and temperature measurements were used in the Green’s function models to calculate mechanical process power, heat flows and the heat partitioning in the milling process.

2. Experimental setup

The following section describes the experimental setup for the analysis of the effect of process parameters as well as cooling environment on the resulting thermo-mechanical conditions in the process.

2.1. Machining setup

For the machining experiments an indexable milling cutter of the type Cryo.tec® F4042.6903229 provided by Walter was used. A thermocouple has been integrated into the tool holder to measure tool temperature during milling experiments. MT-CVD Ti(C,N) and Al₂O₃ multilayer coated tungsten carbide cutting inserts of company Walter Tools with designated number ADMT120408R-F-56 and WSM45X Tiger·Tec Silver® tool material were used. The milling cutter is depicted in Figure 2 and the tool specifications are summarized in Table 1:
Table 1: Cutting tool specification

<table>
<thead>
<tr>
<th>Tool specifications</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Milling cutter</td>
<td>Cryo.tec® F4042.6903229 Walter Tools</td>
</tr>
<tr>
<td>Number of cutting edges</td>
<td>z = 4</td>
</tr>
<tr>
<td>Cutter diameter</td>
<td>D = 40 mm</td>
</tr>
<tr>
<td>Insert type</td>
<td>ADMT120408R-F56</td>
</tr>
<tr>
<td>Cutting material</td>
<td>Cemented carbide with a MT-CVD-Multilayer Ti(C,N) and Al2O3 coating</td>
</tr>
</tbody>
</table>

Three workpiece materials have been investigated: (i) quenched and tempered 42CrMo4 steel, and titanium alloy Ti-6Al-4V in (ii) αβ-condition and in (iii) β-condition. Microstructure of the materials used for this investigation are depicted in Figure 3.

In addition to the workpiece material, cutting speed and machining environment were also investigated. Two levels of cutting speed were used for each material to identify the impact of cutting speed on temperature distribution. The process parameters used for each material are shown in Table 2. Each experiment was conducted using three machining environments of (i) CRYO CL, (ii) HP CF and (iii) dry cutting. Each machining experiment was repeated in order to ensure statistical validity of the results.

Table 2. Process parameters

<table>
<thead>
<tr>
<th>Material</th>
<th>v_c (m/min)</th>
<th>f_z (mm)</th>
<th>a_p (mm)</th>
<th>a_e (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>42CrMo4</td>
<td>100, 150</td>
<td>0.2</td>
<td>3</td>
<td>15</td>
</tr>
<tr>
<td>Ti6Al4V αβ</td>
<td>40, 60</td>
<td>0.14</td>
<td>2.5</td>
<td>15</td>
</tr>
<tr>
<td>Ti6Al4V β</td>
<td>40, 60</td>
<td>0.14</td>
<td>2.5</td>
<td>15</td>
</tr>
</tbody>
</table>
2.2. **HP CF and CRYO CL setup**

The machining environments used in this research were (i) high-pressure cutting fluid supply (HP CF), (ii) cryogenic cooling lubrication based on a single channel supply of liquid carbon dioxide and liquid oil (CRYOCL) and (iii) dry machining (DRY) at varying parameters.

A tailor-made tool holder of the type HSK-A63 with two separated channels was provided by the companies Haimer and Walter Tools and used in the experiments similar to [45]. One channel was used to supply the mixture of CO\(_\text{2}\) and oil during the experiments with cryogenic cooling lubrication. The other channel was used to supply cutting fluid in the experiments with HP CF.

Water-based cutting fluid (TU545, Rhenus Lub) was delivered into the cutting zone through the tool holder and an exit orifice with \(d = 1.3\) mm diameter. The cooling lubrication parameters for HP CF are presented in Table 3. An industrial relevant maximum pressure of \(p = 50\) bar was set, because significantly higher pressure stages need custom-made supply units that causes low acceptance from manufacturing industry [46].

<table>
<thead>
<tr>
<th>High-pressure cutting fluid supply</th>
<th>10, 30, 50</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cutting fluid supply pressure (p) in bar</td>
<td>10, 30, 50</td>
</tr>
<tr>
<td>Diameter of the cutting fluid exit (d) in mm</td>
<td>1.3</td>
</tr>
<tr>
<td>Volumetric flow rates (Q_{HP}) in l/min</td>
<td>9.0, 16.9, 21.9</td>
</tr>
<tr>
<td>Designation of water-based cutting fluid</td>
<td>Rhenus TU545</td>
</tr>
</tbody>
</table>

Table 3: Cooling lubrication parameters – High-pressure-cutting fluid supply

For CRYO CL, the mass flow rate of the liquid CO\(_2\) (\(\dot{m}_{CO2}\)) and the volumetric flow rate of the oil (\(Q_{oil}\)) served as variables as shown in Table 4. The \(\dot{m}_{CO2}\) was adapted with an Aerosolmaster 4000cryolub\textsuperscript{®} (Rother Technology) connected to a pressurized CO\(_2\) bottle with a riser pipe to allow a supply of liquid CO\(_2\) under a pressure of \(p_{CO2} = 57\) bar at room temperature. The flowrate of CO\(_2\) was adjusted off-line by monitoring the CO\(_2\) bottle’s weight. A Hawe Hydrauliks HC pump delivered the lubricating oil (LA722199, Rhenus Lub) at \(p_{oil} = 65\) bar with a T-connected highspeed valve to the CO\(_2\) tubing. The amount and flow of the oil into the stream of liquid CO\(_2\) could be controlled by the opening frequency and duration of the high speed-valve. A schematic layout of the system is shown in Figure 4. This LA722199 oil selected for this study has shown good solubility in liquid CO\(_2\) in previous investigations [47][48]. A good solubility enhances atomization of the oil at the exit [49] where the CO\(_2\) changes the phase from liquid to gaseous and solid. During the expansion, the temperature decreases to a minimum of -78 °C.

Table 4: Cooling lubrication parameters – CO\(_2\) and oil

<table>
<thead>
<tr>
<th>CO(_2) and oil</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Liquid CO(<em>2) supply pressure (p</em>{CO2}) in bar</td>
<td>57</td>
</tr>
<tr>
<td>Diameter of the CO(_2) exit (d) in mm</td>
<td>1</td>
</tr>
<tr>
<td>Mass flow rate of the liquid CO(_2) in g/min</td>
<td>170, 200, 230</td>
</tr>
<tr>
<td>Volumetric flow rate of the oil in ml/h</td>
<td>20, 40, 60</td>
</tr>
<tr>
<td>Designation of the oil</td>
<td>Rhenus LA722199</td>
</tr>
</tbody>
</table>
Offline measurement of the volumetric oil flow rate was realized with a mixing chamber made of acrylic glass. For the measurement, the chamber was connected to a CO\textsubscript{2} cylinder to apply the CO\textsubscript{2}-pressure. Simultaneously oil was delivered into the chamber until a defined volume of the chamber was filled. Then the volumetric oil flow rate was calculated considering the required filling time.

An Ex-Cell-O XHC 241 four axis horizontal CNC machining center was used for milling experiments. It offers a maximum spindle power of P = 40 kW, a maximum spindle speed of n\textsubscript{max} = 24,000 min\textsuperscript{-1} and a maximum spindle torque of T\textsubscript{max} = 54 Nm. The machine tool is equipped with an Ott-Jakob Spanntechnik rotary union suitable for an internal supply of liquid CO\textsubscript{2} and minimum quantity lubrication (MQL) through the spindle via separate channels. For this study, oil dispersed through CO\textsubscript{2} was delivered through a single channel.

2.3. Sensors and data acquisition system

The tool holder was equipped with a wireless FM/PCM transmitter (Manner Sensortelemetrie) to transfer the analogous signal generated by a K-type thermocouple. The diameter of the thermocouple was d = 0.5 mm positioned inside the cutting insert, as shown in Figure 6. To reduce the transmission losses, the measured thermoelectric voltage was amplified to and transmitted at a radio frequency of f = 13.56 MHz. A data acquisition system collected the temperature measurement data at a sampling rate of f = 6620 Hz. Table 5 displays the specifications of the temperature measurement system:

<table>
<thead>
<tr>
<th>Temperature measurement</th>
<th>Type K (NiCr-Ni)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Embedded thermocouple</td>
<td>FM/PCM Transmitter</td>
</tr>
<tr>
<td>Telemetry system</td>
<td></td>
</tr>
<tr>
<td>Data transfer frequency in MHz</td>
<td>13.56</td>
</tr>
<tr>
<td>Sample rate in Hz</td>
<td>6620</td>
</tr>
<tr>
<td>Measurement range in °C</td>
<td>0-900</td>
</tr>
</tbody>
</table>
The cutting force components were measured using a 9255b Kistler piezoelectric three component dynamometer with a sampling rate of $f = 5 \text{ kHz}$ together with a 5019 Kistler charge amplifier.

3. Post processing of temperature and force signals

The measured temperature trends and the corresponding force signals were processed using physical models in order to inversely quantify the heat flow into the insert of the milling cutter under the specific process and cooling-lubrication parameters.

As shown in Figure 5, the methodology follows an inverse approach by modelling the transient temperature trends in the tool at the position of the thermocouple and comparing them with the measured temperatures, particularly with the virtually steady state condition. The mechanical process power was the principal heat source, which is transformed into heat at the cutting zone. A partition $B_{\text{tool}}$ of this process power is thus conducted into the tool as a transient heat flow $Q_{\text{tool}}(t)$:

$$
\dot{Q}_{\text{tool}}(t) = B_{\text{tool}} \cdot P(t) = B_{\text{tool}} \cdot F_{c,\text{measured}}(t) \cdot v_c
$$

According to Figure 5 (c), the mechanical process power $P(t)$ was calculated on the basis of the transient Cartesian force components $F_{x,y,z}$, shown in Figure 5 (a), which were transformed geometrically with the cutter rotation angle $\Phi$ into the cutting force $F_c$ tangential to the cutter, shown in Figure 5 (b), and cutting normal force $F_{cn}$.

The cutter rotation angle $\Phi$ was estimated by a peak detection algorithm. This detected the maximum peaks of the force signal $F_y$ and defined it for each engagement as cutter rotation entry angle $\Phi_{\text{entry}}$ geometrical given by:

$$
\Phi_{\text{entry}} = \pi - \cos(d/2 - ae)/(d/2)
$$

The heat flow into the tool $Q_{\text{tool}}$ was used as input into the analytical transient temperature model for the semi-infinite corner given by Equation (2) in form of a spatial uniform heat flux given by:

$$
q''(y_p,x_p) = \dot{Q}_{\text{tool}}(t)/L_y \cdot L_z
L_y = a_p, L_z = 0.5 \text{ mm}
$$

The heat source’s planar area was set to the depth of cut $a_p$ multiplied by the estimated tool-chip contact length of 0.5 mm. This value in reality changes with the undeformed chip thickness during the cutting engagement, however is of minor importance since the order is a realistic assumption as previous investigations have shown [45][43]. However, of major importance is the correct quantification of the temporal heat flux, which is assured by using the force measurements multiplied with $B_{\text{tool}}$ as model input.

The Green’s function $\Theta G$ for the spatial uniform heat source was chosen according to Equation (5). The position of the temperature simulation as well as the real position of the thermocouple (Figure 6) were $y, z = 0.8 \text{ mm}$ and $x = 0.17 \text{ mm}$. The thermal diffusivity and heat conductivity amounted to $\alpha = 2 \cdot 10^{-5} \text{ m}^2/\text{s}$ and $\lambda = 65 \text{ W/mK}$, respectively as typical thermal properties of tungsten carbide. Another assumption of the analytical model was the homogenous tool
medium not considering a coating as it was used in the experiments. This might cause some differences between modelled and real temperature field in direct proximity to the rake face and to some degree an underestimation of the heat flow into the tool. However the influence at the measurement and simulation point at \( x = 0.17, y = 0.8, z = 0.8 \) is minimal and in particular since the steady state temperature levels are used for the fitting. In this steady state, the temperature rise was able to penetrate into the tool.

Advantages of thermocouples are their high precision, simple application and broad temperature range from -100 °C to 1200 °C. However, a disadvantage is the restricted reaction time and varying contact conditions to their surroundings, limiting their use in transient measurement processes such as milling. Thus, the behavior of the thermocouple was simulated considering a PT1-behaviour for a meaningful comparison between the analytical simulation and the measurement [45]. The corresponding functional relationship in the time domain can be modelled using differential equations shown in Eq 10:

\[
T \cdot \dot{y}(t) + y(t) = K u(t)
\]  

In this case \( K, K > 0 \), designates the transmission constant or the gain factor and \( T > 0 \), the time constant. The variable \( u(t) \) is the input and \( y(t) \) the output. For the signal postprocessing, the time discrete form was used with \( \Delta t \) as the scanning increment:

Figure 5: Methodology for the data processing: (a) measured raw data of force signals, (b) measured raw data of force signals, detected cutter rotation angle and transformed cutting force time series, (c) calculated transient heat flow into the tool as a fraction \( B_{\text{tool}} \) of the calculated transient mechanical cutting power, (d) time series of the punctual measured thermocouple temperature as well as transient simulated temperature trend (both compared by their arithmetic mean \( T_{\text{tool}} \) )

\[
T \cdot \dot{y}(t) + y(t) = K u(t)
\]
\[ y_n = \frac{1}{T \Delta t + 1} \left[ K u_n + \frac{T}{\Delta t} \cdot y_{n-1} \right] \]  

A value of \( T = 5 \, s \) for the thermocouple reaction time and a gain factor \( K = 1 \) was assumed. These values are subjected to high uncertainty, in particular under wet conditions, as they depend on the reaction time of the thermocouple itself as well as the contact conditions of the measurement wire within the blind-hole of the tool [45]. The parameters \( T \) and \( K \) were identified by adjusting them until the mean temperature trend in the heating and cooling phase as well as the temperature fluctuation amplitude were similar between measurement and simulation. However, in this case, the long term trend and virtually steady state conditions are considered and therefore, accurate quantification of the variation in \( T \) due to engagement and disengagement of the cutter is of minor importance and has minimal impact on the analysis. Therefore, the empirical analysis or correct modelling of the short time temperature fluctuations were not the focus of this approach. These steady states occurred for each experiment and characterized a process condition where the tool temperature fluctuated around a mean value due to tool engagement and disengagement but did not rise globally as shown in Figure 5 (d). For each measurement, this steady state was the evaluation basis in order to compare the mean measured and mean simulated temperature \( T_{\text{tool}} \) and iteratively determine the heat partition into the tool \( B_{\text{tool}} \). This factor was changed manually and iteratively until the deviation of mean simulated and mean modelled steady state temperature \( \Delta T_{m} \) was less than 10\%, see Figure 5 (d). No optimization algorithm was used.

For the iterative determination of the heat flow into the tool a time window of 10-50 s (depending on the cutting velocity) during the steady state was extracted from the temperature and force measurements. Thus, the simulated heat flow lasted for a duration of 10-50 s considering a virtually steady state of engagement conditions in deviation to the actual measured temperature which followed a heating phase characterized by varying geometrical tool engagement conditions while approaching the workpiece as well as a longer thermal and mechanical steady state as shown in Figure 5 (d). Thus, the initial engagement of the tool with the workpiece during the heating up period was removed from the analysis. This is due to the fact that during approaching the workpiece, the tool is not fully engaged with the workpiece, resulting in false peaks in cutting forces. Therefore, measurement and simulation were compared in their steady state with regards to their arithmetic mean values \( T_{\text{tool}} \) of the time series regardless of when they occur in the overall course as shown in Error! Reference source not found. (d). The initial tool temperature in the model was in all cases assumed to be 20°C, however is of minor importance since model and measurement were matched in the steady state.

The force and temperature signals were resampled at a factor of 4 to reduce computational requirements and a lowpass filter with a cut-off of 100 Hz was used to denoise the force signal. The force was approximated by linear piecewise regression in order to provide a function of the transient heat source as input for the temperature model.

By simulating the heat flow into a tool as partition of the measured cutting power, the approach per definition does use a pulsating positive heat source, which occurs during the cutting phase of the milling process. During the cutting interruption, the simulated heat flow into the tools drops to zero together with the process power. However, this is a necessary simplification of the expected real conditions. Instantly after the cutting engagement, which is characterized by a positive heat flow into the tool due to chip and work piece friction, in reality a negative heat flow occurs at the tool due to the cutting fluid. This heat flow decreases during the cutting interruption phase because of the decreasing temperature gradient between convective medium and the tool. Henceforth, the assumption of a constantly negative
heat flow acting on the tool may also not apply. As a result, the transient heat flow should be quantified as a complex problem of the tool temperature itself constituting convective heat transfer together with the cooling medium properties. So in this case the model considers the cooling effect, but not in a temporarily corrected resolved manner. The model assumes the tool is only heated and cooled during the cutting phase. This combined heating and cooling is summarized in the $B_{\text{tool}}$ factor. So, the tool is less heated under cooling lubrication conditions compared to dry conditions, but still heated positive and never heated negative. These assumptions seem valid since we do not observe the short time temperature fluctuations in the tool but compare the long time heating trend of the tool in order to inversely determine $B_{\text{tool}}$. So, we basically determine a mean $B_{\text{tool}}$ over a cutter rotation.

4. Analysis of the results and discussion

The following section explains and discusses the results for the three investigated materials namely 42CrMo4, Ti6Al-4V in $\alpha \beta$-condition and $\beta$-condition under different cooling strategies. Figure 7 shows the results for the work piece material 42CrMo4 and the process parameters $f_z = 0.2$ mm, $a_p = 3$ mm, $a_e = 15$ mm. It provides the measured mean cutting force $F_c$ (over one revolution), the measured mean tool temperature in the steady state $T_{\text{tool}}$, the mean heat flow into the tool (over one revolution) $Q_{\text{tool}}$ and the affiliated heat partition into the tool $B_{\text{tool}}$ in relation to the process power $P$. In contrast to the process power, the cutting force offers comparability between different cutting velocities and is thus preferred for the analysis of the lubrication efficiency of the used medium. On the other hand, the measured mean tool temperature in the steady state $T_{\text{tool}}$ and the heat flow into the tool $Q_{\text{tool}}$ reflect the cooling efficiency of the medium. The heat partition into the tool is with 10 % the highest for dry conditions as can be expected and significantly higher compared to HP CF at levels below 1 %. A similar value was observed by [38], however for 50 m/min and steel 1050. For CRYO CL the heat partition into the tool seems much higher with 3.25 % compared to HP CF.

Figure 7: Results for the work piece material 42CrMo4, feed per tooth $f_z = 0.2$ mm, $a_p = 3$ mm, $a_e = 15$ mm
The cutting forces rise slightly with reduced cutting speed for HP CF. This might be due to the reduced temperature increase in the cutting zone due to reduced cutting speed as an effect of the relation between heat source velocity, which is feed velocity, and thermal diffusivity. This effect occurs for a moving heat source in a medium with defined thermal diffusivity and was also observed previously [54]. Moreover, the influence of the HP CF pressure on the conditions of friction in the contact zone is assumed to be significant at pressure levels above $p = 1000$ bar, according to the state of the art [11][24]. In this sense, the drastically decreasing cutting forces for dry conditions are explained, as an effect of significantly rising workpiece temperatures and thermal softening of the materials strength and thus specific cutting forces apart from the conditions of friction in the contact zone. This results in lower mechanical stresses on the tool in dry conditions. However, the cutting tool will experience higher temperature cycles and thermal loads.

For CRYO CL, the cutting forces are comparable to HP CF, while the mean tool temperature in the steady state $T_{\text{tool}}$ is significantly higher compared to HP CF, so is the heat flow and partition into the tool. These results may be explained by the far lower flow rates of CO$_2$ and oil compared to HP CF. The analysis of the mean tool temperature, heat flow and heat partition for HP CF shows a decreasing, significant effect with rising coolant pressure. However, the absolute effect is low. Coolant pressure in this case is accompanied by an increase of volumetric flow rate, given constant nozzle diameter. For dry conditions, the tool temperature at the measurement position of the thermocouple was highest, however only slightly higher compared to CRYO CL. This implies a relatively low absolute global cooling effect on the tool of the investigated CO$_2$ and oil based cryogenic cooling lubrication strategy compared to HP CF for these specific boundary conditions. However, local cooling effects at the tool might be still higher for CRYO CL. For practical industrial application of cryogenic cooling, the question arises, if higher temperature cycles and thermal shocks need to be expected locally. These might cause comb cracks in particular in the moment of cutting edge disengagement, where the hot cutting edge enters the coolant flow.

The results show the importance of the cooling strategy and its parameters in relation to the process parameters as well as cutting materials. The cooling parameters may contradictorily influence the workpiece temperature and thus indirectly cutting forces as well as the tool temperature. A proper balance may possibly lower the cutting forces by permitting a certain degree of thermal softening of the workpiece and at the same time preventing overheating of the tool. This way mechanical as well as thermal damaging mechanism of the tool can be potentially minimized. However, an increasing pressure of the HP CF does not seem to permit thermal softening of the workpiece to a significant level. This is because the effective cooling flow surmounts the dissipated generated heat at the cutting zone and thus focuses on preliminary preventing thermal damage of the tool. Similar observations have been reported by Klocke et al. [10]. However, when comparing flood cooling, where the cutting fluid is applied over the cutting zone to the focused high-pressure cutting fluid supply, the cutting forces are lower for HP CF compared to flood cooling according to reported findings [16]. Consequently, the determination of suitable setting parameters for the supply pressure and volumetric flow rate may be under similar boundary conditions as those investigated in this publication of superordinate importance for HP CF. Therefore, a conflict of objectives can be identified, where a balance of the sufficient supply pressure and flow rate is the goal. It must be considered that lower supply pressures lead to higher temperatures that allow thermal softening in order to lower the mechanically induced tool wear, whereas the thermally induced wear increases simultaneously.
On the other hand, cryogenic based cooling strategies seem to dispose a global effective cooling in a lower magnitude and may permit thermal softening of the workpiece to a certain degree while sufficiently cooling the tool.

Figure 8 shows the results for the work piece material Ti-6Al-4V αβ-condition, feed per tooth \( f_z = 0.14 \text{ mm} \), \( a_p = 2.5 \text{ mm} \), \( a_e = 15 \text{ mm} \). It compares different cooling strategies with dry conditions under different cooling and process parameters as well as proceeding tool wear (as land wear VB in \( \mu \text{m} \)) under dry conditions. The evaluation variables are cutting force \( F_c \), mean tool temperature \( T_{\text{tool}} \), heat flow \( Q_{\text{tool}} \) and heat partition \( B_{\text{tool}} \) into the tool.

The analysis of the measured cutting forces shows no significant influence of the cooling pressure for HP CF. However, they drastically decrease under CRYO CL and dry conditions. This is potentially due to the reduced cooling effect on the workpiece resulting in elevated temperatures at the cutting zone. This can result in thermal softening of the workpiece material and reduced specific cutting forces. However, the lowering effect on the tool temperature is significant, compared to dry conditions. Similar observations regarding temperature were documented in previous studies [39]. The same effect was observed under dry conditions for 42CrMo4, but not for CRYO CL conditions. An explanation can be the different thermal diffusivities of the two materials, which are approximately 13 mm\(^2\)/s for steel and 3 mm\(^2\)/s for Ti-6Al-4V [54]. In this sense, the thermal softening effect is more distinct for Ti-6Al-4V [56] increasing the relevance of cooling at the cutting zone, which is lower for CRYO CL compared to HP CF. This is also indicated by the higher mean temperatures in the tool as shown in Figure 9. The impact of temperature and cooling on workpiece thermal softening at the cutting zone will require further investigation.

For dry conditions, the increase of the cutting speed from 40 m/min to 60 m/min causes no significant increase of the cutting force in contrast to the expected thermal softening effect, which does not seem to be relevant in this parameter range. Hence, the total dissipated energy increased linear with speed and so the heat flow into the tool \( Q_{\text{tool}} \). Proceeding tool wear leads to increasing cutting forces as expected. At the same time, the mean tool temperature \( T_{\text{tool}} \) at the measured position and consequently the calculated heat flow increase, both not drastically but significantly. This result indicates the principle feasibility of a temperature based tool wear monitoring system under the same process.
parameters. The heat partition into the tool does not seem to be affected by proceeding tool wear, however is lower compared to the new tool condition.

For HP CF, increasing coolant pressure and flow rate lead to a low but significant reduction of the mean tool temperature, the heat flow into the tool as well as the heat partition into the tool. Compared to dry machining, the heat flow into the tool under HP CF is negligible, but still thermally induced wear mechanisms occur when using this technology in machining [55]. This may be explained by a spatial constraint thermal softening of the tool material at the tool chip contact zone, where cooling lubrication media may not enter, as reported in the literature [16][17].

Figure 9 shows the results for the work piece material Ti-6Al-4V β-condition for the process parameters $f_z = 0.14$ mm, $a_p = 2.5$ mm, $a_e = 15$ mm. It compares different cooling lubrication strategies with dry conditions under different cooling, lubrication and process parameters in CRYO CL.

The analysis of the cutting force $F_c$ and the tool temperature $T_{tool}$ shows no evidence for the influence of an increasing cutting speed for HP CF. This indicates the effective cooling capability to be at least in the order of the total generated power at the higher of both cutting velocities.

However, for CRYO CL, an increase of the cutting speed from $v_c = 40$ m/min to $v_c = 60$ m/min at mass flow rate of $\dot{m}_{CO_2} = 200$ g/min and volumetric flow rate $Q_{oil} = 40$ ml/h does not seem to influence the cutting forces. However, it drastically increases the measured mean tool temperature, indicating an effective cooling flow lower than the dissipated heat in the cutting zone.

For CRYO CL at a constant cutting speed of $v_c = 40$ m/min and a constant CO$_2$ mass flow rate of $\dot{m}_{CO_2} = 200$ g/min, the cutting force decreases slightly but significantly with an increasing volumetric flow rate of the oil from $Q_{oil} = 20$ ml/h to $Q_{oil} = 60$ ml/h. Furthermore, the tool temperature $T_{tool}$ is significantly reduced by an increasing volumetric flow rate of the oil. Both, the reduction of tool temperature $T_{tool}$ and the reduction of the cutting force $F_c$ can be explained by an improved lubrication effect, which causes a reduction of the friction between the tool and chip and thus reduction.
of the heat generation rate. Previous research has shown that lower active forces in MQL machining compared to dry and wet machining can be explained by an improved lubrication effect [39]. The combination of CO₂ and MQL showed comparable active forces to flood cooling [39]. The increase of the active force achieved with CO₂ and MQL compared to only MQL was reasoned by material solidification, different nozzle orientation and by influenced oil properties due to the low CO₂ temperatures [39]. Another reason can be that due to the two channel supply of the CO₂ and the MQL from the outside of the tool, the CO₂ prevents the MQL to reach the cutting zone. Therefore, the MQL cannot develop the full lubrication effect. A similar observation has been reported in the literature [47]. With the CRYO CL concept (single channel supply of oil and CO₂) during the present experiments, a suitable lubrication effect of the oil was assured. Therefore, during CRYO CL cutting forces reduced by less than 10% were achieved compared to HP CF. Furthermore, the results show that a fine adjustment of the lubrication effect can be realized due to a variation of the volumetric oil flow rate.

An increase of the CO₂ mass flow rate from \( \dot{m}_{\text{CO}_2} = 170 \, \text{g/min} \) to \( \dot{m}_{\text{CO}_2} = 230 \, \text{g/min} \) results in a significant reduction of the tool temperature \( T_{\text{tool}} \) and of the heat flow into the tool \( Q_{\text{tool}} \). The variation of the CO₂ mass flow rate \( \dot{m}_{\text{CO}_2} \) shows no principal influence on the cutting force \( F_c \). Based on these results, the decreased tool temperature can be explained only by an improved cooling effect caused by the increased CO₂ mass flow rate. A lubrication effect of the CO₂ was not observed. This proves that CRYO CL strategy may independently influence the effective cooling and lubrication.

For dry machining conditions, the cutting force level is comparable to the other cooling strategies. In contrast to the results for Ti-6Al-4V in αβ-condition, no thermal weakening effect is observed. This can be due to the low cutting speed increase keeping the temperature in the workpiece cutting zone under the threshold, where hardness and material strength substantially decrease. A rapid decrease of tensile strength occurs beyond \( T = 500 \, ^\circ\text{C} \) for Ti-6Al-4V [56]. The increase in cutting speed for Ti-6Al-4V in β condition did not result in increased temperature beyond this thermal softening point of the workpiece material to significantly affect the cutting forces.

The single channel supply of CO₂ and oil allows a fine adjustment of the cooling and the lubrication effect, which depending on the material properties allows for balancing the thermal and mechanical loads during machining.

Under consideration of the process parameters and the workpiece material, cutting temperature and cutting force can be potentially controlled independently to a certain degree, although the adjustment is limited to a small area due to the variation possibilities of the supplied amounts of CO₂ and oil.

5. Conclusion and outlook

A novel methodology is presented for processing cutting forces and transient temperatures in milling processes within a system of physical models in order to calculate heat flow as well as heat partitions. This method allows for decoupling the mechanical effects from thermal effects in machining, which can be used for selection and optimization of cooling and lubrication parameters to enhance machining of difficult-to-cut materials. The in-situ acquired process data are treated within a thermodynamic energy balance enabling mutual comparison by bringing together the mechanical and thermal process domains. On the basis of a tool integrated thermocouple, a measured temperature rise in the milling cutter in steady state conditions was refurbished with a transient temperature model based on Green’s functions. This model used the measured time variant heat flux as process power multiplied with the effective heat partition into the tool as input. By integrating the thermo-mechanical state variables within the thermodynamic domain, the novel
integrated data driven and physical modelling approach offers profound insights into the governing mechanisms of cooling and lubrication in milling processes. This was demonstrated by a systematic analysis of the effects using different cooling strategies, namely high-pressure cutting fluid supply (HP CF) and cryogenic cooling lubrication (CRYO CL), and cooling parameters regarding pressure and mass flow rate for machining 42CrMo4 and Ti-6Al-4V in αβ and β condition. The major findings and conclusions for process optimization of the study are:

• The generic principle of a soft sensor was demonstrated combining force and temperature measurements as well as a thermodynamic and transient temperature model. By this fusion of sensor signals and models in the thermodynamic domain, the process of dissipating mechanical energy into sensible heat for milling under lubrication is broken down.

• The methodology shows a unique way in order to experimentally analyze the heat partitioning in a real milling process as well as separate the cooling and lubrication effect of the used media for the first time.

• It can be potentially used to validate for instance the upcoming combined FEM-CFD-simulation of lubricated cutting processes in the future.

• The approach was used to quantify and compare the cooling lubrication effect of HP CF and CRYO CL under realistic conditions and a broad parameter range for the first time.

• These investigations showed significant changes in cooling capability of the investigated strategies as well as their influence on the tool and the workpiece. They further showed and separated the significant combined influence of cooling parameters, process parameters, tool wear as well as workpiece properties.

• The thermodynamic analysis showed for the three investigated materials and the HP CF strategy a decreasing heat partition into the tool with rising pressure. By this strategy, the measured temperatures in the tool never exceeded 50 °C. The cutting forces are higher compared to dry conditions and CRYO CL, which was explained by a high cooling effect on the workpiece material. The cutting forces are affected by the temperature at the cutting zone and the thermal softening of the workpiece material. The temperature at the cutting zone can be controlled by cutting speed as well as the cooling-lubricating medium. A conflict of objectives was revealed, where the supply parameters and cutting parameters shall be selected to allow thermal softening in order to lower the mechanically induced tool wear, but prevent thermally induced wear simultaneously. The mentioned balance is workpiece-dependent and a determination of suitable parameters is subjected to future work.

• For the cooling strategy CRYO CL, significantly higher temperatures in the milling cutter above 100 °C and heat partitions in the tool were observed. An increase of the CO2 mass flow rate results in a significant reduction of the tool temperature and of the heat flow into the tool. The cutting forces decrease slightly but significantly with increasing volumetric flow rate of the oil, which was explained by the reduced friction. For this cooling-lubrication strategy, the selection of suitable process and supply parameters (Oil and CO2) is of superordinate importance and possible with the presented method.

• Proceeding tool wear increased the cutting forces and with them the process power and heat flow into the tool.
6. Acknowledgements

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