EVALUATION OF THE ATOMIZATION-BASED CUTTING FLUID SPRAY SYSTEM IN MILLING OF TITANIUM ALLOY

BY

SUROJIT GANGULI

THESIS

Submitted in partial fulfillment of the requirements for the degree of Master of Science in Mechanical Engineering in the Graduate College of the University of Illinois at Urbana-Champaign, 2015

Urbana, Illinois

Adviser:

Professor Shiv G. Kapoor
Abstract

Titanium alloys are considered difficult to machine materials because of poor thermal conductivity and long elongation to break ratio, which makes it difficult to dissipate heat generated during cutting process. Therefore, effective cooling and lubrication effects are vital during machining of these alloys. Recently, it has been shown that an atomization-based cutting fluid (ACF) spray system can effectively cool and lubricate the cutting zone during turning of Ti-6Al-4V, leading to significant improvement in machinability of titanium alloys. However, the efficacy of the ACF spray system is yet to be tested for other machining operations that are different from turning, like milling. The droplet impingement dynamics in milling are different than that in turning because of the presence of a rotating cutting tool as opposed to a stationary single point cutting tool in turning. Also, milling is an intermittent cutting process that gets affected by thermal shock caused by cutting fluid.

The research presented in this thesis investigates the effectiveness of the ACF spray system in end-milling of a titanium alloy, Ti-6Al-4V. To accomplish this, an experimental study has been conducted in two phases. During the first phase,
the experiments are conducted to study the role of various combinations of spray parameters on cutting forces and select the one that has the least cutting forces. In the second phase, machining experiments are conducted, using the spray parameters selected in phase one, to assess the machinability of titanium alloy for different cutting fluid application methods, viz., ACF system, flood cooling and dry cutting, and evaluate the effectiveness of ACF spray system for different machining conditions.

It is concluded from Phase 1 experiments that the cutting forces are the least for those spray parameters for which the velocity of the droplets is well within the spreading regime. Furthermore, a numerical model, based on Discrete Phase Modeling approach and Eulerian Wall Film model, of an ACF spray system has been developed and used to simulate the liquid film formation on a rotating cylindrical surface, to explain the variation in the experimentally observed cutting forces for different combinations of spray parameters.

The end-milling experiments show that the presence of CO$_2$ in the droplet carrier gas is responsible for cooling the cutting zone more effectively in milling than what could be achieved in its absence. As a result, tool life increases by 50% when the droplet carrier gas is a mixture of air and CO$_2$ as compared to the case where droplet carrier gas has only air. Tool life experiments show that the ACF spray system outperforms other cutting methods, in three areas critical to access machinability, namely cutting forces, surface roughness and tool wear.
Using the ACF spray system leads to uniform tool flank wear, which results in lower cutting forces and higher surface finish, and the tool life extends up to 75% over flood cooling. Additionally, chip morphology analysis reveals that using ACF spray system leads to the formation of shorter and thinner chips, as compared to that when flood cooling is used.
Acknowledgments

Foremost, I would like to express gratitude to my advisor, Prof. Shiv G. Kapoor, for giving me the opportunity to become a part of his research group and for investing his valuable time in mentoring me. I would forever be grateful to him for his valuable inputs and guidance during the entire duration of the masters program, without which this thesis would not have been completed.

I would like to thank the Grayce Wicall Gauthier Chair and TechSolve for funding this research. I am thankful to the UIUC MechSE machine shop for helping me out with the experimental setup. I would also thank Postdoc Researcher Dr. Chandra Nath for introducing me to this research project. I will always be indebted to Soham Mujumdar for not only providing selfless help during the entire duration of the research project but also for encouraging and supporting me at times when I felt low.

I consider myself lucky to have conducted research alongside Arvind Pattabhiraman and Asif Tanveer, as discussions with them enhanced the quality of my research output. I am grateful to the tenants of 4426 MEL: Ashwin, Piyush and Dr. David Petty, for making my stay at UIUC memorable.

I am forever indebted to my parents Malabika and Bhaskar Ganguli, and my sister Suravi Ganguli for their selfless love, support, and encouragement throughout my life. Finally, I would like to thank God for providing me with metal and physical strength each day of my life.
# Table of Contents

List of Tables ......................................................................................................................... ix

List of Figures ............................................................................................................................ x

Nomenclature ............................................................................................................................. xiv

Chapter 1 Introduction ............................................................................................................. 1
  1.1 Background and Motivation ............................................................................................... 1
  1.2 Research Objectives, Scope, and Tasks .............................................................................. 6
    1.2.1 Research Objectives and Goals ................................................................................. 6
    1.2.2 Scope of Research ...................................................................................................... 7
    1.2.3 Research Tasks ......................................................................................................... 8
  1.3 Overview of Thesis ........................................................................................................... 10

Chapter 2 Literature Review ................................................................................................... 12
  2.1 Milling titanium alloys ..................................................................................................... 13
    2.1.1 Challenges encountered during machining of titanium alloys ................................. 13
    2.1.1.1 Poor thermal conductivity .................................................................................. 14
    2.1.1.2 Low modulus of elasticity and the consequent springback ............................... 15
    2.1.1.3 Chemical reactivity ............................................................................................. 16
    2.1.2 Machinability of titanium alloys .............................................................................. 16
    2.1.2.1 Tool life and wear characteristics ..................................................................... 17
    2.1.2.2 Surface finish ..................................................................................................... 19
    2.1.2.3 Cutting forces ..................................................................................................... 20
    2.1.2.4 Other machinability criteria .............................................................................. 20
    2.1.3 Conventional cooling methods .................................................................................. 21
  2.2 Mist-based systems .......................................................................................................... 23
    2.2.1 Minimum Quantity Lubrication ................................................................................ 23
    2.2.2 Atomization-based cutting fluid (ACF) spray system ............................................. 30
  2.3 Single droplet dynamics ................................................................................................... 40
    2.3.1 Droplet atomization ................................................................................................. 40
    2.3.2 Single droplet impingement dynamics ..................................................................... 42
    2.3.3 Single droplet impingement regimes ....................................................................... 46
  2.4 Single droplet spreading behavior on a stationary surface .............................................. 48
List of Tables

3.1 Thermo-physical properties of cutting fluid and water 87
3.2 Values of different non-dimensional numbers at given ACF spray conditions 88
3.3 Experimental machining parameters 89
3.4 Experimental results for different combinations of spray conditions 90
4.1 Values of droplet velocity at given ACF spray conditions 130
List of Figures

2.1 Cutting zones and their corresponding generated heat [1] .... 14
2.2 Chips obtained after 1 min cutting time (first cut) from the
application of (a) standard pressure coolant and (b) HPC [2] ... 21
2.3 The velocity field with (a) flood coolant and (b) MQL cutting
fluid application systems [3] ................................. 24
2.4 SEM images of inserts under varied cutting speed in three coolant
supply methods [4] ............................................... 26
2.5 Cutting force and cutting temperature variation for different
spray parameters [5] ............................................ 27
2.6 Variation of flank wear with machining time for different cutting
2.7 Velocity contours [7] ......................................... 29
2.8 ACF system used for micro-end milling [8] ........................ 31
2.9 Peak-to-valley cutting forces at the feed rate of 0.33 μm/ flute [8] . 32
2.10 Photographs of burrs [8] ..................................... 32
2.11 Photographs of tool wear after cutting 30 slots with flood cooling
and 45 slots with atomized fluid [8] ............................. 32
2.12 Photographs of generated chips for the conditions of (a) dry,
(b) flood cooling, and (c) atomization-based cooling [8] .......... 33
2.13 Experimental results at different droplet impingement velocities [8] 34
2.14 (a) Photograph of the ACF spray unit and (b) ACF spray pa-
rameters in turning setup [9] ..................................... 35
2.15 Metal chips produced in different cutting fluid application meth-
ods [9] ................................................................. 36
2.16 Two-way diagrams for tool life [9] ............................ 37
2.17 Spray characteristics [10] .................................... 38
2.18 Four different nozzle geometries studied [10] .................... 39
2.19 Photograph of the spray at different nozzle geometries: (a) Lh
= −10.16 mm, γn = 6 °, (b) Lh = −10.16 mm, γn = 0 °, (c) Lh
= +10.16 mm, γn = 6 °, and (d) Lh = +10.16 mm, γn = 0 ° [10]. 39
2.20 Photographs of the spray at mist velocities of 0.5, 1.5, 2.5, and
3.5 m/s and spray velocities of 11.0 and 21.0 m/s [10] .......... 39
2.21 Effect of variation of air velocities [10] ........................ 40
2.22 Operating principles of different types of atomizers [11] .... 41
2.23 Schematic of an ultrasonic atomizer [12, 13] ........................................ 42
2.24 Survey of parameters governing the impact of a liquid drop. [14] ....... 44
2.25 The various impingement regimes identified for a droplet. [15] ......... 45
2.26 Schematic representation of the spread factor with time. The different lines correspond to an arbitrary choice of possible spreading histories, depending on the parameters of the impact [16] ........... 49
2.27 Sequences of water drop impacts on surfaces with different wettabili-ty [16] ........................................................................................................ 50
2.28 Sequences of water drop impacts on surfaces with different sur-face roughness [16] .................................................................................. 50
2.29 The various impingement regimes identified for a droplet. [17] .......... 52
2.30 Drop trajectory and film frames for collision of drop (d_d = 2 mm, v_d = 1.2 m/sec) with surface of rotating disk [18] .......................... 55
2.31 Effect of droplet diameter on the reflection of drop form disk. [18] 56
2.32 Splashing of a liquid droplet with Re = 598.8 [19] ............................... 57
2.33 Schematic view of the splashing process. [19] ........................................ 57
2.34 Splashing of a liquid droplet with Re = 251.4 [19] ............................... 57
2.35 Schematic view of the deposition process. [19] ......................................... 57
2.36 Limits for splashing and deposition of primary droplets. [19] .......... 58
2.37 Variables and coordinates system. [20] .................................................. 59
2.38 Change of impact outcome with increasing We_t for We_n=30 [20] .... 61
2.39 Drop impact regimes. [20] ...................................................................... 61
2.40 Dimensionless excess spread area versus We_t. [20] .............................. 62
2.41 Schematics of: (a) a typical wall film cell used in the formulation of the film model; (b) the major physical phenomena governing film flow [15] ................................................................. 65
2.42 Flat plate impingement comparison for: (a) spray radius; (b) spray height; (c) mean film thickness; and (d) ratio of attached fluid [15] ................................................................. 68
2.43 Fluid film velocity at 9.6 ms for impingement angle of 35°[21] ........ 69
2.44 Variation of the average film thickness with the impact Re- number [22] ............................................................................................... 70
2.45 Film thickness variation with (a) tube air velocity (b) droplet velocity [23] ............................................................................................... 72
2.46 Illustrations of the imprints of the liquid film at 15,000 rpm for (a) 0.3, (b) 0.77 and (c) 1 bar inlet for 45°oriented channels [24] .... 74
2.47 Showing the unlikely occurrence of hydrodynamic lubrication [25] 75
2.48 Fluid flow between two sliding plates [25] ............................................ 77
2.49 Water droplet at the end of the spreading phase [26] ............................ 78

3.1 Photograph of the ACF spray unit [27] ................................................... 83
3.2 ACF spray system in milling setup .......................................................... 84
3.3 Experimental setup of the ACF spray unit ............................................ 85
3.4 Position of ACF spray unit in relation to feed direction ..................... 86
3.5 Variation of cutting forces when machining with M1 machining condition and using the ACF spray system .......................... 91
3.6 Variation of cutting forces for different combinations of spray conditions ........................................................................... 92
3.7 Variation of cutting forces ......................................................................................................................................................... 93
3.8 Wear progress of the tool flank with machining for different cutting fluid application methods .................................................. 94
3.9 Tool flank face when machining at M1 and using the ACF spray system ................................................................................... 97
3.10 Tool flank face when machining at M1 and using dry cutting conditions ................................................................................... 97
3.11 Tool flank face when machining at M1 and using flood cooling ................................................................................................. 97
3.12 Tool flank face when machining at M4 and using the ACF spray system ................................................................................... 97
3.13 Tool flank face when machining at M4 and using flood cooling ................................................................................................. 98
3.14 Chip morphology when machining at M1 ................................................................................................................................... 99
3.15 Chip morphology when machining at M4 ................................................................................................................................... 99
3.16 Wear progress of the tool flank with machining for different cutting fluid application methods .................................................. 100
3.17 Tool flank face when machining at M1 and using the ACF spray system no CO₂ ................................................................. 101
3.18 Variation of cutting forces for different cutting fluid application methods ................................................................................ 102
3.19 Variation of cutting forces for different machining conditions with the ACF spray system ......................................................... 103
3.20 (a) Average (b) maximum surface roughness values for different cutting fluid application method and M1 machining condition 105
3.21 (a) Average (b) maximum surface roughness values for different machining conditions with the ACF system ....................... 106

4.1 Flowchart of the modeling approach. .................................................. 110
4.2 Experimental set-up to measure the carrier gas velocities ............. 115
4.3 Design of the ACF spray that was used for numerical simulations (scale: mm) .............................................................. 117
4.4 Velocity vectors of droplet carrier gas for different inlet pressures 118
4.5 Entertainment of surrounding air in the spray flow ......................... 119
4.6 Velocity contours of droplet carrier gas for different inlet pressures 120
4.7 Comparison of numerical end experimental gas velocity values .... 121
4.8 Schematic of the computational domain ........................................... 125
4.9 Enlarged view of the droplet and gas nozzle .................................. 126
4.10 Contours of film thickness for different spray parameters .......... 131
4.11 Position of film thickness measurement, relative to the ACF spray and cylinder. ................................................................. 132
4.12 Top view of the numerical setup. ....................................................... 132

xii
4.13 0.1 cm .................................................. 133
4.14 0.2 cm .................................................. 134
4.15 0.3 cm .................................................. 135
4.16 0.4 cm .................................................. 136
4.17 3-D plot of the film formed on the rotating cylindrical surface .. 138
4.18 3-D plot of average film thickness as a function of angular position, in Cartesian coordinates ................................. 139
4.19 Average film thickness ........................................ 140
4.20 Variation of cutting forces for different combinations of spray parameters. ......................................................... 143
Nomenclature

$h$ height of film thickness

$v$ carrier gas velocity

$c$ speed of sound

$M$ Mach number

$We$ Weber number

$Oh$ Ohnesorge number

$Re$ Reynolds number

$d_o$ liquid droplet’s diameter

$u_o$ normal velocity of liquid droplet

$x_1$ spray distance

$x_2$ droplet carrier gas inlet pressure

$FR$ feed/tooth

$V_b$ tool flank wear

$R_a$ average surface roughness

$R_z$ maximum surface roughness

$DPM$ discrete phase modeling
$EWF$ eulerian wall film

$ACF$ atomization-based cutting fluid

$SST$ shear stress transport

$ADOC$ axial depth of cut

$RDOC$ radial depth of cut

Greek Symbols

$\alpha$ angular position relative to the tool feed direction

$\theta$ droplet impingement angle

$\mu$ liquid viscosity

$\rho$ cutting fluid density

$\rho_g$ carrier gas density

$\sigma$ liquid surface tension
Chapter 1

Introduction

1.1 Background and Motivation

Titanium alloys were first introduced in early 1950s and within a relatively short period of time it has become the backbone material for medical, aerospace, automotive and chemical industries, among others. Attributes of titanium alloys that lead to its rise in demand and popularity were high strength to weight ratio, fracture and corrosion resistant characteristics, bio-compatibility and high temperature strength. Nevertheless, titanium alloys are considered difficult to machine materials because of certain characteristics, such as poor thermal conductivity and low elongation to break ratio, which makes it difficult to dissipate the heat generated during the cutting process [28, 29]. High temperatures generated in the cutting zone lead to plastic deformation of the tool and an increase in chemical reactivity of titanium with the tool because of that the tool wears out rapidly and fails catastrophically by chipping [30, 31]. As a result, production cost increases because of frequent tool replacements and machining quality decreases because of poor surface finish. Hence, it is imperative to find effective methods
to remove heat from the cutting zone in order to reduce tool wear and improve surface finish while machining titanium alloys.

Different cutting fluid application methods have been proposed to remove heat and reduce stress in the cutting zone. In flood cooling copious amount (1-10 L/min) of cutting fluid at low pressure is applied to the tool and work piece. However, inability of the fluid to penetrate at the cutting interfaces [32, 3, 33] and high operational costs along with negative impact on environment and health [34, 35, 36], has lead researchers to explore other alternatives of cutting fluid application method. In order to mitigate such effects, minimum quantity lubrication (MQL) approach has been introduced and developed wherein, very small amount of cutting fluid, 6-100 ml/h, is applied in the form of a directed mist [37, 38]. Experimental results show that MQL machining can improve the tool life and reduce the cutting force due to better cooling and lubricating effect [4, 39, 40, 5, 41, 3]. Application of MQL results in removal of heat mainly because of forced convection provided by the compressed air and partially by evaporation of the cutting oil [42]. Nevertheless, the major drawback of the MQL system is that the size of droplets generated by the system cannot be easily controlled and as a result of which the penetration of these droplets into the the tool-chip interface is not guaranteed. Consequently, using MQL system leads to underutilization of the cooling potential of the cutting fluid in cooling the cutting surface [43].

Cooling gas methods that includes injecting refrigerated gas such as air, nitrogen
and carbon dioxide, at the cutting zone to remove the heat, have also been used to effectively control temperatures and eliminate cutting fluid usage, while increasing tool life and reducing wear when machining titanium alloys [44, 45, 42, 46, 47, 48]. Nonetheless, the alternating thermal loads on cutting edges of the tool act adversarial when milling and higher cutting forces are recorded because of improved mechanical strength of the workpiece material at low temperature [49, 46, 50].

To overcome the limitations inherent to the previously mentioned techniques of cooling and lubricating the cutting zone, Jun et al. [8] proposed an atomization-based cutting fluid (ACF) spray system for micro-end milling, which is based on the principle that atomized droplets having diameters in the range of 5-15 µm can access the cutting zone, absorb heat while acting as a lubricating agent, and remove heat from the cutting zone by evaporation [51]. The ACF system was found to reduce cutting forces, significantly improve tool life, by 3 times over flood cooling, even at feed rates where ploughing and rubbing is dominant, and lower the temperature of the cutting zone remarkably more than what could be achieved while using dry and flood cooling methods. Nonetheless, the success of ACF spray system in macro-scale milling is not guaranteed because the cutting interface that is generated during macro-scale milling, because of greater feed and depth of cut, is larger than the one generated in micro-scale milling. As a result the liquid film formed upon droplets impingement can get evaporated even before reaching the entire tool-chip interface [52].
Nath et al. [9] developed an ACF spray system for macro-scale turning of Ti-6Al-4V and conducted experiments to study the effect of ACF spray parameters and found that a combination of low pressure (150 psi) air-mixed CO$_2$ as the carrier gas, higher cutting fluid flow rate (20 ml/min) and a large spray distance (35 mm) can extend the tool life up to 40-50% over flood cooling. It was further observed that using the ACF spray system results in the production of broken chips that is not the case when flood cooling is used, which further goes on to show that using the ACF spray system in turning operation leads to effective cooling and lubrication at the cutting zone. The success of the ACF spray system was attributed to the formation of a spreading thin liquid film upon impinging of the droplets on the rake face of the tool. Superior cooling and lubrication capabilities of the ACF spray system is because the thin liquid film has higher penetrating capabilities, at the cutting interface, as compared to the liquid in flood cooling. However, it is yet to be seen if the ACF spray system is effective for other macro-scale machining operations that are different from turning, like milling.

There are three main challenges unique to milling when an ACF spray system is used. First, the droplet impingement dynamics gets significantly influenced by the presence of a rotating cutting tool and the parameters governing the fate of droplets after impingement could be different from the one that govern the dynamics of droplet impacting a stationary tool, found in turning. Second, due to the complicated geometry of the milling tool, the flow behavior of each droplet
after impingement could be quite different and this would lead to the formation of a nonuniform fluid film. Third, the film formed on an end mill tool would have variability in momentum, with the highest value being near the point of impact and lower values farther away from it, and it would be quite challenging to achieve the film momentum that would ensure effective film penetration at the tool-chip interface all throughout the time the cutting edge of tool is engaged with the workpiece. Also, milling is an intermittent cutting process that gets affected by thermal shock caused by cutting fluid [53], which is not the case for turning operations.

The film formation has been shown to be the reason behind the the effective cooling and lubrication capabilities of the ACF spray system [21]. Hence, the design of a ACF spray system could be evaluated by studying the characteristics of the liquid film formed by the impinging droplets. However, it is quite complicated to experimentally characterize a liquid film, formed on a tool, during the machining operations. In such a scenario, researchers have used the numerical approach to study the liquid film formation. Ghai et al. [26] modeled single droplet spreading on a rotating surface with an aim to design an efficient ACF sprays system for micro-turning processes. However, the model is restricted in it’s application as it is applicable for only single droplet impingement and it is valid for only certain types of cutting fluid. Boughner et al. [23] developed a probabilistic model to study the rate of micro-film formation on a rotating cylindrical surface due to an ACF spray system. The model was used to evaluate the relative
importance of different spray parameters on liquid film formation. Nonetheless, the model is 2-dimensional and does not include droplet interactions that could have an appreciable impact on the results of the model predictions.

While the studies conducted in the past have established the effectiveness of the ACF spray system in micro-machining process and macro-scale turning operations, several gaps in knowledge still exists. First, there is a lack of study conducted in macro-scale end-milling, using an ACF spray system and as such it is not known whether the ACF spray system would be effective in end-milling process as compared to other coolant application methods. Second, there is a lack of understanding of the effect that the composition of the droplet carrier gas, in an ACF spray system, would have on the tool life. Third, it is not clear as to what effect the variation of spray parameters would have on the machinability, when end-milling titanium alloy.

1.2 Research Objectives, Scope, and Tasks

1.2.1 Research Objectives and Goals

The overall objective of the research is to investigate the effectiveness of the ACF spray system in end-milling of a titanium alloy, Ti-6Al-4V. To accomplish this, the specific objectives are:
1. To gain an understanding of the role of the ACF spray parameters on the machinability when milling titanium alloy, Ti-6Al-4V.

2. To develop a numerical model, to study the liquid film formation, when the droplets from the ACF spray system impinge on a rotating surface.

3. To study the effectiveness of the ACF spray system in terms of cooling and lubrication for different machining conditions.

4. To compare cutting forces, tool life and surface finish using the ACF spray system with other cutting fluid application methods, viz., flood cooling and dry cutting.

1.2.2 Scope of Research

This research focuses on investigating the machinability performance of the ACF spray system that is used for providing cooling and lubrication effects when end-milling a titanium alloy. In this study the ACF spray system that is used, generates liquid droplets of diameter 20 \( \mu m \) or less. Out of the four specific spray parameters, viz., pressure level of droplet carrier gas, fluid flow rate, droplet impingement angle and spray distance, the, fluid flow rate and droplet impingement angle were fixed in accordance with values that are known to produce highest tool life when machining is carried out using the ACF spray system in turning operations. The droplet carrier gas used for this study is a mixture of air and
CO₂, in equal volumes. The work material used in this research is Ti-6Al-4V because it is widely used in different industries. The end-mill tools used in this study are restricted to uncoated carbide tools having four flutes, 10 mm mill diameter, and 30° helix angle. The machining experiments were performed using a water-soluble cutting fluid, S-1001 at 10% dilution. The machining conditions used for experiments are chosen to represent the range typically used in industry during titanium machining (i.e. cutting speed: 40 - 80 m/s, axial/radial depth of cut: 0.1 - 5 mm and feed: 0.04 - 2 feed/tooth).

1.2.3 Research Tasks

The objectives of this research will be accomplished in the following two phases:

Phase 1: Focus is given to experimental investigation of the effect that the variation of ACF spray parameters would have on the measured cutting forces when end-milling a titanium alloy. A numerical model of the ACF spray system will be developed and used to provide a physics-based understanding of the experimental observations. This will be achieved in the following sequence of tasks:

1. Develop a testbed for evaluating the ACF spray system in end-milling operations.

2. Identify the ACF spray parameters that are relevant to end-milling operations. Evaluate the parameters whose values could be fixed and the ones
whose values are to be varied.

3. Ascertain the machining parameter that would be used to evaluate the different combinations of spray parameters and conduct machining experiments.

4. Develop a numerical model of the ACF spray system that can be simulated to get an insight into the characteristics of the liquid film formed by the impinging droplets on a rotating cylindrical surface.

5. Simulate the liquid film formation for different combinations of spray parameters. Utilize the simulation results to explain the experimental observations.

Phase 2: Focus is given to experimentally compare the machining performance of the ACF spray system with other cutting fluid application methods, viz., dry cutting and flood cooling. The emphasis in this phase will also be on evaluating the ACF spray system for different machining conditions. The necessary specific tasks are:

1. Identify the distinct machining parameters that are to be used for evaluating the different cutting fluid application methods. Also, develop an understanding of methods that will be used to measure the machining parameters.

2. Conduct tool-life experiments and record the machining parameters (cutting
forces, tool wear and surface roughness), when end-milling titanium alloy for
a particular machining condition but using different cutting fluid application
methods.

3. Conduct tool-life experiments and record the machining parameters (cutting
forces, tool wear and surface roughness), when end-milling titanium alloy
using the ACF spray system for four different machining conditions.

4. Analyze the recorded machining parameters to evaluate the effect that the
cutting fluid application method has on the machinability of titanium alloy
and also to study the impact that the variation of machining conditions has
on the functionality of the ACF spray system.

5. Study the characteristics of metal chips, generated during end-milling of ti-
nium alloy, to compare different cutting fluid application methods, namely,
ACF spray system and flood cooling.

1.3 Overview of Thesis

Chapter 2 provides an overview of the current state of machinability of titanium
alloys along with the available literature on the application and performance of
the ACF spray system, single droplet impingement dynamics, behavior of droplets
impinging on a stationary or rotating surface and the numerical study of film
formation by impinging droplets of spray based systems.
In Chapter 3, the effect of variation of ACF spray parameters on the machining parameters when end milling a titanium alloy, has been experimentally investigated. The performance of ACF spray system has been compared to other cutting fluid application methods by the way of conducting tool-life experiments and undertaking chip morphology study. Evaluation of the effect that the variation in machining parameters has on the performance of the ACF spray system has been conducted experimentally.

In Chapter 4, the numerical model of the ACF spray system has been presented and validated. The characteristics of liquid film formed by the model on a rotating cylindrical surface, for different combinations of spray parameters, have been presented and used for explaining certain experimental observations of Chapter 3.

Chapter 5, summarizes the conclusions that are obtained as a part of this research activity and offers direction of future work in this area.
Chapter 2

Literature Review

This chapter commences with an overview of past research related to machinability of titanium alloys and the coolant application methods investigated to improve the machinability of titanium alloys. Section 2.2 presents a summary of the different mist-based systems that have been used for metal cutting, with an aim of reducing the cutting fluid consumption. Section 2.3 reviews the different mechanisms for generating atomized droplets and presents the dynamics of a droplet impinging on a surface. Section 2.4 presents the experimental and numerical studies that have been conducted to gain further insight into droplet spreading behavior on a stationary flat surface. Section 2.5 reviews the different experimental studies that have been conducted to gain further insight into the effect the presence of a rotating surface will have on droplet impingement dynamics. Section 2.6 examines the different modeling techniques that are available in literature to model the parameters that are used for evaluating the performance of the ACF spray system in metal cutting. The chapter concludes with a summary of previous research and list the areas for future research.
2.1 Milling titanium alloys

Titanium and its alloys are used extensively in the aerospace industry, to develop aircraft engines and manufacture airframes, because of certain characteristics of these class of materials. They possess high specific strength (strength-to-weight ratio) and fracture resistance attributes, which is maintained even at high temperatures. Titanium alloys have exceptional corrosion resistance characteristics, which provides savings on protective coating like paints that would otherwise be used in case of steel [54]. Furthermore, titanium and its alloys are finding increased usage in other industrial and commercial applications such as petroleum refining, chemical processing, surgical implantation, pulp and paper, pollution control, nuclear waste and storage, food processing, electrochemical and marine applications [28]. As a result it has become critical to evaluate the machinability of titanium and its alloys for different machining operations.

2.1.1 Challenges encountered during machining of titanium alloys

Titanium and its alloys are considered hard-to-cut materials, implying that they are difficult to machine [54]. Difficulties in machining titanium alloys are caused by a combination of the features presented in sections below.
2.1.1.1 Poor thermal conductivity

During the cutting process, energy is consumed to remove material by plastic deformation of the workpiece or to overcome friction at the tool-chip or tool-workpiece interfaces. Almost all of this energy is converted into heat and consequently increases the temperature of the cutting zone. The three main heat sources during cutting operations are: first, the primary shear zone at which the heat is mainly generated by plastic deformation; second, the secondary shear zone at which the heat is generated by a combination of shearing and friction on the tool rake face, and finally, the tertiary shear zone at which the heat is produced due to friction between newly machined and the flank face of the cutting tool [1]. Schematic representation of the cutting zones and the corresponding heat affected zones are shown in Fig. 2.1

![Figure 2.1: Cutting zones and their corresponding generated heat [1]](image)

Although the heat generated at the cutting zone softens the workpiece material and facilitate easier cutting, it is generally considered as an undesirable
phenomenon that must be prohibited or kept minimized. The heat generated during machining is primarily dissipated by the discarded chip. A smaller portion of the heat is also dissipated by means of workpiece and cutting tool [1].

Due to poor thermal conductivity (about 15 W/m °C), the heat generated during machining titanium and its alloys is not easily dissipated from the cutting zone [28], as a result of which, a vast amount of heat is trapped on or near the cutting zone which intensifies the temperature. The temperature can sometimes reach as high as 900 °, for moderate cutting velocities [30]. The elevated temperature near the cutting zone, where tool and workpiece are in touch, can rapidly deteriorate the cutting edges and make them dull. Continuing machining using a tool with dull cutting edges may generates more heat and cause catastrophic tool failure by edging and chipping.

2.1.1.2 Low modulus of elasticity and the consequent springback

Superior elasticity of titanium alloys, make them an ideal candidate for those applications where flexibility, without the possibility of cracks or disintegration is desired. However, high elasticity of titanium implies that its modulus of elasticity is relatively lower as compared to other materials. Low modulus of elasticity leads to relatively higher strain and consequently greater deformation under a certain magnitude of force. During machining operations, when the tool touches the workpiece and cutting force is applied, titanium’s elasticity makes the workpiece
spring away from the tool, which causes cutting edge being rubbed against the workpiece surface rather than performing cutting action [1]. Rubbing rather than cutting, is instrumental in increasing the friction and consequently further rise in temperature at the cutting zone. Rubbing also destroys the surface quality and dimensional accuracy.

2.1.1.3 Chemical reactivity

Despite titanium's chemical inertness at room temperatures, which makes it one of the best options for medical implants, titanium becomes highly reactive when the temperature goes beyond 500 °C. When the temperature increases, chemical reaction occurs between titanium workpiece and cutting tools that results in rapid tool wear [55]. As a result, the majority of currently available cutting tools, even the hardest ones, are not appropriate for machining titanium and its alloys due to chemical affinity, which deteriorate the cutting tool by initiating chemical wear [1].

2.1.2 Machinability of titanium alloys

Machinability of a metal refers to the ease with which a metal can be cut, permitting the removal of the metal. Machinability is usually determined based on criteria such as tool life, tool wear, cutting force, chip formation, cutting temperature, surface integrity and burr size.
During milling operation, the cutting tool is imposed to various failure modes due to extensive loading and unloading effects [56]. This phenomenon adversely affects the tool life, cutting forces, surface quality and dimensional accuracy. This implies an appropriate selection of cutting tools, machining parameters and lubrication and coolant conditions during machining titanium alloys. In order to evaluate the different options available to select from, certain performance criteria needs to be selected. The criteria that are commonly used in the industry and academia are: cutting tool's tool life and wear characteristics, cutting forces, chip formation and surface finish of the machined surface.

2.1.2.1 Tool life and wear characteristics

Tool life is one of the most important criterion that influence the selection of tool and cutting conditions. For a better understanding of tool life, it is critical to monitor tool wear. Type of tool wear encountered during milling operation can be classified according to the region of the tool that they affect [57]. During milling of titanium alloys, it was observed that flank wear was the most significant and dominant form of wear and the wear on the minor cutting edges and the tool rake face were too small to have any significant effect on tool life [58]. Quantification of flank wear is achieved by constantly monitoring the maximum flank wear land width of the cutting edges of the tool. The primary wear mechanisms that lead to tool flank wear, while machining titanium alloys at moderate cutting speeds, were,
adhesive or attritional wear, abrasive wear and oxidation [4]. Adhesive wear occurs when chips get adhered to the tool due to friction and are removed from the tool. This type of wear mechanism is important at lower cutting speeds as adhesive wear is accompanied by built up edge (BUE) formation and BUE formation is significant at low cutting speeds. During adhesive wear the carbide grain and cobalt binder wear at the same rate and the adhesive wear is characterized by the presence of grooves and dull flat appearance on the flank face [59, 60]. Abrasive wear occurs when hard particles abrade and remove material from the tool. Hard particles could be present in the work piece or could be a part of cutting fluid. This type of wear takes place in the low to medium range of cutting speeds and requires the cutting zone to be at a temperature higher than what was required for the activation of adhesive wear. Carbide tools, like the one used for this study, are made up of carbide particles held together by a binder like cobalt. At high temperatures the binder material is worn faster than the carbide grains and this leaves the carbide particles protruding out of the flank face.

Abrasive wear is often considered the primary cause of flank wear, notch wear and nose radius wear. Oxidation is a temperature activated wear mechanism and occurs when the cutting zone experiences very high temperatures. At such high temperatures, the constituents of the tool (specially the binder) react with oxygen present in the atmosphere. Oxidation occurs on the tool surface that is exposed to atmosphere and results in severe depth of cut notch formation, which is recognized by the discolored region of the tool near the notch [57]. The primary reason for
the domination of the aforesaid wear mechanisms is the high temperature that is generated within the primary and secondary shear zones along with the intimate contact between the tool-work piece and tool-chip interfaces [59].

2.1.2.2 Surface finish

During machining of titanium alloys, it is crucial to constantly keep a track of the surface quality of the machined surface because the surface integrity gets affected by the choice of machining conditions [61]. The main surface integrity concerns appear on (a) topography characteristics such as textures, waviness and surface roughness (b) mechanical properties affected, such as, residual stresses and hardness, and (c) metallurgical states such as micro-structure, phase transformation, grain size and shape, inclusions, etc [1]. It has been reported that surface roughness of machined titanium workpiece, gets affected by different machining parameters, such as, tool characteristics and tool wear [62], tool coating [63], temperature (influenced by the presence or absence of cooling techniques), feed rate, cutting speed and depth of cut [4]. Regardless of all other cutting parameters used, the surface roughness results was found to be higher in fresh cutting tools as compared to those measured when using slightly used cutting tools [64]. However, in general, it is agreed upon that higher surface roughness are observed for worn out tools [1].
2.1.2.3 Cutting forces

Ezugwu et al. [54] studied the variation of cutting force when machining titanium alloys and concluded that machining with tools that have dull or improperly ground edges, increases the cutting forces. In general, it is concluded that on increasing the material removal rate the cutting forces also increases [65]. When end-milling titanium alloys, down milling operations are mostly preferred for reducing the cutting forces [66].

2.1.2.4 Other machinability criteria

Other machinability criterions include: residual stress, burr formation and chip morphology. Both continuous and segmented chip formation processes are observed, during machining of titanium alloys, under different machining conditions [65]. Studying the characteristics of chips that are formed during the machining process, can be used for evaluating different cutting fluid application methods [9]. Palanisamy et al. [2] experimentally observed that using high pressure coolant (HPC) for machining titanium, results in the formation of broken chips, as shown in Fig. 2.2 and the surface finish and tool life also improves as compared to that obtained when stand pressure coolant are used.

The presence of residual stresses are considered as a potential source of risk in terms of crack initiation, propagation and fatigue failure and effectively has
Figure 2.2: Chips obtained after 1 min cutting time (first cut) from the application of (a) standard pressure coolant and (b) HPC [2]
detrimental influences on the component life. Different results are in literature with respect to effects of cutting parameters and tool parameters on residual stress recorded in machining titanium alloys [1].

2.1.3 Conventional cooling methods

In metal cutting considerable amount of heat is generated in the cutting zone, primarily due to plastic deformation in the primary shear zone and friction at the interface of tool rake face and the metal chip. High temperatures generated in the cutting zone lead to plastic deformation of the tool and increase in chemical reactivity of the metal with the tool because of which the tool wears out rapidly and fails catastrophically by chipping [67, 60]. As a result, production cost increases because of frequent tool replacements and machining quality decreases because of poor surface finish. Cutting fluid is used in metal cutting to remove heat and reduce friction in the cutting zone. Application of cutting fluid helps in achieving
three specific objectives: heat elimination, lubrication on the chip-tool interface and chip removal [3].

The most widely used cutting fluid application method is flood cooling. In this method, copious amount (1-10 L/min) of cutting fluid at low pressure is applied to the tool and work piece. Although flood cooling has been to able to achieve improvements in surface finish, tool lives and production rate [25] as compared to dry cutting, there are serious health, environmental and economical problems associated with such large quantity of cutting fluid usage [34, 35, 36].

In order to reduce the cutting fluid consumption, cooling gas methods, high pressure cooling and mist based spray systems have been proposed for machining titanium alloys. Cooling gas methods have been used to effectively control temperatures and eliminate cutting fluid usage, while increasing tool life and reducing wear when machining titanium alloys by injecting refrigerated gas such as air, nitrogen and carbon dioxide, to the cutting zone to remove the heat from the cutting zone [44, 45, 42, 46, 47, 48]. Nonetheless, the alternating thermal loads on cutting edges of the tool act adversarial when milling and higher cutting forces are recorded because of improved mechanical strength of the workpiece material at low temperature that are achieved in the cutting zone by using this method [49, 46, 50]. Recently, researchers have tried applying high-pressure coolant, at 70-160 bar or above, directly at the critical tool-chip interface and found that tool life increases by by 3-4 folds over flood cooling [68, 69]. However, the overall
productivity improvement that was reported using this method was found to be only about 50%, due to higher consumption rate of the cutting fluid, its delivery cost at such high pressure, and the system setup cost [68, 69, 9].

2.2 Mist-based systems

2.2.1 Minimum Quantity Lubrication

In order to overcome the limitations of the above mentioned strategies of cutting fluid application and also reduce the cutting fluid consumption, minimum quantity lubrication (MQL) method has been developed and implemented in machining processes.

In MQL method of cutting fluid application, mixture of pressurized air and cutting fluid is applied onto the cutting zone. The pressurized air stream is able to form oil mist out of the mixture of water and cutting fluid. The oil mist is able to get close to the tool-chip interface because of the high momentum imparted to the small liquid droplets by the pressurized air. As a result, significant reduction in friction and cutting forces are achieved during machining. In MQL, the temperature reduction in the cutting zone is achieved by evaporation and vaporization of liquid droplets, which is not the case for flood cooling [4]. The rate at which cutting fluid is consumed in MQL is 30 mL/h, which is significantly lower than that of flood cooling. The required air pressure in an MQL setup varies
Lacalle et al. [3] carried out experimental and numerical investigations in the field of high speed milling using MQL spray system to study the penetration capabilities of MQL jet and emulsion coolant. Lacalle et al. [3] experimentally observed that the tool wear values were lower for MQL technique as compared to conventional techniques for the same machined length and hypothesized that the difference in wear is due to the fact that emulsion coolant cannot penetrate into the cutting zone because of high spindle speed, whereas the flow of air with oil of the MQL system effectively penetrates. The hypothesis was verified using computational fluid dynamics (CFD) simulation with the code Pamflow. The results of numerical simulations are shown in fig. 4.9.

Figure 2.3: The velocity field with (a) flood coolant and (b) MQL cutting fluid application systems [3]

Figure 2.3(a) shows the velocity field in the vicinity of the rotating tool for an emulsion coolant. It is observed that a "wall" is generated that obstructs the
way of the coolant towards the tool center and prevents the cutting fluid from reaching the cutting zone. Figure 2.3(b) shows the velocity field corresponding to the MQL system. Under these circumstances the MQL jet adequately penetrates into the inner zones of the tool edge and effective lubricates and cool the cutting zone.

Sun et al. [4] studied the effect of different cutting fluid application methods, namely, dry cutting, flood cooling and MQL, on the tool life and cutting forces when end-milling a titanium alloy. Based on the experimental observations, they concluded that MQL machining can achieve the longest tool life and the lowest cutting forces for the different cutting speed, feed rate, and radial depth of cut that were used for the study.

Sun et al. [4] in their study concluded that among the different cutting fluid application methods, MQL provides the most superior lubrication, as the cutting forces were lower when MQL system was used. Also, by observing the SEM images of worn out edges, as shown in Fig. 4.3, for different cutting fluid application methods at a particular cutting speed, it is inferred that MQL cutting inserts were less seriously affected after the machining process because of the superior lubrication capabilities of MQL system as compared to flood cooling and dry cutting.

Liu et al. [5] investigated experimentally the effect of different MQL parameters, viz., air pressure, rate of consumption of oil, nozzle angle and position of the
Figure 2.4: SEM images of inserts under varied cutting speed in three coolant supply methods [4]

nozzle on cutting force and cutting temperature when end-milling titanium alloy, Ti-6Al-4V. Experimental results show that optimization of spraying distance and air pressure is critical in order to enhance the oil mist penetration into the cutting zone for different machining conditions. It was observed that the spraying angle of nozzle has minimal effect on machining process while increasing the flow rate of oil upto an extent decreases the cutting force and temperature. However, on increasing the flow rate of oil beyond 10 ml/h had little or no effect on the machining process. Figure 4.4 summarizes the effect of variation of different MQL parameters on cutting forces and temperatures.

Yuan et al. [6] experimentally investigated the impact that the variation of temperature of the pressurized gas in a MQL system would have on the machining
performance in milling of titanium alloy with uncoated cement carbide inserts. In their experimental setup cooling air and atomized oil were supplied to the cutting zone by two different nozzles. Based on experimental observations they found that application of MQL with cooled air resulted in lower cutting force, tool wear and surface roughness than what was observed with MQL system.

Yuan et al. [6] in their study concluded that there exists an optimal cooling temperature of the gas, which was -15 °C for their study, that would lead to the best machinability performance. Figure 4.5 shows the tool flank wear for different cutting environments. Furthermore, in this study it was shown that the chip generated while using MQL with cooling air, were short and chip curl was not significant as compared to other cutting environments.

There is a limited number of research that have been conducted to explore the
impact the number of nozzles in a MQL system has on the machining performance. 
Najiha et al. [7] used a 3 Dimensional computational fluid dynamic model to study 
the effect of the number of nozzles on MQL flow in the milling process. The input 
parameters for the model were number of nozzles, nozzle distance and height of 
the nozzles from the cutting point. The CFD analysis was performed using the 
ANSYS Fluent solver. Figure 2.7(a) shows that the MQL system having a single 
spray does not cover the periphery of the tool and thus the cutting surface is 
not entirely lubricated. However, MQL system with three nozzles can solve the 
problem of uneven distribution of the fluid along the periphery of the tool, which 
is encountered with a single nozzle. Figure 2.7(b) shows that using three nozzles 
can lubricate all the cutting edges of the tool. Hence, [7] concluded that three 
nozzles in MQL system might work well to achieve complete lubrication of the
rotating tool.

![Figure 2.7: Velocity contours [7]](image)

While the above studies show that MQL has been able to provide economical and environmental benefits over flood cooling during machining operations, however it has been reported that improvement in machinability of the metal is not always obtained with this technique for all types of cutting [43]. For an MQL system the temperature reduction at the cutting zone is primarily achieved by the cooling effect of the pressurized air and partially by evaporation of the cutting fluid [54]. Hence, the major drawback of the MQL system is that the size of droplets generated by the system cannot be easily controlled and as a result of which the penetration of these droplets into the tool-chip interface is not guaranteed. Consequently, using MQL system leads to underutilization of the cooling potential of the cutting fluid in cooling the cutting surface. Therefore, MQL does not work very well in machining operations where many thermal problems occur, like in the machining of difficult-to-cut materials [43]

To overcome the limitations inherent to an MQL system, atomization based cut-
ting fluid (ACF) spray systems was proposed to enhance the cooling capability at the cutting zone during macro as well as micro-scale machining operations.

2.2.2 Atomization-based cutting fluid (ACF) spray system

Jun et al. [8] developed and evaluated an ACF spray system for micro-end milling. In the ACF spray system, the liquid was atomized by ultrasonic vibration to generate droplets having diameters less than 10 µm. Air at low velocity is used to carry the atomized droplets through a pipe. As soon as the droplets exit the pipe, they are carried to the cutting zone by high velocity air that is delivered through a tube, which lies along the center of the pipe. Figure 4.10 shows the schematic of the ACF spray system that was used for conducting experiments. The velocities of air in the pipe and tube were selected on the basis of droplet impingement dynamics. In the study, Jun et al. [8] assumed that the velocity of the droplet is equal to the velocity of the air that is carrying it. The velocity of air in the pipe was selected such that droplets do not stick on the pipe wall and travel all the way to the exit. The velocity of air in the tube was selected such that not only do the droplets upon striking the cutting tool effectively wet the cutting zone by spreading but also the air velocity was high enough to carry away the chips from the cutting zone.

Jun et al. [8] used the above mentioned ACF spray system and compared its machining performance with dry cutting and conventional flood cooling by milling
slots in an aluminum workpiece and analyzing the peak-to-valley cutting forces.

Figure 4.13 shows that at feed rate of 0.33 /flute, where ploughing dominates the cutting process, the milling cutter failed after cutting eight slots in one case and five slots in the other when cutting dry. On the other hand, the cutter was able to machine more than fifty slots when the atomization-based cutting fluid application was used. Figure 4.15 shows that for the same feed rate, burrs formed during dry cutting are significantly larger than those formed when the ACF spray system is used.

On comparing the machining performance of flood cooling and ACF spray system it was found that using flood cooling leads to clustering of metal chips near the cutting zone that is instrumental in increasing the surface roughness of the machined surface. However, on using the ACF system no such chips clusters were reported. Jun et al. [8] observed that using flood cooling leads to chipped cutting
Figure 2.9: Peak-to-valley cutting forces at the feed rate of 0.33 µm/flute [8]

Figure 2.10: Photographs of burrs [8]

edges, whereas the tool wear is uniform, without any chipped edges when ACF spray system is used, as shown in Fig. 4.16

Figure 2.11: Photographs of tool wear after cutting 30 slots with flood cooling and 45 slots with atomized fluid [8]
To further evaluate the performance of an ACF spray system as compared to other cutting fluid application methods, Jun et al. [8] studied the metal chips generated during different cutting fluid application methods. Figure 2.12 shows that chips generated during flood cooling do not have uniform sizes and are broken into little pieces, owing to the continuous collision of the cluster of metal chips with the cutting tool. Whereas, when the atomization-based cooling method is applied, the chips are more serrated and segmented/discontinuous which is an indication of more effective cooling of the cutting zone.

Figure 2.12: Photographs of generated chips for the conditions of (a) dry, (b) flood cooling, and (c) atomization-based cooling [8]
Jun et al. [8] also studied the effect of variation of a single spray parameter, namely, droplet velocity on two critical machining parameters, viz., peak-to-valley resultant forces and surface roughness. Figure 2.13 shows that the droplet impingement velocity does not appreciably affect the cutting performance and it can be concluded that as long as the droplet velocity is within the spreading regime, the machining performance is not significantly affected by the droplet velocity.

![Figure 2.13](#)

**Figure 2.13**: Experimental results at different droplet impingement velocities [8]

Nath et al. [9] designed and evaluated an atomization-based cutting fluid spray system in macro-scale turning of titanium alloy. Figure 2.14(a) shows the ACF spray unit for conducting turning experiments. Nath et al. [9] not only conducted experiments to compare the machining performance of the ACF spray system with that of flood cooling but also studied the effect of variation of five spray parameters, namely, cutting fluid flow rate, spray distance, impingement angle,
and type and pressure level of the droplet carrier gas on cutting forces, tool life, and chip characteristics. ACF spray parameters in turning setup are shown in Fig. 2.14(b).

![ACF spray unit and parameters](image)

Figure 2.14: (a) Photograph of the ACF spray unit and (b) ACF spray parameters in turning setup [9]

Nath et al. [9] observed that under the same machining conditions, the tool life obtained using the ACF spray system is 40-50% more than what could obtained using flood cooling. Figure 2.15(a) and 2.15(b) show the metal chips while using ACF spray system and flood cooling, respectively. Using the ACF spray system leads to broken chips whereas flood cooling resulted in continuous long chips. From this observation it can be concluded that the ACF spray system is able to cool the cutting zone effectively, as a result of which the brittleness of the metal chips increases and it breaks easily, leading to broken chips. Due to lack of penetrating capability of cutting fluid in flood cooling, the cutting zone does not get cooled much and the brittleness of the chips does not change significantly, as a result of which application of flood cooling results in long continuous metal chips.
Plots showing the outcome of experiments conducted in [9], to study the effect of variation of various spray parameters on tool life, are shown in Fig. 2.16. It was observed that both air - CO\textsubscript{2} mixture and the N\textsubscript{2} as droplet carrier gas offer about the same tool life. However, the air - CO\textsubscript{2} mixture effectively diminishes smoke from the cutting zone that is produced due to burning of the cutting fluid at elevated cutting temperature. In contrast, the N\textsubscript{2} gas does not help diminishing smoke probably due to high dispensing temperature. Hence, air - CO\textsubscript{2} mixture is preferred as the droplet carrier gas. It was observed that the combination of lower pressure (150 psi), higher cutting fluid flow rate (20 ml/min) and a larger spray distance (35 mm), produces a significantly longer tool.

Rukosuyev et al. [10] proposed that the spray characteristics of the atomization based cutting fluid spray system plays a vital role in improving the performance of the ACF spray system, especially during micro-machining. Since the cutting
zone in micro-machining is small, it is desired to have a narrow and focused spray for effective penetration into the cutting zone. Hence, Rukosuyev et al. [10] studied the effects of the system input parameters (mist and spray velocities) and nozzle designs on the spray characteristics, focus length and focus height, which are shown in Fig. 2.17. The focus height of the spray is the spray diameter at the focal point, and the focus length is the distance from the air jet pipe to the
focal point. The focus length is important because it determines the position of
the nozzle tip with respect to the cutting zone for effective wetting of the cutting
fluids. Hence, in the experiments conducted in [10], focus length and focus height
have been considered as parameters to evaluate the performance of ACF spray
system.

The effect of variation of two aspects of the nozzle geometry, namely, location
of high speed air outlet with respect to the nozzle tip (L_h) and slope of the nozzle
inside (γ_n), on the performance parameters was studied in [10] by considering
different nozzle geometries, shown in Fig. 2.18. Experiments were also conducted
to study the influence of variation of velocities of air in the tube (spray velocity
(V_s)) and the pipe (mist velocity (V_m)), in a configuration similar to Fig. 4.10, on
the performance parameters.

Figure 2.19 and 2.20 show the effect that the variation of nozzle design, spray
velocity and mist velocity have on the performance parameters. It is observed
that best focusing is achieved when the air jet pipe is inside the nozzle and the
nozzle has a slight convergence angle.

Figure 2.17: Spray characteristics [10]
Figure 2.18: Four different nozzle geometries studied [10]

Figure 2.19: Photograph of the spray at different nozzle geometries: (a) $L_h = -10.16$ mm, $\gamma_n = 6^\circ$, (b) $L_h = -10.16$ mm, $\gamma_n = 0^\circ$, (c) $L_h = +10.16$ mm, $\gamma_n = 6^\circ$, and (d) $L_h = +10.16$ mm, $\gamma_n = 0^\circ$[10].

<table>
<thead>
<tr>
<th>$V_m = 1$ m/s</th>
<th>$V_m = 3$ m/s</th>
</tr>
</thead>
<tbody>
<tr>
<td>$V_s = 12$ m/s</td>
<td>$V_s = 12$ m/s</td>
</tr>
<tr>
<td>$V_s = 18$ m/s</td>
<td>$V_s = 18$ m/s</td>
</tr>
</tbody>
</table>

Figure 2.20: Photographs of the spray at mist velocities of 0.5, 1.5, 2.5, and 3.5 m/s and spray velocities of 11.0 and 21.0 m/s [10].

Experiments were further conducted in [10] to gain deeper understanding of the effect of variation of spray velocity and mist velocity on the system’s performance parameters. Figure 2.21(a) shows that for a particular spray velocity, an increase
in mist velocity leads to an increase in focal length and height. In contrast, the variation of spray velocity for a particular mist velocity does not seem to influence the focus height and length much when the mist velocity is less than 1.5 m/s, as shown in Fig. 2.21(b). However, when the mist velocity is higher than 1.5 m/s, an increase in the spray velocity decreases both the focus length and height.

![Graphs showing focus length and height at different mist and spray velocities](image)

Figure 2.21: Effect of variation of air velocities [10]

2.3 Single droplet dynamics

2.3.1 Droplet atomization

Generation of sprays is quite a critical step towards ensuring that the spray full fills the required objectives. Atomization of liquid that leads to the generation of sprays could be broadly classified in two groups [11]. In the first group are systems that generate droplets based on the shearing properties of co-flowing jets. Such systems rely upon large velocity differences between the liquid jet and surrounding medium, to induce surface instabilities and droplet peeling from the jet surface.
Two major drawbacks of using such a system are that the distribution of droplet sizes is not uniform and high pressures are required to create such sprays. Figures 2.22(a) and 2.22(b) show a couple of examples of such types of atomizers. In the second group of sprays, liquid atomization is achieved by using external excitation methods to induce dynamics instabilities of the liquid surface that eventually lead to the breakup of the liquid surface, resulting in the generation of droplets. The unique feature of externally excited spray system is the generation of a quasi-monodisperse spray [11]. Figure 2.22(c) shows an example of such type of a spray.

![Figure 2.22](image_url)

(a) Mechanical atomizer  (b) Air-blast atomizer  (c) Ultrasonic atomizer

Figure 2.22: Operating principles of different types of atomizers [11]

Droplets generation in the ACF spray system could be best achieved by ultrasonic excitation [8, 9]. Ultrasonic excitation leads to generation of droplets that are not only of equal sizes but also the size of the droplets could be easily varied by controlling the oscillation frequency [11]. Furthermore, the design of the ACF spray system can be compact and consume less energy for operation because unlike a shearing atomizer, an ultrasonic atomizer does not require high pressure pumps for generating droplets [11]. An ultrasonic atomizer works on the principle of resonating a surface by using acoustic waves, generated by a piezoelectric
element [12]. The liquid to be atomized is delivered to the vibrating/resonating surface and due to the accumulation of surface waves on the free surface of the liquid, the liquid surface eventually undergoes a uniform breakup process, resulting in monodispersed spray [13]. A typical diagram of an ultrasonic atomizer is shown in Fig. 2.23.

![Figure 2.23: Schematic of an ultrasonic atomizer [12, 13]](image)

2.3.2 Single droplet impingement dynamics

The phenomena of drop impact with surfaces is widely encountered in different scenarios like, spray cooling, ink-jet printing, raining and spray painting, to name a few. Understanding of the fluid mechanics behind drop impact holds the key to not only improve processes encountered in engineering fields but also to explain mechanism behind some of the phenomena observed in non-engineering fields, for
example, atmospheric and oceanographic sciences study the phenomena connected with the interaction of rain drops with the surface of the ocean [14]. Single droplet impingement refers to a liquid drop striking a dry or wet surface. The fate of a droplet after impacting a surface would depend on droplet and surface characteristics. Hence, studying the dynamics of the droplet, before, during and after impingement, is crucial step in controlling the processes that involve droplet impingement on a surface. There are a number of parameters that needs to be taken into account to predict the outcome of a drop impact [14], as shown in Fig. 2.24

Most of the theoretical and experimental studies that are conducted, assume that the droplets are spherical in shape [14]. For the ACF spray system the droplet impact on the surface can either be normal or oblique and it would depend on the orientation of the ACF spray unit with respect to the impinging surface. During the initial stages, the droplets coming out of the ACF spray unit would impinge on a solid surface, however, after passage of some time, the droplets would be impinging on a thin liquid film that has been formed due to previous droplets impingement.

The various impingement regimes identified for droplets impacting on a dry or wet surface are stick, rebound, spread and splash, as shown in Fig. 2.25 [71]. On the basis of studies conducted by Wachters et al. [72], Levin et al. [73] and Stow et al. [74], it was suggested that parameters that affect droplet impingement
dynamics are incident droplet’s diameter \( (d_o) \) and normal velocity \( (u_o) \), liquid viscosity \( (\mu) \), density \( (\rho) \) and surface tension \( (\sigma) \). The parameters characterizing the conditions of the receiving surface, such as film thickness \( (h_f) \) for wet surfaces also play an important role in controlling the outcome of droplet impingement \cite{71}.

Non-dimensional numbers, formed by combining the above mentioned param-
Figure 2.25: The various impingement regimes identified for a droplet. [15]

Parameters, that have been identified to be the most critical in predicting the outcome of droplet impingement are:

\[ W_e = \frac{\rho u_0^2 d_0}{\sigma}, \quad Re = \frac{\rho u_0 d_0}{\mu}, \quad Oh = \frac{\mu}{\sqrt{d_0 \sigma \rho}}, \quad h_{nd} = \frac{h_f}{d_o}, \]  \( (2.1) \)

where, \( W_e \) is the Weber number and it represents the ratio of the droplet kinetic energy to the droplet surface energy, \( Oh \) is the Ohnesorge number and is used for relating the viscous forces to the inertial and surface tension forces, \( Re \) is the Reynolds number and it is the ratio of the inertial to the viscous forces for a droplet and \( h_{nd} \) is the non-dimensional film thickness number [75]. These non-dimensionless numbers could be used for predicting the impingement regime for a droplet impacting on a surface.
2.3.3 Single droplet impingement regimes

**Stick Regime**

The droplet is in this regime when it adheres to the surface in a nearly spherical shape and this happens when the impact energy of the droplet is extremely low and the temperature of the wall is below pure adhesion temperature [15]. For a wet surface, Jaya et al. [76] studied the sticking regime by conducting experiments using water droplets, wherein the droplet radius, velocity and angle of impact were variable parameters. After analyzing the experimental results, [76] concluded that sticking of water droplets would occur for $\text{We} < 5$ [15].

**Rebound Regime**

The characteristic feature of the rebound regime is that the droplet will bounce off the surface because of low impact energy. The air layer trapped between the drop and the impinging surface causes low energy loss resulting in bouncing [15]. Based on the experimental investigations carried out by Stow et al. [74] and Rodriguez et al.[77], the transition criteria for this regime is given by $5 < \text{We} < 10$.

**Spreading Regime**

This regime is similar to sticking regime but droplets that spread have a greater impact energy than the droplets that only stick. Droplets would spread on a surface if $\text{We} > 10$ [15].

**Splashing Regime**
Droplets would be categorized under this regime if they have a very high impact energy. Characteristic feature of splashing droplets is that upon impact a crater is formed with a crown at the periphery where liquid jet becomes unstable and breaks up into many secondary droplets [15]. Based on the experimental study conducted by Yarin et al. [78] and Mundo et al. [19], a couple of transition criteria for splashing regime have been proposed as follow:

\[
W_e > 324d_o \left( \frac{\rho}{\sigma} \right)^{1/2} u_o^{1/4} f^{1/4},
\]

(2.2)

\[
K_y > 57.7; \quad K_y = Oh \ast Re^{1.25},
\]

(2.3)

where, \( f \) is the frequency of impinging droplets and \( K_y \) is a non-dimensional number formed by combining Reynolds and Ohnesorge numbers.

Depending on the application, the impinging droplets could be controlled to be in one of the above mentioned four regimes, by varying the parameters that constitute the non-dimensional numbers. Jun et al. [8] in their study pointed out that the objective of the droplets coming out the ACF spray unit is to uniformly wet the surface of the tool by spreading upon impinging on the surface. Hence, the droplets coming out of the ACF spray system should satisfy two criteria, viz., \( W_e > 10 \) and \( K_y < 57.7 \). Furthermore, it should be noted that the above mentioned criteria has been derived considering droplets are impacting on a stationary surface, however the surface involved in the present study is a rotating end-mill tool and the above mentioned criteria should be applied with some caution.
2.4 Single droplet spreading behavior on a stationary surface

Spreading of droplets upon impact on a surface have been studied, both numerically and experimentally, due to its relevance in industrial applications. Moreover, as mentioned in Section 2.3, efficient functioning of the ACF spray system relies on ensuring that the impinging droplets are in the spreading regime. Hence, it is critical to study droplet spreading behavior.

2.4.1 Experimental study

Riboo et al. [16] experimentally studied the temporal development of a spreading film formed due to normal impact of single drops on a dry as well as wet surface. To simplify the analysis, Riboo et al. [16] defined non-dimensional time \( t^* \) by combining the droplet impact velocity \( V \) and initial spherical diameter \( D \) with time \( t \) \( (t^* = t(V/D)) \). Another non-dimensional number, spreading factor \( d^* \) was defined as the ratio of spread diameter \( d \) and \( D \). If the droplet is in the spreading regime, then the temporal evolution of the spread factor can be divided into four distinct phases: the kinematic phase, the spreading phase, a relaxation phase and a wetting/equilibrium phase, as shown in Fig. 2.26 [16].

Parameters that were varied by Riboo et al. [16] in the experiments could be divided into two types. First, properties related to the liquid droplets, viz.,
Figure 2.26: Schematic representation of the spread factor with time. The different lines correspond to an arbitrary choice of possible spreading histories, depending on the parameters of the impact [16].

droplet impact velocity, diameter, viscosity and surface tension, and second, characteristics of the droplet receiving surface, namely, wettability and roughness of the surface ($R_a$). In the experiments reported in [16] it was observed that the kinematic phase was similar for all experimental for all experimental conditions and simple scaling according to the droplet impact velocity and initial diameter could take care of any observed variation in droplet spreading. However, the spreading phase was significantly affected by the variation of the droplet and receiving surface parameters, and the difference in spreading characteristics can not be accounted for by considering just the Weber and Reynolds number [16]. Furthermore, it was concluded in [16] that surface wettability and roughness play a major role in temporal evolution of the spread factor, as seen in Fig. 2.27 and 2.28, respectively.
2.4.2 Numerical study

Numerical modeling of spreading of a film formed by the impact of droplets is quite challenging because of the complex fluid dynamics involved in the process [79]. As a result many researchers have studied the spreading behavior of a single droplet, as a first step towards modeling the spreading behavior of a spray. Fukai et al. [79] used conservation of mass and momentum under the Lagrangian approach to simulate the impact of a liquid droplet on a solid substrate. The model took into account the effect of surface tension on droplet spreading. The numerical
model in [79] was used to evaluate the effects of impact velocity, droplet diameter, surface tension, and material properties on the fluid dynamics of the deforming droplet. The model successfully predicted the droplet spreading and was also able to demonstrate droplet recoiling.

Ghai et al. [26] modeled a single droplet spreading on a dry rotating surface with an objective of designing an efficient atomization-based cutting fluid (ACF) system for micro-machining purposes. In [26], modeling approach based on conservation of volume and energy is used to predict the spreading behavior of a droplet impinging on a rotating surface. Ghai et al. [26] developed a spreading model, which would predict the droplet spread and height, by parameterizing the droplet shape to obtain a 3-D equation of the droplet at the time of maximum spread. Although the above models are able to capture accurately the single droplet spreading on a stationary or rotating surface, however such models are incapable of simulating the film formation by sprays, where multiple drops are impacting on a dry or wet surface.

Arienti et al. [17] modeled droplet impingement on a surface, where droplets were generated due to cross-flow atomization. The numerical model was developed by modifying the continuity and momentum equations to take into account the different regimes a droplet could be in after impingement. Furthermore, the numerical model utilizes experimental data to modify the governing equations and is based on direct numerical simulations. Figure 2.29 shows the result of numer-
ical simulation, along with experimental observation, of droplets impinging on a surface having a thin film of liquid. Although the model in [17] is able to simulate droplet spreading due to successive droplet impingement, the computational time is excessive because the model used semi-implicit scheme for simulations. Furthermore, the model presented above does not have provisions for accommodating the additional ACF parameters (carrier gas pressure) that would affect the droplet impingement dynamics.

2.5 Behavior of droplets impinging on a rotating surface

In the literature, most of the experimental and numerical studies related to droplet impingement have been performed on stationary surfaces. However, there are many practical situations where droplets coming out of a spray are impacting rotating surfaces, for example spray cooling of a grinding wheel or using the ACF
spray system in milling operations. It is suggested that the presence of a rotating surface could alter the droplet impingement dynamics and hence, the relations obtained for droplet impingement on a stationary surface could not be applied directly. Therefore, there is a need to study behavior of droplets impinging on a rotating surface.

2.5.1 Droplet impingement dynamics

The study of the interaction of impinging droplet with a boundary layer on a surface of a rotating disk, for droplet diameters \(d_d\) in the range of 0.3-4 mm and droplet velocities \(v_d\) varying from 0.1-10 m/sec, was studied by Povarov et al. [18]. For the experiments performed in [18], three different types of interaction of the droplet with the rotating disk was observed: 1) the drop is captured by the disk on contact and spreads out on the surface; 2) the drop upon impact enters the boundary layer of the disk, gets slightly deformed, and is partially spread out over the surface and partially reflected from it; 3) the drop is strongly deformed in the boundary layer and, without touching the surface, is reflected away from it.

In the study, [18], it was concluded that for a given droplet impact velocity and initial diameter, the azimuthal velocity \(u\) of the disk, plays a major role in deciding which one of the above mentioned interaction a droplet would have with the rotating disk. The first form of interaction corresponds to low values of \(u\).
Under such a scenario, the drop passes through the boundary layer undeformed and comes in contact with the surface, gradually spreading out in the direction of rotation of the disk. On increasing the value of \( u \), second form of interaction is observed, as shown in Fig. 2.30(a). In this form of interaction, as the drop approaches the disk, it gets slightly deformed and the lower portion of the drop, upon coming in contact with the disk gets carried away with it, while the upper portion of the drop remains undeformed (frames 1-3). The bottom part of the drop in contact with the disk, gets spread out but the rearward portion of the drop does not undergo any spreading and this leads to the formation of a velocity gradient in the boundary layer. As a result of which, a wedge of air forms under the drop, which begins to rise (frames 4-8) and this provides sufficient lifting force to separate a portion of the drop from the disk surface (frames 9-12) and project it as a parabola towards a second collision with the surface [18]. The third form of interaction is observed on further increasing the value of \( u \), as seen in Fig. 2.30(a). Under such condition, the drop gets significantly deformed upon entering the boundary layer because of the combined effect of the dynamic pressure difference exerted on the drop by the flow gradient and the reduction in static pressure close to the surface of the rotating disk (frames 2-3). The lower part of the drop gets displaced in the direction of rotation of the surface, and under the action of the increasing lift force begins to rise above the surface and proceeds to take a streamlined form (frames 4-7). The center of application of the aerodynamic force (the pressure center) does not coincide with the center of mass of the drop,
which leads to spatial rotation of the drop (frames 8-12) at some angular velocity [18].

![Figure 2.30: Drop trajectory and film frames for collision of drop (d_d = 2 mm, v_d = 1.2 m/sec) with surface of rotating disk [18]]

(a) u = 30 m/sec  (b) u = 60 m/sec

In [18], it was concluded that the pattern of interaction between drop and rotating disk remains the same for different drop sizes investigated in the study. The boundaries of different form of interactions for different droplet diameters are shown in Fig. 2.31. The boundary between interactions of types I (complete adherence) and II (partial reflection), is independent of the drop size. Type-I collisions were observed over the whole range of d_d, when V_d/u < 0.15. The position of the boundary between types II and III (complete reflection) is determined by the drop size.
Mundo et al. [19] studied droplet impingement on a rotating disc, with the aim of formulating an empirical model describing the deposition and the splashing process. This study has been performed for different initial droplet diameters ($60 < d_o < 150 \, \mu m$) and velocities ($12 < w < 18 \, m/s$). Splashing was observed for droplets having high Reynolds number, as shown in Fig. 2.32. The schematic view of the splashing process is shown in Fig. 2.33. Mundo et al. [19], described splashing to happen because once the lower half droplet has undergone deformation, the total volume flow rate into the wall film begins to decrease. As a result of which, the corona that was formed around the deforming droplet, after having been stretched in the radial direction, also now has less fluid feeding the film and hence becomes thinner. An instability develops and leads to a circumferential wreath which propagates upward in the corona and finally results in a disintegration into secondary droplets. However, droplets having lower Reynolds number,
spreads after getting deposited on the surface, as seen in Fig. 2.34. The schematic view of the deposition process is shown in Fig. 2.35. Droplet having low Reynolds number, just spreads around the point of impact and corona formation does not take place because most of the kinetic energy with the droplet gets dissipated during the deformation process and little or no momentum normal to the wall exists for corona formation.

Figure 2.32: Splashing of a liquid droplet with Re = 598.8 [19]

Figure 2.33: Schematic view of the splashing process. [19]

Figure 2.34: Splashing of a liquid droplet with Re = 251.4 [19]

Figure 2.35: Schematic view of the deposition process. [19]
Based on experimental observation Mundo et al. [19], obtained a correlation between Reynolds number (Re) and Ohnesorge number (Oh), as \( K = OhRe^{1.25} \), to define limits of deposition and splashing of droplets (normal velocity component of the droplets should be used for calculating dimensionless numbers). A value of \( K \) exceeding 57.7 leads to incipient splashing, whereas \( K \) less than 57.7 leads to complete deposition of the liquid, as illustrated by the results presented in Fig. 2.36 [19].

![Figure 2.36: Limits for splashing and deposition of primary droplets. [19]](image)

2.5.2 Droplet spreading behavior

Chen et al. [20] studied the effect of tangential velocity on the outcome of water drop impingement on a rotating cylindrical teflon surface. The significant parameters that were varied in the study were the drop diameter (500 \( \mu m < D < 900 \mu m \)), drop speed (1 m/s < \( V < 3 \) m/s) and cylinder rotational speed (0 < \( \omega < 280 \) rpm). The coordinate system used for this study can be seen in Fig. 2.37.
To simplify the analysis, some parameters based on the geometrical relation were defined as,

\[ V_n = V \cos \theta, \]  
\[ V_t = \omega R - V \sin \theta, \]

where, \( V_n \) is the impact velocity of the droplet in the \( n \)-direction (normal) and \( V_t \) denotes the impact velocity of the droplet in the \( t \)-direction (tangential). Based on the above mentioned parameters two dimensionless numbers were defined as below:

\[ We_n = \frac{\rho V_n^2 D}{\sigma}, \]  
\[ We_t = \frac{\rho V_t^2 D}{\sigma}, \]

where, \( We_n \) represents the ratio of the \( n \)-direction collisional energy to the surface energy of the drop before impact and \( We_t \) represents the ratio of the \( t \)-direction collisional energy to the surface energy of the drop before impact.

Figure 2.37: Variables and coordinates system. [20]
For the experiments conducted in [20], three different impact patterns were observed, which are shown in Fig. 2.38 and the regimes of these patterns are plotted in Fig. 2.39, as a function of $W_e_n$ and $W_e_t$. Show in Fig. 2.38(a), partial rebound of the drop results from close-to-normal impacts with medium to high Weber numbers. The drop on impinging the surface, spreads out radially into a rather axi-symmetric disk. The retraction of the disk, upon reaching the maximum spread, generates an upward internal flow, which stretches the end of the liquid rod into one drop jumping off the surface; while leaving a portion of the liquid on the surface. Deposition pattern refers to an impact in which the drop simply spreads and retracts into one drop sticking to the surface, as seen in Fig. 2.38(b). Deposition happens in low-energy impacts or impacts with a medium tangential Weber number. Finally, split deposition denotes an impact which results in the drop gets split into two droplets, as shown in Fig. 2.38(c). This type of impact happens at a high tangential Weber number; the high tangential speed stretches the drop into an elliptical disk, if the elongation is sufficient, the retraction of the disk would generate a necking in the middle to produce two droplets. As mentioned before, functioning of the ACF spray system relies on the spreading of droplets, after it gets deposited on the surface. For the range of parameters studied in [20], it can be concluded that droplets coming out of the ACF spray system should have medium tangential Weber numbers in order to be in the spreading regime.

Recognizing the importance of droplet’s spread area for different industrial ap-
(a) Partial rebound ($W_{en} = 30, W_{et} = 0$)

(b) Deposition ($W_{en} = 30, W_{et} = 60$)

(c) Split deposition ($W_{en} = 30, W_{et} = 110$)

Figure 2.38: Change of impact outcome with increasing $W_{et}$ for $W_{en}=30$ [20]

Figure 2.39: Drop impact regimes. [20]

Based on experimental observations, maximum spread area for the normal impacts was estimated to be in the order of $\pi d_m^2$, where $d_m$ is the maximum spreading diameter for normal impacts. Similarly for drops with tangential speed, the maxi-
imum spread area was estimated to be in the order of $\pi d_m b$, if the drop is assumed to have taken an elliptical disk shape at the maximum spread with the axes $d_m$ and $b$. A new parameter, dimensionless excess spread area ($X_A$), given by $(b d_m - d_m^2)/D^2$, is defined and its value as a function of $We_t$ is plotted, as shown in Fig. 2.40. From the plot, it can be concluded that in general, higher $We_t$, induces more dimensionless excess spread area due to higher energy dissipation. By performing linear regression of the plotted data, a linear relation between $X_A$ and $We_t$ was obtained, which is mathematically given by

$$X_A = (d_m/D)^2 (b/d_m) = 0.0226 We_t.$$  \hspace{1cm} (2.8)

The experimental study undertaken by Chen et al. [20], successfully established that the droplet impingement dynamics gets affected by the presence of a
rotating surface, by conducting experiments using both stationary and rotating surface. However, in [20] no attempt was made to quantify the droplet spread area as function of droplet velocity and surface speed. Furthermore, it is debatable if the study in [20], can be directly applied to evaluate the spreading behavior, of the film formed by impinging droplets, of a spray based system. The impingement of droplets was carried out on a dry surface for the experiments presented in [20], however, droplets coming out of a spray are expected to impinge on a rotating surface that already has some liquid film present on it. Droplet impingement dynamics, specially spreading, is bound to get affected by the presence of a liquid film [14]. Droplets coming out of a spray based system undergo complex interactions with each other as well as with the gas mixture which carries them to the rotating surface. As a result of which, unlike the situation presented in [20], condition of the droplet before impinging the surface is not easily defined in terms of a fixed droplet diameter and initial impact velocity.

2.6 Performance evaluation of spray-based systems

2.6.1 Film thickness models

Sprays are used in a wide number of industrial and technical applications, such as direct injections in diesel engines, cutting fluid dispensation in metal cutting, spray cooling, spray painting and coating, to name a few, wherein the
spray is impinging on a surface. To ensure that the spray is able to achieve the desired functionality, it is critical to study and monitor a few parameters after the spray has impinged onto the surface. One such important parameter is the film thickness of the liquid film that is formed after the droplets impinge on a surface. In spray cooling, the prediction of average film thickness and average velocity is very important because these parameters significantly affect the efficiency of heat transfer in the sprayed surfaces [22]. In micro-machining processes, the thickness of the film, formed by a spray dispensing cutting fluid, determines whether the liquid film is able to access the tool-chip interface in order to cool and lubricate the cutting zone [9]. In direct injection diesel engines, fuel spray impingement and fuel film formation influences the engine performance and emissions [15]. The characteristic of liquid film formed by a spray, gets significantly influenced by the dynamics of the impinging surface. Hence, the approach for modeling spray impingement on a stationary surface might be different from that on a rotating surface.

2.6.1.1 Stationary flat surface

Modeling of spray impingement on a stationary flat surface, is well documented in literature [15, 22, 21, 80] because this situation is widely encountered in different industries. However, the modeling approach might vary depending on the accuracy and complexity of the problem.
The most common approach for modeling fluid films created by impinging fuel sprays is to utilize the continuity equations for mass and momentum in conjunction with the energy equation. Stanton et al. [15] formulated and validated a multi-dimensional, fuel film model to help account for the fuel distribution during the combustion in internal combustion engines. The fuel film model took into account the major physical processes that affect a liquid film, as shown in Fig. 2.41(a), and these were: mass and momentum variation caused due to spray impingement, splashing effects, shear forces on the film, convective heat and mass transfer and flow separation.

![Figure 2.41: Schematics of: (a) a typical wall film cell used in the formulation of the film model; (b) the major physical phenomena governing film flow [15]](image)

The thin fuel film flow on a surface was modeled by solving the continuity, momentum and energy equations for each and every wall film cell as shown in Fig. 2.41(b). By integrating across the film thickness and using 'thin film' assumptions, the equations are reduced to a 2-D film flowing across a 3-D surface. After integrating in the film normal direction the continuity equation gets transformed...
into the following form:

$$\frac{\partial \delta}{\partial t} + \frac{1}{A_{wall}} \sum_{i=1}^{N_{side}} (V_f \cdot \hat{n})_i \delta_i l_i = \frac{S_d}{\rho_l A_{wall}} - \frac{\dot{M}_{vap}}{\rho_l A_{wall}}$$ \hspace{1cm} (2.9)$$

where $A_{wall}$ is the wall area, $V_f$ is the film velocity, $l_i$ is the length of side $i$, $\rho_l$ is the film density, $\delta_i$ is the film thickness at side $i$, $S_d$ is the source term, $\hat{n}$ is the normal vector to a given entry or exit side for an arbitrary fluid cell, and $\dot{M}_{vap}$ is the rate of fuel vaporization. In Eqn. 2.9, $S_d$ is the source term and it accounts for the mass flux of drops that impinge upon the film or the secondary droplets that leave the film which results from splashing. The specific value of $S_d$ is determined based on the impingement regimes of the droplets [15]. Furthermore, Eqn. 2.9 is used to calculate the temporal evolution of film thickness in a given cell.

The momentum equation for film modeling is given by:

$$\frac{\partial (\delta V_f)}{\partial t} + \frac{1}{A_{wall}} \sum_{i=1}^{N_{side}} V_f (V_f \cdot \hat{n})_i \delta_i l_i \phi_i = -\sum_{i=1}^{N_{side}} (P \hat{n})_i \delta_i l_i + g \delta + \frac{\dot{M}_{tang}}{\rho_l A_{wall}} + \frac{\sum_{i=1}^{N_{edge}} (\tau A_i)}{\rho_l A_{wall}} + a \delta.$$ \hspace{1cm} (2.10)$$

In Eqn. 2.10, the first term on the left is the time derivative of the film momentum per unit area. The second term denotes convective momentum and is approximated using the two equations stated below:

$$\int_{L_1}^{L_2} \int_{0}^{\delta} V_f (V_f \cdot \hat{n}) d\tilde{x}_3 d\tilde{x}_1 \approx \sum_{i=1}^{N_{side}} V_f (V_f \cdot \hat{n})_i \delta_i l_i \phi_i$$ \hspace{1cm} (2.11)$$
where,
\[
\phi_i = \frac{1}{1 - \frac{\delta_t}{\delta}} - \frac{\Theta}{\delta} \left( \frac{1}{\delta - \delta_t} \right)^2.
\] (2.12)

Equation 2.12 is a result of approximation of the integration of the non-linear convective terms in the cross-film direction. The displacement thickness, \(\delta_t\) and the momentum thickness, \(\Theta_t\), in Eqn. 2.12 can be calculated once the velocity profile in the cross-film direction has been chosen [15].

The third term in Eqn. 2.10 is the pressure term and it accounts for the gas pressure as well as the pressure generated due to droplet impingement and splashing. The fourth term in Eqn. 2.10 accounts for the gravity effect in liquid film flow and becomes significant when the film is flowing over an inclined or vertical surface. Fifth term in Eqn. 2.10 takes care of addition of tangential momentum to the liquid film due to droplet impingement and splashing. Finally, the sixth term in Eqn. 2.10 is the viscous film term and accounts for the shear forces acting on the fluid film at the wall and at the fluid-gas boundary. Seventh term in Eqn. 2.10 is relevant only for internal combustion engines and hence not discussed as a part of this study.

Stanton et al. [15] developed and implemented the fluid film model in order to study the drop interaction process, impingement regimes and post-impingement behavior. The mathematical model was implemented in a KIVA-II computer code. Experimental data from previous studies was utilized to validate the fluid film
model. Figure 4.11 shows the variety of information that can be extracted from a model of this framework including, spray characteristics, fluid film thickness, and amount of fuel adhered to the impact surface.

![Figure 4.11](image)

Hoyne et al. [21] developed an analytical 3D thin fluid film model, for the ACF spray system, based on the Navier-Stokes equations for mass and momentum, in order to characterize the thin fluid film that is formed by impinging droplets when the ACF spray system is used for turning operations. The thin fluid film was characterized, in terms of thickness and velocity for a set of ACF spray parameters. The mathematical model was simulated using a MATLAB script and the
numerical values of film thickness and film velocity at different locations from the point of impact was validated by comparing them to experimental values. Figure 4.12 shows the predicted film velocity profile for an impingement angle of $35^\circ$ at various distances from the impingement point (DIP) and various perpendicular offset distances (POD).

![Graph showing film velocity profile](image)

Figure 2.43: Fluid film velocity at 9.6 ms for impingement angle of $35^\circ$[21]

The model developed by [21], is instrumental in explaining the cooling and lubrication mechanism at the tool-chip interface because of the fluid film formed by an ACF spray system during turning operations.

Although numerical models based on the conservation equations of mass and momentum are widely used and offers great amount of flexibility, several other fluid film modeling methods have been proposed in the literature. Kalantari et al. [22] developed a new empirical model, based on data obtained after conducting an experimental study of spray impact onto horizontal flat and rigid surfaces, to
correlate average fluid film thickness to parameters of the impacting spray, namely, normal and tangential component of impact velocity, volume-averaged diameter of impacting droplets \((d_{30b})\), dynamic viscosity of liquid used in spray and also the boundary condition of the target; average target surface roughness and target size and shape. The empirical spray impact model takes into account the presence and influence of an accumulated wall film. Also, the model is accurate as it is based on mean statistics over many events and not on the outcome of single drop impact experiments. The model developed by Kalantari et al [22] is valid for Weber number range of \(10<\text{We}<160\), for which the impinging droplets are in the spreading regime. Figure 2.44 shows the comparison between the film thickness values obtained using experiments and empirical model, for different impact Reynolds number \((\text{Re}_{nb})\).

![Figure 2.44: Variation of the average film thickness with the impact Re-number](image)

Mundo et al. [81] developed a droplet-wall impingement model to calculate near-wall polydisperse spray flows. The gas phase of the spray was modeled using
the Eulerian approach where as the droplets were modeled using the Lagrangian approach. The model could evaluate the maximum film thickness height if all of the impinging droplets are deposited by assuming a fully developed, steady-state film flow under gravity, neglecting shear forces due to air flow on the film surface. Mundo et al. [81] formulated a mass conservation equation to calculate the maximum film thickness height by numerical integration of the equation. This model has many limitation including the inability to predict the fluid film velocity, the exclusion of surface tension, and the exclusion of droplet-droplet interactions. Furthermore, the film thickness model only offers a one-dimensional representation of the fluid film, as it only predicts the maximum film thickness height.

2.6.1.2 Rotating surface

In literature it was found that only a few studies being conducted to model the interaction between the droplets coming out of the spray and a rotating surface. Such a scenario is encountered when sprays are used in machining operations such as milling, grinding and drilling.

Boughner et al. [23] prepared a 2D probabilistic model to study the characteristic of microfilm formed on a rotating cylindrical surface, and studied the effect that the variation of system parameters would have on the film formation for an Atomization-based cooling system. Parameters investigated in the model were fluid and mist properties (surface tension and droplet size) and system parameters
Figure 2.45: Film thickness variation with (a) tube air velocity (b) droplet velocity [23]

(delivery tube air velocity, spray air velocity, spray geometry, cylinder diameter and cylinder rotational velocities). The response provided by the model were the time to creation of a continuous microfilm on the cylinder surface and thickness of that film with respect to time.

Figure 2.45(a) shows the film thickness as a function of air tube velocity. It was observed that, for a given surface velocity, the film thickness reaches a maximum value for tube air velocity at 8 m/s and 9 m/s, when mean droplet size is 7.8 µm, and film thickness then decreases if the tube air velocity either increases or decreases beyond this range. This observation was explained by the fact that the Weber number of droplets are within the most favorable range for film formation when the tube velocities vary between 8 and 9 m/sec, however anything beyond this range lead to either splashing or sticking of droplets, which is not favorable for film formation. Figure 2.45(b) shows the effect of droplet velocity on the film thickness. It was observed that with the increase in droplet velocity the steady
state film thickness decreases. The above observation was explained on the basis of increased evaporation rate, as a result of high droplet velocities. Although the model is quite informative, there are a few shortcomings. First, the model is 1D and as such neglects the film dynamics in other spatial directions, which could lead to accumulation of errors. Second, the model utilizes empirical data that has been generated by [82] and as such the model is restricted in its application. Third, the model does not take into account the presence of a previously accumulated fluid film on the rotating surface and as such is unable to account for the dynamics of droplets impinging on a fluid film.

Duchosal et al. [24] studied the oil mist behavior and fluid film formation on a smooth surface. The surface was a part of the inner channels of the milling tool. The simulations were performed using STAR CCM+ and complex models such as motion reference frame, langrangian particle tracking in a eulerian continuous phase and a special wall interaction model were used. The most important response of the model was the variation of the liquid film imprint on the flat surface, as the system parameters were varied.

Duchosal et al. [24] studied the effect of variation of rotational velocity and inlet pressure of MQL jet on the imprints created by the oil mist on the tool. Figure 4.2 shows the variation in the imprints for different inlet pressure for a tool rotating at 15,000 rpm. To predict the time over which the imprint area becomes uniform, 0.3 μm film thickness criteria was selected. The impingement analyses predicted
better lubrication when high inlet pressure were used, especially in high speed machining.

2.6.2 Lubrication models

The liquid film formed due to impingement of the droplets on a stationary or rotating surface not only leads to effective cooling of an heated surface but its presence between two sliding surfaces can reduce the friction between the two surfaces. Fluid film lubrication is well studied and characterized for different types of bearings. However, the literature available on lubrication mechanisms and models, when a spray based system is used in machining, is quite limited.

Machado et al. [25] conducted experiments to investigate the effect of applying water and soluble oil along with compressed air stream, in an attempt to
reduce the cutting fluid consumption that is associated with using conventional flood cooling. Machado et al. [25] in their study have concluded that the type of lubrication encountered in traditional metal cutting is unlikely to be hydrodynamic lubrication, which is often found in bearings, because in bearings the load is supported by a film of lubricant that is formed as a result of dragging of the fluid film into a tapered gap. However, this situation is unlikely to be found in metal cutting because the chip flow will move the film of lubricant away from the area where it is required, as shown in Fig. 2.47. Hence the type of lubrication encountered in metal cutting could likely be boundary or elastohydrodynamic lubrication, depending on the lubricant film thickness.

Figure 2.47: Showing the unlikely occurrence of hydrodynamic lubrication [25]

Han et al. [83] used water vapor as a coolant and lubricant for machining and based on experimental observations concluded that, water vapor possesses better lubricating action, as as compared to other cutting fluid application systems, because of the excellent penetration capability and the low lubrication layer shearing strength of water vapor that helps in reduction if the cutting forces. Han et al. [83] in their study proposed that the water vapor accesses the cutting zone through
capillary action, which is contrary to the mechanism suggested by Machado et al. [25], wherein the formation of a barrier layer at the tool chip interface has been attributed as the lubricating mechanism.

As stated earlier, the presence of a thin viscous fluid film between two surfaces can result in the reduction of friction as one surface slides over the other. However, in such a situation the liquid film thickness influences the pressure differences between the two surfaces that is instrumental in keeping the surfaces apart. Langolis et al. [84] made an attempt to study the relationship between the thickness of the liquid film and the pressure force generated by it. They assumed a couette flow between two plates sliding over each other with a liquid film in between, as shown in Fig. 2.48 and derived the expression of lubrication force generated by the liquid film is given by,

$$f = \frac{6\mu l^2 U}{(h_1 - h_2)^2} \left[ \ln\left(\frac{h_1}{h_2}\right) - 2\left(\frac{h_1 - h_2}{h_1 + h_2}\right) \right],$$  

(2.13)

where, $h_1$, $h_1$, $l$ are shown in Fig. 4.20, and $U$ is the velocity of the sliding surface.

Ghai et al. [26] developed a lubrication model, by modifying the model framework of Langolis et al. [84], to study the lubrication performance of the ACF based spray system in micro-machining processes. The magnitude of lubrication force generated by a single droplet was given by:
where, $u_s$ is the surface velocity, $h$ is the droplet height, $d_1$ and $d_2$ denote the major and the minor axes of the droplet in its top-view, respectively, at the end of the spreading phase, as shown in Fig. 2.49. The magnitude of lubrication force denotes the extent to which the droplet is able to reduce the friction in the cutting zone. High lubrication force denotes large reduction in friction, which would lead to lower values of cutting forces. Hence, Ghai et al. [26] concluded that higher spreading of droplets, lead to greater lubrication forces.
2.7 Gaps in knowledge

Survey of the previous literature, shows that the atomization-based cutting fluid (ACF) spray system has been proven to effectively cool and lubricate the cutting zone during micro-machining and macro-scale turning of Ti-6Al-4V, leading to significant improvement in machinability of titanium alloys, while consuming significantly less amount of cutting fluid as compared to flood cooling. While the ACF spray system has been shown to be productive for certain types of machining operations, there is a lack of experimental or numerical study to evaluate the efficacy of the ACF spray system for machining operations that are not in the micro-scale range or are different from macro-scale turning, like milling.

First, there is a lack of experimental investigations conducted to compare the machinability performance of the ACF spray system to that of other cutting fluid application methods, viz., dry cutting and flood cooling. Even though Nath et al. [9] compared the machinability of of macro-scale turning of titanium alloy for different cutting fluid application methods, namely, flood cooling and ACF
spray system, the conclusions drawn from that analysis could not be directly applied to milling. There are three main challenges unique to milling; First, the droplet impingement dynamics gets affected by the presence of a rotating milling tool and it would not be that same as it were for a stationary tool, Second, the fluid film formed on the milling tool would be non-uniform with variable momentum and it would be quite challenging to achieve fluid film penetration at the tool-chip interface all throughout the time the cutting edge of tool is engaged with the work piece, Third, milling is an intermittent cutting process that gets affected by thermal shock caused by cutting fluid, which is not the case for turning operations.

Second, there is a lack of experimental studies conducted to understand the effect of variation of the ACF spray parameters and machining conditions on the machinability, when end-milling a titanium alloy. Although, Jun et al. [8] evaluated the ACF spray system for micro-milling operations, success of ACF spray system in macro-scale milling is, however, not guaranteed because the cutting interface that is generated during macro-scale milling, because of greater feed and depth of cut, is larger than the one generated in micro-scale milling. As a result the liquid film can get completely evaporated before it reaches the entire tool-chip interface [52].

Lastly, there is a lack of numerical study that has been done to model the liquid film formation by impinging droplets, from a spray, on a rotating surface.
Such a model could be useful to evaluate characteristics of fluid film formed by different combinations of spray parameters. Experimental and numerical studies conducted in the past [82] deal with single droplet impingement on a rotating surface and the knowledge gained thereof can not be directly applied to study liquid films formed by sprays.
Chapter 3

End-Milling experiments

The atomization-based cutting fluid (ACF) spray system has recently been shown to cool and lubricate the cutting zone during macro-scale turning of Ti-6Al-4V, leading to significant improvement in machinability of titanium alloys. However, the efficacy of the ACF spray system is yet to be tested for other machining operations that are different from turning, like milling. The droplet impingement dynamics in milling are different than that in turning because of the presence of a rotating cutting tool as opposed to a stationary single point cutting tool found in turning. Also, milling is an intermittent cutting process that gets affected by thermal shock caused by cutting fluid.

The objective of this chapter is to experimentally evaluate the effectiveness of the ACF spray system in end-milling of a titanium alloy, Ti-6Al-4V. Experiments have been conducted in two phases. During first phase, experiments are conducted to study various combinations of spray parameters that affect the machinability of Ti-6Al-4V and select the one that has the least cutting forces. In the second phase, machining experiments are conducted, using the spray parameters selected in phase 1, to assess the machinability of titanium alloy for different cutting fluid
application methods, viz., ACF system, flood cooling and dry cutting, and evaluate the effectiveness of ACF spray system for different machining conditions.

Section 3.1 contains the description of the ACF spray unit that is used for milling operations. This section also draws attention towards the important ACF spray parameters that could potentially affect the functionality of the ACF spray system in macro end-milling. Section 3.2 presents the experimental setup and design, followed by a presentation of the experimental results and analysis in section 3.3. Chapter’s summary is presented in section 3.4

3.1 ACF spray unit layout in milling setup

The ACF spray system unit used during milling of Ti-6Al-4V is shown in Fig. 3.1 [27]. The system consists of: (i) an ultrasonic-based atomizer; (ii) cutting fluid reservoir with a delivery tube; (iii) nozzle unit consisting coaxially-assembled outer droplet and inner gas nozzles; and (iv) high-pressure gas delivery tube for the nozzle-spray unit. The ultrasonic atomizer used for this study is of the type of NS130K50S316, which vibrates at a frequency of 130 kHz and generates atomized droplets having mean diameter of 11.8 \( \mu \text{m} \). Once the fluid is atomized, the microsize droplets move forward through the outer droplet nozzle. The high-velocity gas flowing through the gas nozzle entrains these droplets to produce a focused axisymmetric spray jet that is employed in the cutting zone during machining. The exit diameters of the droplet nozzle and the high-velocity gas nozzle are
designed to be 18.8 mm and 1.6 mm, respectively. Both nozzles are considered to have a convergence slope of 40° and 0.750°, respectively, and the gas nozzle was placed 5 mm inside the droplet nozzle exit point in order to avoid divergence of droplets [9].

![Figure 3.1: Photograph of the ACF spray unit [27]](image)

For effective removal of heat from the machining zone, it is crucial that the droplets are able to access the tool-chip and tool-workpiece interfaces. The size of droplets (10-50 \( \mu m \)) being smaller than the tool-chip contact area in macro-scale machining [9], ensures that the droplets are uniformly able to wet the cutting area and form a thin liquid film on the cutting tool for effective cooling and lubrication of the cutting edges.

The behavior of droplets upon impinging a surface, depends on four specific spray parameters, viz., inlet pressure level of droplet carrier gas, fluid flow rate, droplet impingement angle \( (\theta) \), and spray distance [9]. The interplay of these parameters, influences the characteristics of the liquid film, formed by the impinging droplets, which in turn affects the quality of cooling and lubrication of the cut-
ting zone. The relationship of the above mentioned specific spray parameters to a milling tool is schematically shown in Fig. 3.2.

![ACF spray system in milling setup](image)

**Figure 3.2: ACF spray system in milling setup**

In the ACF spray system, the cutting fluid is supplied to the atomizer through a delivery tube and the flow rate of the delivered cutting fluid is fixed in the range of 8-9 ml/min. The droplet carrier gas used for this study is a mixture of air and carbon dioxide (CO₂). The primary reason for using CO₂ in the mixture is that when it is delivered from a pressurized cylinder it cools down to significantly lower temperatures of −1 °C for 9 psi and −3 °C for 15 psi. When CO₂ under such conditions is mixed with air, the mixture attains a temperature in the range of 8-10 °C and this low temperature is effective in cooling the cutting zone. One could argue that the temperature could be reduced further by using only CO₂ as a droplet carrier gas, however in such a situation the temperature of the gas would be below 0 °C and this would lead to the freezing of the cutting fluid that is composed of 90% of water by volume.
3.2 Experimental setup and procedures

A CNC milling machine (OKUMA MC-4VAE) was used for milling experiments. The ACF spray unit was mounted on a metallic frame, as shown in Fig. 3.3, which was attached to the vertical head of the milling machine. The supporting metallic frame not only allowed easy mounting of the ACF spray unit but also allowed the experimenter to change the position of the ACF spray unit with respect to the end mill tool in a precise and repeatable manner.

![Experimental setup of the ACF spray unit](image)

Figure 3.3: Experimental setup of the ACF spray unit

The relative position of the ACF spray unit with respect to tool feed direction is an important parameter, especially for the case when only one set of nozzle is used. Depicted in Fig. 3.4 are the three possible angular positions, denoted by $\alpha$, in which the spray unit could be placed. Placing the spray unit at any angle greater than 180° would not be preferred because in such an orientation the
atomized droplets coming out of the atomizer would find it difficult to access the cutting zone because of the hindrance provided by the uncut work piece material. For conducting the experiments, an angle of $135^\circ$ for the ACF spray unit was preferred over that of $45^\circ$ because Lacalle et al. [3] reported that at an angle of $45^\circ$, the cutting fluid is not able to penetrate completely in the tool edges because of the interference produced between the cooling jet and the flying metal chips that are generated during the machining process. For the present study the value of impingement angle ($\theta$), as shown in Fig. 3.2, was fixed at $30^\circ$ for all the experiments.

![Figure 3.4: Position of ACF spray unit in relation to feed direction](image)

A slab of Ti-6Al-4V having a cross-sectional area of 7200 mm$^2$ and height of 120 mm is used for milling experiments. The tool is an uncoated carbide end mill and the geometry is set as follows: four flutes, 10 mm mill diameter, and $30^\circ$ helix angle. National Instrument data acquisition system (SCB-68), integrated with
the LabVIEW software is used to capture cutting force data from the Kistler 3-component force dynamometer (type 9265B), at a sampling frequency of 20 kHz. The cutting force values reported here are average peak-to-valley forces for 100 revolutions of machining. Water soluble cutting fluid S-1001 at 10% dilution is used as the cutting fluid. The thermo-physical properties of water and 10% S-1001 are presented in Table 3.1. The mixture of water and cutting fluid is prepared keeping in mind that a cutting fluid with high viscosity and low surface tension is preferable for better lubricity[26].

Table 3.1: Thermo-physical properties of cutting fluid and water

<table>
<thead>
<tr>
<th>Fluid</th>
<th>Surface Tension [mN/m]</th>
<th>Density [kg/mm³]</th>
<th>Viscosity [cP]</th>
<th>Thermal conductivity [W/mK]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Water</td>
<td>72</td>
<td>1000</td>
<td>1.01</td>
<td>0.58</td>
</tr>
<tr>
<td>10% S-1001</td>
<td>41</td>
<td>1003</td>
<td>1.22</td>
<td>0.53</td>
</tr>
</tbody>
</table>

First phase of experiments are conducted to study the effect of spray parameters on the cutting forces. The spray parameters that are varied in this study are spray distance and the inlet pressure of the droplet carrier gas. The spray distance is varied by adjusting the position of the ACF spray unit on the metallic frame. The pressure level of the droplet carrier gas is controlled by individually regulating the pressure of air and CO₂. The velocity of the droplet carrier gas at different distances from the gas nozzle is measured by a pitot-static tube (type PSA-"a") [85].

A 2² full factorial design is used to access the different combinations of spray parameters and the different levels of these spray parameters are shown in Table 3.2.
Table 3.2: Values of different non-dimensional numbers at given ACF spray conditions

<table>
<thead>
<tr>
<th>Test</th>
<th>$\theta$ [$^\circ$]</th>
<th>Distance, $x_1$ [mm]</th>
<th>Pressure, $x_2$ [psi]</th>
<th>$u_g$ [m/s]</th>
<th>$u_o$ [m/s]</th>
<th>We</th>
<th>$K_y$</th>
<th>$K_m$</th>
</tr>
</thead>
<tbody>
<tr>
<td>T1</td>
<td>30</td>
<td>40</td>
<td>9</td>
<td>27</td>
<td>23</td>
<td>160</td>
<td>2.2</td>
<td>49.3</td>
</tr>
<tr>
<td>T2</td>
<td>30</td>
<td>60</td>
<td>9</td>
<td>14</td>
<td>12</td>
<td>39</td>
<td>1.15</td>
<td>21.7</td>
</tr>
<tr>
<td>T3</td>
<td>30</td>
<td>40</td>
<td>15</td>
<td>33</td>
<td>30.3</td>
<td>269</td>
<td>2.8</td>
<td>56.8</td>
</tr>
<tr>
<td>T4</td>
<td>30</td>
<td>60</td>
<td>15</td>
<td>25</td>
<td>21.6</td>
<td>137</td>
<td>2.05</td>
<td>44.8</td>
</tr>
</tbody>
</table>

The values of the droplet carrier gas velocity, ($u_g$), at the end-mill tool for different combinations of spray distance and pressure level are also shown in Table 3.2. In order to ensure that the selected spray parameters would produce droplets in the spreading regime, the non-dimensional numbers, viz., We, $K_y$ and $K_m$ are calculated. For calculating the non-dimensional numbers, normal gas velocity ($u_o$), which is the component of $u_g$ perpendicular to the end-mill tool axis, is required and it is obtained by taking the product of $u_g$ with the cosine of the droplet impingement angle ($\theta$). As can be seen from these non-dimensional numbers, the impinging droplets on the end-mill tool would be in the spreading regime ($We>10$, $K_m<57.7$ and $K_y<17$). For each combination of spray parameters mentioned in Table 3.2, a new tool is used to end-mill titanium alloy for a single pass, having a length of 150 mm. Machining conditions for these experiments are: spindle speed 1500 RPM, ADOC 0.5 mm, RDOC 2 mm and feed/tooth ($f$) 0.1 mm/tooth. Each experiment is repeated three times and average peak-to-valley cutting forces of these three trials are calculated.

In phase 2, the experiments are conducted at different machining conditions, M1-M4, as shown in Table 3.3 and the machinability is measured in terms of
Table 3.3: Experimental machining parameters

<table>
<thead>
<tr>
<th></th>
<th>M1</th>
<th>M2</th>
<th>M3</th>
<th>M4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Spindle Speed [RPM]</td>
<td>1500</td>
<td>1500</td>
<td>1500</td>
<td>1500</td>
</tr>
<tr>
<td>Axial Depth of Cut [mm]</td>
<td>0.5</td>
<td>1</td>
<td>0.5</td>
<td>1</td>
</tr>
<tr>
<td>Radial Depth of Cut [mm]</td>
<td>2</td>
<td>2</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>Feed/Tooth [mm/tooth]</td>
<td>0.1</td>
<td>0.1</td>
<td>0.14</td>
<td>0.14</td>
</tr>
</tbody>
</table>

cutting forces and surface roughness. Note that the machining condition, M1 corresponds to the least and M4 the highest material removal rate among the four sets of machining conditions. The cutting forces reported in these experiments are peak-to-valley forces obtained after averaging over 100 revolutions of machining at an instance of time. The force profile observed, when end-milling a material with a tool having four flutes, for two rotations of the tool is shown in Fig. 3.5. From the figure it can be observed that the force plots are periodic and the forces in feed and cross-feed directions are greater than those in Z direction. Similar force profiles are observed for all the experiments performed in this study. The tool failure criteria are determined by ISO 8688-2 [86]: (1) average flank wear = 0.3 mm (average of all cutting edges), (2) maximum flank wear = 0.5 mm (on any of the cutting edges), (3) chipping/flaking or fracture of any of the cutting edges. For all the experiments, during the initial stages of machining, the end mill tool was removed from the milling machine at intervals after cutting a length of 1.8 m and then later on at intervals of 0.4 m to observe the progress of flank wear until the tool failed. Using the Quadra-Check 300 optical microscope, the total flank wear is measured and photographs of the tool flank faces are taken. Average and maximum surface roughness ($R_a$) and ($R_z$), respectively, are measured for
the machined surface using a portable surface roughness tester, SR100, having a cutoff length of 0.76 mm. Surface roughness measurements are first made after cutting 0.9 m (6 passes) and then at intervals of 0.6 m, until the tool failed.

3.3 Experimental results and analysis

3.3.1 Experiments to select spray parameters

Table 3.4 lists the results of cutting forces for four different combinations of the spray parameters, each of these combinations is referred to as Ti, where the range of i is from 1 to 4, for the rest of the thesis. Figure 3.6 shows the cutting force variation for four spray conditions.

<table>
<thead>
<tr>
<th>Test</th>
<th>$x_1$[mm]</th>
<th>$x_2$[psi]</th>
<th>Cutting Forces [N]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Run1</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Feed</td>
</tr>
<tr>
<td>T1</td>
<td>40</td>
<td>9</td>
<td>116</td>
</tr>
<tr>
<td>T2</td>
<td>60</td>
<td>9</td>
<td>123</td>
</tr>
<tr>
<td>T3</td>
<td>40</td>
<td>15</td>
<td>127</td>
</tr>
<tr>
<td>T4</td>
<td>60</td>
<td>15</td>
<td>118</td>
</tr>
</tbody>
</table>

As seen in Fig. 3.6, the cutting forces for T1 and T4 spray conditions are lower than that for T2 and T3. A possible reason is that droplets impinging under T2 conditions have an impingement velocity of 14 m/sec, which is lower than the other three conditions and could lead to the formation of a thick liquid
Figure 3.5: Variation of cutting forces when machining with M1 machining condition and using the ACF spray system.
film, spreading with a low momentum, preventing it from effectively accessing the small gap between the tool and chip. This would lead to higher temperatures and friction at the tool chip interface. On the other hand droplets under T3 conditions have $K_m$ value of 56.8, which borders with the critical $K_m$ value of 57.7 and as such some of the impinging droplets might have splashed. For droplet conditions under T1 and T4, the difference in cutting forces are not significant. Hence, further machining tests are performed for conditions under tests T1 and T4 up until a time when appreciable difference in cutting forces are seen. As observed from Fig. 3.7 (a) and (b), the cutting forces for spray condition T4 after 180 cm of cut is slightly lower than for the spray condition T1. A possible reason for this trend is that droplets impinging under T1 conditions have a droplet velocity of 27 m/sec which is slightly higher than the droplet velocity of 25 m/sec under T4 conditions. Fluid film thickness formed by droplets upon striking a surface
depends on the impact velocity of the droplet and higher velocity would lead to formation of a liquid film having thickness less than that formed by droplets having lower impact velocity [82]. As machining progresses the temperature of the cutting zone also increases and thin liquid film could evaporate even before it reaches the cutting edge where as, a thicker fluid film would be able to provide effective lubrication and cooling in the cutting zone, which would lead to lower cutting forces[9]. Hence, in this study a combination of spray parameters under T4 is selected to evaluate the machinability of titanium alloys.

(a) Variation of cutting force in feed direction
(b) Variation of cutting force in cross-feed direction

Figure 3.7: Variation of cutting forces
3.3.2 Experiments to evaluate the machinability of titanium alloy in milling using the ACF spray system

3.3.2.1 Tool life

In an attempt to quantify flank wear, the maximum flank wear land width is constantly monitored. Figure 3.8 illustrates the progress of tool flank wear with machining for different cutting fluid application methods and machining conditions. Under both machining conditions, M1 and M4, the ACF spray system extends the tool life by 75% and 33.8% over flood cooling, respectively. Thus an increase in tool life, as high as 75%, can be achieved using the ACF spray system as compared to other methods of cutting fluid application.

![Figure 3.8: Wear progress of the tool flank with machining for different cutting fluid application methods](image)

(a) Machining condition, M1  
(b) Machining condition, M4

The reason for longer tool life in machining titanium alloy using the ACF spray system could be given as follows. In the ACF spray system, the droplet carrier gas accelerates and imparts momentum to the micro-sized droplets, generated by the atomizer, because of which the droplets are able to penetrate the tool/chip...
and tool/workpiece interface and form a thin liquid film upon impacting the surfaces. As a result, Sulphur, an additive in 10% S-1001, chemically reacts with the freshly generated metal surfaces of the chip to produce a metallic sulfide film that has lower shear strength than the chip material, thus reducing friction, and which further leads to a reduction in cutting forces and temperature in the cutting zone [87]. Thin liquid film is able to remove heat from the cutting zone by evaporative cooling whereas the low temperature and high velocity of the droplet carrier gas provides forced convective cooling of the cutting zone. The combined action of these two cooling mechanisms lowers the temperature of the cutting zone significantly. Dry cutting offers the least amount of tool life among the cutting fluid application methods evaluated in this study and fails after 4.1 min of machining.

3.3.2.2 Tool wear

Figures 3.9–3.13 show the tool flank wear for two out of the four diametrically opposite flutes of the end mill at the time of tool failure. It is seen that maximum flank wear that occurs at the nose is the controlling factor of tool life for the different machining conditions. Figures 3.9 and 3.12 show that with the application of the ACF spray system, the tool wear is uniform, without any abrupt chipping and notch formation, and the presence of groove on the flank face and shiny flank wear are indicative of the attritional wear mechanism, which takes
place when the cutting zone temperature is not very high [88]. Attritional wear leads to uniform wear of both the carbide particles and binder material in the tool. Figures 3.11 and 3.13 show that tool fails catastrophically by cutting edge chipping and notch formation when flood cooling cutting condition is used. Predominant wear mechanism for such type of wear is abrasion, which takes place at high cutting temperatures. During abrasive wear, the high temperatures at the cutting zone increases the wear rate of binder material in tool as compared to the carbide particle. As a result the carbide particles keep sticking out of the tool. Edge chipping and notch wear indicate that conventional flood cooling is capable of cooling the tool and the workpiece in bulk but is not able to effectively cool and lubricate at the chip-tool and tool-workpiece interfaces, where the temperature is high, because of lack of penetration capabilities of cutting fluid in the cutting zone. Figure 3.10 shows that under dry cutting conditions the tool fails catastrophically because of excessive chipping of the flank surface. The significant discoloration of the flank surface of the tool is indicative of the presence of high temperatures near the cutting zone that lead to oxidation of the flank surface and plastic deformation of the tool. The tool failure in dry cutting is by excessive chipping of flank face, due to intense temperatures experienced at the cutting zone, as shown in Fig. 3.10(a).
Figure 3.9: Tool flank face when machining at M1 and using the ACF spray system

Figure 3.10: Tool flank face when machining at M1 and using dry cutting conditions

Figure 3.11: Tool flank face when machining at M1 and using flood cooling

Figure 3.12: Tool flank face when machining at M4 and using the ACF spray system
3.3.2.3 Chip morphology

Further evidence of greater cooling capability of the ACF spray system over flood cooling can be found by studying the chips that are collected during end-milling titanium alloy using either of these two cutting fluid application methods are used. Figure 3.14(b) and 3.15(b) show the collected chips for machining conditions, M1 and M4. For machining condition M1, the chips collected while using ACF spray system have an average length of 3 mm and thickness of 0.18 mm, whereas those collected under flood cooling condition have an average length of 5 mm and thickness of 0.17 mm. For machining condition M4, the chips collected while using ACF spray system have an average length of 4.3 mm and thickness of 0.34 mm, whereas those collected under flood cooling condition have an average length of 6 mm and thickness of 0.38 mm. Hence, it can be concluded that the chips produced when machining with the ACF spray system are shorter in length as compared to chips that are generated using flood cooling. A possible reason for such chip morphology is that the usage of ACF spray system leads to the production of broken chips, due to the increase in brittleness of the produced chips as a result of excellent cooling capabilities of the ACF spray system [9]. Flood cooling does
not provide effective cooling in the cutting zone and as a result the brittleness of the chips does not increase, which leads to higher chip lengths for flood cooling as compared to the ACF spray system.

![Figure 3.14: Chip morphology when machining at M1](image)

(a) ACF spray system  
(b) Flood cooling

Figure 3.14: Chip morphology when machining at M1

![Figure 3.15: Chip morphology when machining at M4](image)

(a) ACF spray system  
(b) Flood cooling

Figure 3.15: Chip morphology when machining at M4

### 3.3.2.4 Tool life with the ACF system in the absence of CO\textsubscript{2} in the carrier gas

It is believed that the presence of CO\textsubscript{2} in the droplet carrier gas of the ACF spray system further helps in reducing the thermal shock experienced by the cutting
edges in milling due to its intermittent cutting nature. In order to evaluate the effect of presence of CO$_2$ in the gas mixture of the ACF spray system on the tool life, experiments are conducted with and without CO$_2$ (only air in the droplet carrier gas). Figure 3.16 shows the average flank wear, when machining with M1 and using the ACF spray system, with and without the presence of CO$_2$ in the carrier gas. It is seen from Fig. 3.16 that the tool life is 33% lesser than what would have been obtained had CO$_2$ been present in the gas mixture. It can also be seen from Fig. 3.17 that the absence of CO$_2$ and presence of only air in the droplet carrier gas has the undesirable effect of chipping of cutting edges due to the presence of high temperatures in the cutting zone that increases the tool abrasion. Note that, even in the absence of CO$_2$ from the gas mixture, the tool life is 17% more as compared to flood cooling because of effective penetration capabilities of atomized droplets in the cutting zone.

Figure 3.16: Wear progress of the tool flank with machining for different cutting fluid application methods
Figure 3.17: Tool flank face when machining at M1 and using the ACF spray system no CO₂

3.3.2.5 Cutting force

Figure 3.18(a)–(c) shows the cutting forces when end-milling a titanium alloy using machining condition M1. Note that the first cutting force data has been recorded at 0.25 min from the start of machining. As seen from the Fig. 3.18, the cutting forces using the ACF spray system are generally lower than the other cutting fluid application methods. However, except for dry cutting, where the rise in cutting forces are steep, for all the other cutting fluid application methods the cutting forces for the first three minutes are almost the same. A possible reason is that during the initial stages of machining the temperature rise in the tool-workpiece and tool-chip interaction zones are small because of sharp cutting edges of the tool and cooling and lubrication provided by the cutting fluid. However, with increase in time the tool wears out and the temperature rise and friction in the cutting zone become significant leading to higher cutting forces. The ACF spray system is able to provide the most effective cooling and lubrication, which is instrumental in reducing the rate of flank wear and the cutting forces.
Figure 3.18: Variation of cutting forces for different cutting fluid application methods
Figure 3.19: Variation of cutting forces for different machining conditions with the ACF spray system

Figure 3.19 (a)-(c) shows the variation of cutting force components for different machining conditions, M1-M4. For a given value of feed/tooth, the variation in axial depth of cut (ADOC) does not affect the rate of increase in cutting forces.
significantly. On the other hand for the same ADOC (machining conditions, M1 and M3 or M2 and M4) the variation in feed rate significantly affects the rate at which the cutting forces increase with time. Consequently, the rate of increase in cutting forces is higher with the increase in feed rate than by an increase in ADOC alone. A possible reason is that with the change in ADOC, for a given feed rate, the part of the tool that gets engaged with the workpiece has a liquid film formed over it by the spreading of impinging droplets that is sufficient for cooling and lubrication. On the other hand, for a given ADOC, when the feed rate is high, the cutting zone experiences higher temperatures, due to larger chip load, than what it would be for lower feed rates. The thickness of the liquid film formed on the tool might not be sufficient and it would evaporate, owing to high temperatures, even when the cutting edge is engaged with the tool. As a result the cooling and lubrication is not effective in this situation, leading to higher cutting forces and tool wear.

3.3.2.6 Surface roughness

Figure 3.20 (a)-(b) shows the quality of the machined surface, in terms of average and maximum surface roughness for different cutting fluid application methods. Figure 3.20 shows that the surface roughness obtained when using flood cooling and ACF system are almost comparable for the first 7.5 min of machining. However, after that time, the surface quality of the machined surface deteriorates
rapidly for flood cooling as compared to that of ACF spray system. Under dry cutting conditions the cutting edges experience intensive stresses and temperatures that accelerate the rate of flank wear, as seen in Fig. 3.8(a), leading to catastrophic failure of tool, resulting in higher values of surface roughness. A probable cause of difference between the surface finish obtained by ACF spray system and flood cooling is that the application of latter method eventually leads to notch formation on the cutting edges, as seen in Fig. 3.11, which leads to a sudden increase in surface roughness, whereas the application of ACF spray system leads to lower and steady flank wear, as seen in Fig. 3.8(a). As a result the surface roughness increases slowly with time for the ACF spray system.

Figure 3.21(a) shows the average surface roughness obtained using ACF spray system for four different machining conditions. It is observed that similar to cutting forces, the average and maximum surface roughness are also dependent upon the machining conditions, especially, the feed rate plays a significant role as compared to ADOC in determining the surface finish.

![Figure 3.20: (a) Average (b) maximum surface roughness values for different cutting fluid application method and M1 machining condition](image)

Figure 3.20: (a) Average (b) maximum surface roughness values for different cutting fluid application method and M1 machining condition
Figure 3.21: (a) Average (b) maximum surface roughness values for different machining conditions with the ACF system

3.4 Chapter summary

In this chapter an atomization-based cutting fluid spray system has been evaluated for end-milling of titanium alloy, Ti-6Al-4V. Experiments were conducted to not only study the impact that the variation of specific ACF spray parameters has on the measured cutting forces but also to compare the performance of an ACF spray system to that of other cutting fluid application methods, viz., flood cooling and dry cutting. Furthermore, the performance of ACF spray system has been evaluated for different machining conditions. In this study it is observed that the ACF spray system is able to extend the tool life as high as 75% over flood cooling when the feed/tooth and axial depth of cut values are chosen as 0.1 mm/tooth and 0.5 mm, respectively.

In the ACF spray system, CO$_2$ is a part of droplet carrier gas and in this study experiments have been designed to study the effect of the presence CO$_2$ on the machinability when end-milling a alloy. It is observed that the presence of CO$_2$ in
the droplet carrier gas is responsible for cooling the cutting zone more effectively in milling than what could be achieved in its absence. As a result, tool life increases by 50% when the droplet carrier gas is a mixture of air and CO$_2$ as compared to the case where droplet carrier gas has only air. Moreover it is noticed that the ACF spray system even in the absence of CO$_2$ from the gas mixture, can achieve upto 17% more tool life as compared to flood cooling.

Results of experiments that have been presented in this chapter show that when ACF spray system is used, the cutting edges undergo uniform wear and the tool does not fail catastrophically due to attritional wear caused by lower temperatures of the cutting edges. On the other hand when dry and flood cooling cutting conditions are used, the tool wears out rapidly and undergoes catastrophic failure because of chipping and notch formation on the tool flank face. Superior cooling and lubrication capabilities of the ACF spray system, due to thin film formation on the cutting edge, over other cutting methods is instrumental in reducing the tool flank wear and hence the cutting forces and surface finish.

Superior cooling capability of the ACF spray system over flood cooling, is demonstrated by studying the chip morphology. It is found that for a particular machining condition, the length of the chips generated with the ACF spray system is shorter than those that are generated with flood cooling. It is further observed that the cutting forces and surface roughness increase more for an increase in feed/tooth as compared to an increase in axial depth of cut alone.
Chapter 4

Modeling of liquid film formation

The reason behind the ACF spray system’s ability to uniformly cool and lubricate the cutting zone in micro-machining and macro-turning operations has been attributed to the formation of a spreading thin liquid film that successfully penetrate into the tool-chip interface [8, 9]. Hence it is of paramount importance to study the liquid film formation by the ACF spray system. Hoyne et al. [21] developed an analytical three-dimensional (3D) thin fluid film model in order to gain a physics-based understanding of liquid film formation by the ACF spray system during turning of titanium alloys. The objective of this chapter is to develop a numerical model to study the liquid film formation when the cutting fluid droplets from the ACF spray system impinge on a rotating surface, like milling, where the tool rotates.

It is known that droplet impingement dynamics get affected by the presence of a rotating surface [26, 23]. Boughner et al. [23] developed a probabilistic model to study the rate of micro-film formation on a rotating cylindrical surface by an ACF spray system. Nevertheless, the model has some limitations. First, the model is two-dimensional (2D), which could affect the spreading behavior and the
simulated results may not accurately represent the actual solution. Second, the
droplets are assumed to be impinging on a dry surface, with a velocity equivalent
to that of the carrier gas. However, in the ACF spray system, after a certain period
of time, the droplet impingement takes place on a previously formed liquid film
and the velocity of the impinging droplets are not equal to that of the carrier gas.
Finally, the model neglects droplet interactions that could have an appreciable
impact on the prediction of film characteristics by the numerical simulations.
Hence, there is a need to develop a model that would accurately simulate the film
formation on a rotating surface and predict the film characteristics.

This chapter is divided into four sections. Section 4.1 presents the modeling
approach that is adopted to study film formation by the ACF spray system on
a rotating surface. Section 4.2 contains a detailed description of the numerical
model, used to simulate the ACF spray, along with the results of experiments
that were conducted to validate the simulated results of the spray model. Pertinent
information regarding the Eulerian Wall Film (EWF) model, which is used
to model the liquid film formation on a rotating surface, along with the results
of numerical simulations and relevant discussion, is provided in Section 4.3. Fi-
ally, the chapter is concluded by providing a summary of the chapter in Section
4.4.
4.1 Modeling approach

In order to study liquid film formation on a rotating surface, through a computational fluid dynamics (CFD) approach, two numerical models, namely, ACF spray model and film formation model, have been developed. A flowchart for the modeling approach, along with the input and output parameter at each step, is schematically shown in Fig. 4.1.

![Flowchart of the modeling approach.](image)

The ACF spray model takes into account the effect of ACF spray parameters including, (i) pressure of the droplet carrier gas (ii) distance of the spray from the surface of impingement and (iii) thermal and physical properties of the liquid droplets, and predicts the spatial variation of velocity of the carrier gas. The spray
model also tracks the position and velocity of liquid droplets that impinge on the rotating surface. The discrete phase modeling (DPM) approach is used for modeling the liquid droplets generated by the ACF spray system. The ACF spray model considers the interaction between the liquid droplets and the carrier gas, as well as the breakup and coalescence among droplets. Hence, the steady-state velocity field of the carrier gas, emerging out of the gas nozzle is first determined. This is followed by solving the droplet’s velocity and position profile in the presence of the gas.

The liquid film modeling on the rotating surface of the cylinder is carried out by using the eulerian wall film (EWF) model. The EWF model uses the properties of the impinging droplets as input parameters. The outputs of the model are the spatial variation of the liquid film on the surface and film parameters, such as film thickness and film velocity among others. The EWF model also has the provision for taking into account droplet splashing.

### 4.2 ACF spray model

The ACF spray model is composed of two individual models: (i) carrier gas model and (ii) droplet model. The carrier gas model is employed for solving the steady-state velocity field of the carrier gas, emerging out of the gas nozzle at a certain pressure, while considering the flow to be compressible. This is followed by solving the droplet’s velocity and position profile in the presence of the carrier gas by using
the droplet model.

4.2.1 Carrier gas model

The gas phase is treated as a steady, compressible fluid. The modeling of the gas phase is accomplished by solving the equations of continuity, momentum and ideal gas law, as follows,

\[ \nabla (\rho_g \vec{v}) = 0, \]

\[ \rho_g (\vec{v} \cdot \nabla \vec{v}) = -\nabla p + \mu \nabla^2 \vec{v} + \rho_g f, \]

\[ p = \rho_g RT, \]

where \( \rho_g, \mu, p, v, f, R \) and \( T \) are the density, dynamic viscosity, pressure, velocity, body force (gravitational force), gas constant and temperature of the flow, respectively. To model for the convection and diffusion of turbulent energy, shear stress transport (SST) \( k-\omega \) model has been used. The above turbulence model has been selected among the many available options because the SST formulation combines the best effects of \( k-\omega \) and \( k-\varepsilon \). The use of a \( k-\omega \) formulation in the inner parts of the boundary layer makes the model directly usable all the way down to the wall through the viscous sub-layer and the SST formulation also switches to a \( k-\varepsilon \) behavior in the free-stream and thereby avoids the common \( k-\omega \) problem that the model is too sensitive to the inlet free-stream turbulence properties [89]. The pressure-based solver with the coupled option for the pressure-velocity coupling
was chosen for the numerical simulations presented in this study. It is a good alternative to density-based solvers of ANSYS Fluent when dealing with applications involving high-speed jets (flow having mach number greater than 0.3), which is the case for the ACF spray system [90].

On unstructured meshes (elements of the mesh have irregular connectivity, characterized by the presence of more than one type of mesh elements), the accuracy of the least-squares gradient method is comparable to that of the node-based gradient (and both are much more superior compared to the cell-based gradient). However, it is less expensive to compute the least-squares gradient than the node-based gradient [89]. Therefore, least-squares method has been selected as the gradient method. Spatial discretization of pressure and density terms have been accomplished using second order schemes and discretization of momentum and energy equations have been realized by using second order upwind scheme. Higher order schemes have been preferred because it provides higher accuracy over the first order schemes [91]. The use of higher-order schemes necessitates the implementation of Higher-Order Term Relaxation (HOTR) with a relaxation factor of 0.25 for flow variables in order to improve the startup behavior of simulation and prevent the solution from diverging [91].

The whole computational domain was assigned the ambient temperature of 300 K. At the inlet of the gas nozzle, a constant total pressure is applied. The surfaces of the droplet and the gas nozzle have wall boundary condition as that
4.2.2 Validation of carrier gas model

The carrier gas model, developed in Sec. 4.2.1, is validated by experimentally measuring the carrier gas velocities as a function of distance from the gas nozzle and comparing them to those obtained from the numerical simulations. The experimental set up for taking down the measurements is shown in Fig. 4.2. The nozzle unit was placed on a XYZ linear transnational stage, so that the position of the droplet and gas nozzle could be accurately varied. Experiments were performed using a Pitot-Static tube (Model PDA-18-F-16-KL, United Sensor Corp., USA), with a sensing stem diameter of 1/16 in. The dynamic pressure sensed by the pitot-static tube was measured using a digital manometer (HHP-2082, Omega Engineering Inc., USA), as the gas nozzle is translated axially along the nozzle’s axis. The recorded dynamic pressure can be used for computing the gas velocity by using the Bernoulli’s equation. Measurements were taken for three different pressure levels, 9, 15 and 21 psi.

The dynamic pressure recorded by the digital manometer can be converted into flow velocity by using the Bernoulli’s equation. However, there are two forms of the Bernoulli’s equation. The compressible (Eqn. 4.4) and incompressible (Eqn.
4.5) form of the Bernoulli’s equation are given as below:

\[
\int \frac{dp}{\rho_g} + \frac{1}{2}v^2 + gz = C, \quad (4.4)
\]

\[
p + \frac{1}{2}v^2\rho_g + \rho gz = C, \quad (4.5)
\]

where, \(p\) is the static pressure at a point, \(\rho_g\) is the density of the carrier gas, \(v\) is the velocity of droplet carrier gas, \(g\) is the acceleration due to gravity and \(z\) is the elevation, with respect to a datum, at which measurements are recorded. The incompressible form of the equation is obtained after simplifying the compressible form of Bernoulli’s and hence if enough parameters are known, it is always more accurate to use Eqn. 4.5 for computations. A flow can be classified as compressible or incompressible on the basis of Mach number \((M)\), which is a ratio of flow
velocity and speed of sound \( c \) in air \( (M = v/c) \). If the Mach number of flow is less than 0.3, then the flow is considered incompressible, otherwise it is considered as compressible flow. At room temperature the value of \( c \) is 343 m/s. For using Eqn. 4.4, the relation between \( p \) and \( \rho_g \) should be known a priori. However, no such relation is available during the conduction of experiments, hence, Eqn. 4.5 has been used for calculating the carrier gas flow velocity from the dynamic pressure measurements.

ACF spray geometry that was used for simulating the carrier gas velocities is shown in Fig. 4.3. The velocity vectors that were obtained for different pressure levels of the droplet carrier gas are shown in Fig. 4.4. From Fig. 4.4, it was observed that due to the flow of high velocity droplet carrier gas, the surrounding air gets entrained in the flow due to the presence of pressure difference between the gas nozzle and surrounding area outside the ACF spray. Figure 4.5 shows the magnified view of the velocity vectors in the surrounding area near the droplet nozzle for gas inlet pressure of 21 psi. The direction of these velocity vectors reveal that surrounding air is indeed getting entrained into the flow and this would result in the production of a focused jet of liquid droplets [92].

From Fig. 4.4 it was also observed that the higher velocities are obtained in the region near the gas nozzle and exit of droplet nozzle. Thereby, in order to study the velocity magnitude distribution of the droplet carrier gas it was decided to observe the magnified view of velocity contours near the droplet and gas nozzle, as
shown in Fig. 4.6. Irrespective of the inlet pressure level, it was observed in Fig, 4.6 that the velocities are highest at the $90^\circ$ bend of the carrier gas inlet tube. The most probable reason for this observation is that the pressure loss associated with a sudden bend in the flow of fluid is quite significant and this gets manifested in the form of increase in velocity. For the pressure levels used in this study, it can be observed that an increase in gas inlet pressure leads to a rise in maximum velocity magnitude of the carrier gas. For gas inlet pressures of 9, 15 and 21, the maximum gas velocity magnitude are predicted to be 278, 328 and 348 m/s, respectively.

The comparison between the simulated and experimentally measured data for gas velocities as a function of distance from droplet nozzle is shown in Fig. 4.7. The zero point on the x-axis denotes the instance when the gas nozzle and sensing tip of pitot-static tube are coinciding. The experimental data points in Fig. 4.7
Figure 4.4: Velocity vectors of droplet carrier gas for different inlet pressures
Figure 4.5: Entertainment of surrounding air in the spray flow

were obtained using the simplified form of Eqn. 4.5 and is given as below:

\[ v = \sqrt{\frac{2\Delta p}{\rho}}, \]  

(4.6)

where, \( v \) is the velocity of gas flow, \( \Delta p \) is the dynamic pressure recorded by the digital manometer and \( \rho_g \) is the constant air density at room temperature.

From Fig. 4.7 it can be observed that for all the different pressure levels, the experimental and predicted gas velocities match reasonably well, except for the distance of 0-4 cm from the gas nozzle. A possible reason could be that the experimental values of the gas velocities have been computed using the incompressible
Figure 4.6: Velocity contours of droplet carrier gas for different inlet pressures
form of the Bernoulli’s equation where the velocity of the gas flow is assumed to be less than 103 m/s (for \( M < 0.3 \)). However, from Fig. 4.7 it was observed that irrespective of the pressure level, the gas velocity is always greater than 103 m/s, up to a distance of 1.8 cm from the exit of gas nozzle. Hence, application of Bernoulli’s equation, in this domain, to calculate gas velocity would not yield accurate results.

![Figure 4.7: Comparison of numerical and experimental gas velocity values](image)

4.2.3 Droplet model

Once the steady state carrier gas flow field has been obtained, the trajectory of the liquid droplets can be modeled using the discrete phase modeling (DPM) technique. In the DPM method, the dispersed phase (liquid droplets) is solved by tracking a large number of particles through the calculated carrier gas flow field. The
dispersed phase can exchange momentum, mass, and energy with the fluid phase [89]. The implementation of the DPM method is reserved for situations wherein the volume fraction of the discrete phase is less than 10% [91]. A similar situation is encountered for the ACF spray system because the volume fraction of the liquid droplets is less than 10% and hence using DPM to model the ACF spray system is well justified. The DPM method is based on the Euler-Lagrangian approach [89]. ANSYS Fluent predicts the trajectory of a discrete phase particle (liquid droplets) by integrating the force balance on the particle, which is written in a Lagrangian reference frame [89]. This force balance equates the particle inertia with the forces acting on the particle, and the equation can be written as,

\[
\frac{du_p}{dt} = F_d(\vec{v} - \vec{u}_p) + \frac{g(\rho_p - \rho_g)}{\rho_p} + \vec{F},
\]

(4.7)

where, \(\vec{F}\) is an additional acceleration (force/unit particle mass) term that would account for forces like saffman lift force, virtual mass force or thermophoretic force, none of which is present in modeling of an ACF spray system and hence, \(\vec{F}=0\). \(F_d(\vec{v} - \vec{u}_p)\) is the drag force per unit particle mass and is given by

\[
F_d = \frac{18\mu C_D Re}{\rho_p d_o^2 24}.
\]

(4.8)

In Eqn. 4.7, \(\vec{v}\) is the carrier gas velocity, \(\vec{u}_p\) is the liquid particle velocity, \(\mu\) is the molecular viscosity of the fluid, \(\rho_g\) is the density of the droplet carrier gas, \(\rho_p\) is the density of the particle, and \(d_o\) is the particle diameter. Re is the relative
Reynolds number, which is defined as

\[ Re = \frac{\rho d_o |\vec{u}_p - \vec{v}|}{\mu}. \]  \hspace{1cm} (4.9)

For the modeling spherical drag law has been adopted and the drag coefficient, \( C_D \), for smooth particles can be be defined as,

\[ C_D = a_1 + \frac{a_2}{Re} + \frac{a_3}{Re^2}. \]  \hspace{1cm} (4.10)

where \( a_1, a_2, \) and \( a_3 \) are constants that apply over several ranges of \( Re \) given by Morsi and Alexander [93]. Droplets coming out of the ACF spray system can undergo many different types of interactions. In order to capture those interaction in the numerical model, the options for modeling droplet collisions, coalescence and breakup, were activated in the DPM model. The physical properties of the liquid droplets used in the simulations are those of the cutting fluid, as shown in Table 3.1.

Computational domain and boundary conditions

The computational domain that is used for this study is shown in Fig. 4.8 and it was designed keeping in mind the ACF spray system setup in end-milling operations, which was schematically represented in Section 3.1. The design of the domain was prepared using the software, ANSYS Design Modeler and the domain measures 18 cm by 13 cm by 10 cm. The size of the computational domain is
selected as such, in order to ensure that the boundaries of the domain are far away from the spray system and the flow near the boundaries do not affect the simulation of the spray system. The enlarged view of the gas and droplet nozzles are shown in Fig. 4.9. The rotating end-mill tool has been modeled as a cylinder. This assumption was made because the surface of rotation of an end-mill tool is a cylinder and hence, the trends observed in spreading of films, for different ACF spray parameters, on an end-mill tool would be similar to those that would be observed on a cylindrical surface. Moreover, this assumption also helps in reducing the computational time and makes the analysis simpler than what it would have been had the complex geometry of an end-mill tool been modeled. The design of the gas nozzle and droplet nozzle in the computational model were kept same as the one used by Nath et al. [9] in their study.

The boundary condition at the inlet of the gas nozzle is that of pressure inlet, as shown in Fig. 4.9. The six boundaries of the computational domain have pressure outlet as the boundary condition. The wall of the cylinder is assumed to be smooth and has a velocity that is prescribed in accordance with the angular velocity that is assigned to the cylinder, which is 1500 RPM for all the simulations. Given the diameter of the cylinder as 1 cm, an angular velocity of 1500 RPM would correspond to a cutting velocity of 47.1 m/min.

Meshing of the computational domain was done using ANSYS meshing module. A mixture of tetrahedral and quadrilateral elements have been used for construct-
A refined mesh was used in the region between the droplet nozzle and the cylinder, with the element size being 0.1 mm. Such a refinement was accomplished by constructing an inclined cylinder, extending all the way from the gas nozzle up to the vertical cylinder, and using it as a 'Body of Influence' in the meshing software, thereby generating a finer mesh in that region. Accuracy and stability of numerical computations get affected significantly by the orthogonal quality of the mesh. The "improve-quality" text command is executed multiple times to improve the cells with lowest orthogonal quality [91]. The Reverse Cuthill-McKee algorithm is used to reduce the bandwidth in order to increase the memory access efficiency, thereby improving the computational performance of
the solver. The number of nodes used in the mesh were 1,436,343 and the number of elements in the mesh were 4,504,839. ANSYS Fluent 15.0 software is used for simulating the numerical models presented in this study.

4.3 ACF spray system film formation model

For the research presented in this study, a 3-Dimensional wall film model has been implemented to simulate the film thickness of the liquid film formed, due to impinging of droplets, on a rotating cylindrical surface for one complete revolution. To simulate the film formation, the Eulerian wall film (EWF) model has been used.
4.3.1 Eulerian wall film (EWF) model

The EWF model is based on the thin film assumption, implying that the thickness of the film formed is small compared to the radius of curvature of the surface so that the properties do not vary across the thickness of the film and that films formed are thin enough so that the liquid flow in the film can be considered parallel to the wall [89]. Such an assumption would not affect the modeling presented in this research because the radius of curvature of the end-mill tool (modeled as a cylinder), 5mm, is three orders of magnitude larger than the radius of droplets, which is 7.5 µm and the film thickness could be assumed to be of the same order as that of droplet diameter [23].

Wall film is modeled by using the conservation equations of mass and momentum for a two-dimensional film in a three-dimensional domain, as shown below

\[
\frac{\partial h}{\partial t} + \nabla_s \cdot [h \vec{V}_l] = \frac{\dot{m}_s}{\rho}.
\]  

(4.11)

In the mass conservation equation, \( \rho \) is the density of the cutting fluid, \( h \) the film height, \( \nabla_s \) is the surface gradient operator, \( V_l \) the mean film velocity and \( \dot{m}_s \) the mass source per unit wall area due to droplet collection, film separation, film stripping, and phase change. Conservation of film momentum is given by

\[
\frac{\partial h \vec{V}_l}{\partial t} + \nabla_s \cdot [h \vec{V}_l \vec{V}_l] = -\frac{h \nabla_s P_L}{\rho_l} + (\vec{g}_r)h + \frac{3}{2\rho_l} \tau_{fs} - \frac{3\nu_l}{h} \vec{V}_l + \frac{\bar{q}_s}{\rho}.
\]  

(4.12)
where

\[ P_L = P_{\text{gas}} - \rho h (\vec{n} \cdot \vec{g}) - \sigma \nabla s \cdot (\nabla_s h). \quad (4.13) \]

The terms on the left hand side of Eq. 4.12 represent transient and convection effects, respectively. On the right hand side, the first term includes the effects of gas-flow pressure, the gravity component normal to the wall surface (known as spreading), and surface tension; the second term represents the effect of gravity in the direction parallel to the film; the third term is the viscous shear force at the gas-film interface; the fourth term represents the viscous force in the film, and the last term is associated with droplet collection or separation [89]. The energy equation can also be solved in the EWF model, however in the present study no thermal modeling is done and hence energy equation is not included in the EWF model.

When liquid droplets strike against the wall of the cylinder, the droplets gets absorbed and its momentum and mass get transferred to the liquid film. Equation 4.11 is able to account for the increase in mass of the liquid film by equating \( \dot{m}_s \) to \( \dot{m}_{ip} \), where, \( \dot{m}_{ip} \) is the flow rate of the liquid droplets impinging on the cylindrical surface. The momentum equation, Eqn. 4.12 for film, is able to account for the increase in momentum of the film due to the impinging droplets by replacing the momentum source term \( \dot{\vec{q}}_s \) by

\[ \vec{q}_s = \dot{m}_{ip} \cdot (\vec{V}_p - \vec{V}_l), \quad (4.14) \]
where, $V_p$ denotes the velocity of the impinging liquid droplet and $V_l$ denotes the liquid film velocity [89]. The EWF model was formulated to also account for splashing of liquid droplets and a detailed numerical set up of the splashing model can be found in [89].

The scheme used for discretizing the continuity and momentum equations, Eqn. 4.11 and 4.12, respectively, is the First Order Upwind scheme, which is the default scheme for solving wall film models in ANSYS Fluent. Discretization of the temporal terms in Eqn. 4.11 and 4.12 is accomplished using the First Order Explicit Scheme. First order discretization schemes are recommended, for modeling wall films, over other discretization schemes because of the superior convergence capabilities of first order schemes. Adaptive time stepping scheme, with maximum Courant Number as 1 and initial time step size as $10^{-6}$, is used for evaluating the time step, $\partial t$, in Eqn. 4.11 and 4.12.

When EWF model is used in the numerical simulations, such as the one presented in this study, boundary conditions need to be specified for different elements, in the computational domain, on which liquid films are formed. Here the boundary condition for the cylinder’s surface is set as "Trap", in order to absorb the liquid droplets impinging on it. The initial conditions of the film thickness ($h$) and film velocity ($V_l$) on the cylinder are set to zero because prior to impingement of droplets there is no liquid film on the cylinder’s surface.
4.3.2 Film thickness simulations

Film formation is critical to the cooling and lubrication functionality of the ACF spray system [21, 8]. As a consequence, different combinations of spray parameters could be evaluated by studying the characteristics of the liquid film. Out of the many different characteristics, film thickness has been singled out as the most critical one [82, 23]. Extent of spreading of a liquid film, can be judged by knowing the film thickness values. Higher film thickness corresponds to lower amount of spreading and vice versa [82]. For the ACF spray system, it is desired that the liquid film spreads out and uniformly wets the machining area [8].

Table 4.1 shows the different combinations of spray parameters that were discussed in Section 3.2 and were used for conducting the experiments. The same combination of spray parameters would be used for carrying out liquid film formation simulations.

Table 4.1: Values of droplet velocity at given ACF spray conditions

<table>
<thead>
<tr>
<th>Test</th>
<th>Distance (mm)</th>
<th>Pressure (psi)</th>
<th>Carrier gas velocity (m/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>T1</td>
<td>40</td>
<td>9</td>
<td>27</td>
</tr>
<tr>
<td>T2</td>
<td>60</td>
<td>9</td>
<td>14</td>
</tr>
<tr>
<td>T3</td>
<td>40</td>
<td>15</td>
<td>33</td>
</tr>
<tr>
<td>T4</td>
<td>60</td>
<td>15</td>
<td>25</td>
</tr>
</tbody>
</table>

For the combinations of spray parameters, as shown in Table 4.1, numerical simulations have been conducted to monitor the liquid film formed on the cylinder, rotating at 1500 RPM. The simulations yield the film thickness contours for
different spray parameters, at the end of one complete revolution of the cylinder, and are shown in Fig. 4.10. From Fig. 4.10 (a), (b), (c) and (d), it can be observed that the maximum film thickness value, shown on the legend bar of the contour plots, for T3 condition is quite different from those of the other conditions, namely, T1, T2 and T4. It can also be inferred that the film thickness contours are quite varied for the different spray parameters.

![Con tours of film thickness for different spray parameters](image)

Figure 4.10: Contours of film thickness for different spray parameters

To characterize the liquid film for different combinations of spray parameters, liquid film thickness along the circumference of the cylinder has been evaluated. Liquid film thickness has been computed at distances of 1 mm, 2 mm, 3 mm and
4 mm from the base of the cylinder, as shown in Fig. 4.11. The top view of the numerical setup of the spray and cylinder is shown in Fig. 4.12. The value of $\theta$ varies from 0 to 360°. Note that the film thickness is estimated after the cylinder has made one complete revolution, from the time the first droplet impinges on the cylinder. The values of film thickness obtained, after one complete rotation of the cylinder, as a function of angular position for different combinations spray parameters and at different locations along the cylinder are shown in Fig. 4.13, 4.14, 4.15 and 4.16, respectively.

![Figure 4.11: Position of film thickness measurement, relative to the ACF spray and cylinder.](image1)

![Figure 4.12: Top view of the numerical setup.](image2)
Figure 4.14: 0.2 cm
On comparing Fig. 4.13, 4.14, 4.15 and 4.16, it was observed that between angular positions 180° - 220°, there is a global minimum value of film thickness, and the peak value of film thickness is observed to just left of the minimum film thickness angular position. However, the film thickness has less fluctuations away from the position where maximum or minimum film thickness occurs. It was also observed that there is a significant amount of fluctuation in film thickness at a distance of 0.1 cm from the base of cylinder, as shown in Fig. 4.13. However, when film thickness is recorded at distances of 0.2, 0.3 and 0.4 cm from the base of the cylinder, the film thickness undergo less fluctuations, as the angular position is varied. Therefore, it can be concluded that at distances of 0.2, 0.3 and 0.4 cm from the base of the cylinder, the liquid film has reached a stable value.

The minimum film thickness appears more or less at similar angular positions because the orientation of the ACF spray system is fixed and high droplet carrier gas velocity along the axis of gas nozzle provides sufficiently high momentum to the droplets. This enables it to spread out in all the directions leaving a region of low film thickness at the point of impingement of high velocity jet on the cylinder. A possible reason for observing excessive fluctuations in film thickness at a distance of 0.1 cm from the base of cylinder is that the film thickness in regions too close to the base of the cylinder, are in transient state, owing to their proximity to the point of impact of the spray. On the contrary, film thickness in regions farther away from the base of the cylinder, show little variation in film thickness, for different spray conditions, due to the loss in excessive momentum
of the fluid film after it has traveled some distance.

The 3-D plot of liquid film thickness, for T4 spray condition, is plotted in cylindrical coordinates and is shown in Fig. 4.17. As seen in Fig. 4.17 the liquid film thickness varies considerably near the point of impact of the spray and at distances farther away from it, the liquid film thickness stabilizes.

![3-D plot of liquid film thickness](image)

**Figure 4.17:** 3-D plot of the film formed on the rotating cylindrical surface

In order to obtain a steady state value of film thicknesses for different combinations of spray parameters on a rotating cylindrical surface, an average of computed film thicknesses at distances of 0.2, 0.3 and 0.4 cm from the base of the cylinder have been evaluated and its variation in the Cartesian coordinates is shown in Fig. 4.18. The average film thickness as a function of angular position has also been plotted in Fig. 4.19. Film thickness obtained at a distance of 0.1 cm from the base...
of the cylinder has not been included in the calculation of average film thickness, as it represents transient conditions. The average film thickness over the entire circumference of the cylinder has also been calculated. For spray conditions, T1, T2, T3 and T4, the average film thickness over the circumference of the cylinder are: 7.3, 10.1, 4.2 and 8.1 $\mu m$, respectively.

Figure 4.18: 3-D plot of average film thickness as a function of angular position, in Cartesian coordinates
Figure 4.19: Average film thickness
As seen from Fig. 4.18 and 4.19, the average liquid film thickness is the largest for the combination of spray parameters under T2 condition (spray distance: 60 mm, carrier gas inlet pressure: 9 psi, droplet velocity: 14 m/s) and the smallest for T3 condition (spray distance: 40 mm and carrier gas inlet pressure: 15 psi, droplet velocity: 33 m/s), among the four different combinations of spray parameters studied in this research. For T3 condition, the droplet velocity is 33 m/s, which is much higher than the droplet velocity under T2 condition. It can be inferred that higher droplet velocities lead to thinner fluid films and lower velocities lead to thicker fluid film, this conclusion is in agreement with the results obtained by Kalantari et al. [22].

In Section 3.3.1, it was shown that the cutting forces reduce significantly when the ACF spray system is used in machining titanium alloys: as compared to dry cutting or flood cooling. It was believed that the liquid film formed by the spreading of the impinging droplets play a role in ensuring that the cutting fluid is able to reach the tool-chip interface and provide cooling and lubrication at the cutting zone. The cutting forces obtained for the different combinations of spray parameters have been reported in Section 3.3.1 and again shown in Fig 4.20. As seen from Fig. 4.20, the cutting forces for T1 (spray distance: 40 mm, carrier gas inlet pressure: 9 psi, droplet velocity: 27 m/s) and T4 (spray distance: 60 mm, carrier gas inlet pressure: 15 psi, droplet velocity: 25 m/s) conditions are lower than that for T2 and T3 conditions.
This observation can be explained in terms of the average film thickness values, along the circumference of the rotating tool, for the different combinations of spray parameters. When the liquid film is thinner, 4.2 $\mu m$ (smallest among the four spray conditions), the liquid film could be considered thin enough to be evaporated even before reaching the cutting zone and little or no liquid would be present for providing lubrication at the tool-chip interface. Film thickness larger than a certain value is also not preferable because in such a situation the liquid film may not be able to penetrate the cutting interface and provide cooling and lubrication. For example, the film thickness for T2 spray condition is, 10.1 $\mu m$, the thickest among the film thickness values encountered in this study and it is possible that the thick liquid film may be preventing the cutting fluid to reach the cutting zone and this causes the high cutting forces. For other spray conditions, the liquid film is adequate enough to be able to access the cutting zone and provide substantial cooling and lubrication at the cutting zone. As a result, the cutting forces for T1 and T4 conditions are lower than T2 and T3 conditions.

4.4 Chapter summary

In this chapter, a physics-based understanding of the variation in cutting force values that are observed when end-milling titanium alloy, using the ACF spray system with different combinations of spray parameters. The characteristics of the cutting fluid film, which is formed on a rotating cylinder for different spray
conditions, are studied. The DPM approach has been adopted to model the ACF spray system and film formation on the rotating surface has been simulated by using the EWF model. Specific conclusions of this chapter are:

1. There seems to exist a relationship between the liquid film thickness and the droplet velocity. Higher droplet velocities resulted in more spreading of film. As a result, lower film thickness values were obtained. Lower droplet velocities, lead to slower spreading of the liquid film and consequently, higher film thickness was recorded for such a scenario.

2. The steady state film thickness values can be used to explain the variation in cutting forces for different combinations of spray parameters. In situations where the spreading fluid film is not able to reach the tool-chip interface, either due to being too thick or thin enough to get evaporated, the cutting forces seem to be higher as compared to other test situations, with different
conditions of ACF spray parameters.
Chapter 5

Conclusions and recommendations

5.1 Conclusions

This thesis investigates the effect of atomozation-based cutting fluid (ACF) spray system’s parameters and machining conditions on the machinability of titanium alloys in macro-scale end-milling operations.

The research was carried out in two phases. In the first phase, focus was on experimental investigation of the effect that the variation of ACF spray parameters would have on the measured cutting forces when end-milling a titanium alloy. A testbed was developed and machining parameters were ascertained that would be used to evaluate the different combinations of spray parameters. A numerical model of the ACF spray system was developed and simulations were conducted with the objective of getting an insight into the characteristics of the liquid film formed by the impinging droplets on a rotating cylindrical surface. The simulation results were utilized for explaining the experimentally observed variation in cutting forces for different combinations of ACF spray parameters.
In the second phase, emphasis was given on experimentally comparing the machining performance of the ACF spray system with two other cutting fluid application methods, viz., dry cutting and flood cooling. Tool-life experiments were conducted and machining parameters recorded, when end-milling titanium alloy for a particular machining condition but using different cutting fluid application methods. Furthermore, the performance of the ACF spray system for different machining conditions was also evaluated based on the experimental work. The following conclusions can be drawn from this study.

5.1.1 Experimental investigation

The following conclusions can be drawn from the experimental work conducted in this thesis:

1. For the ACF spray system to be effective in end-milling of titanium alloys, it was concluded that the ACF spray unit should be placed at an angle of $135^\circ$ with respect to the feed direction.

2. For the different combinations of the ACF spray parameters tested in this study, it was seen that as long as the droplet velocity for a particular combination of spray parameters is such that the droplets lie well within the spreading regime, the machinability of titanium alloy would be better with the usage of ACF spray system as compared to flood cooling.
3. The ACF spray system was able to extend the tool life as high as 75% over flood cooling when the feed/tooth and axial depth of cut values were chosen as 0.1 mm/tooth and 0.5 mm, respectively.

4. It was observed, that the tool life increases by 50% when the droplet carrier gas is a mixture of air and CO$_2$ as compared to the case when droplet carrier gas has only air. Furthermore, the ACF spray system even in the absence of CO$_2$ from the gas mixture, can achieve upto 17% more tool life as compared to flood cooling. This observation goes on to show that the atomized droplets of the ACF spray system are effectively able to reach the cutting zone, which is not the case for flood cooling.

5. Experimental results show that when ACF spray system is used, the cutting edges undergo uniform wear and the tool does not fail catastrophically due to attritional wear caused by lower temperatures of the cutting edges. On the other hand when dry and flood cooling cutting conditions are used, the tool wears out rapidly and undergoes catastrophic failure because of chipping and notch formation on the tool flank face.

6. Superior cooling and lubrication capabilities of ACF spray system is instrumental in reducing the tool flank wear and hence the cutting forces and surface finish.

7. For a particular machining condition, the length of the chips generated with
the ACF spray system is shorter than those that are generated with flood cooling. Thereby, suggesting that chip breakability is enhanced by using the ACF spray system.

8. Ability of the ACF spray system to reduce cutting forces and surface roughness is dependent upon the machining conditions. The cutting forces and surface roughness increase more for an increase in feed/tooth as compared to an increase in axial depth of cut alone.

5.1.2 Modeling liquid film formation on a rotating surface

1. It was observed that for the design of ACF spray system used for this study, the droplet carrier gas velocities are highest at the 90° bend of the carrier gas inlet tube. The most probable reason for this observation is that the pressure loss associated with a sudden bend in fluid flow is quite significant and this gets manifested in the from of large increase in velocity of liquid. In the numerical simulations it was also observed that the high velocities of the carrier gas causes entertainment of the surrounding air that results in the generation of a focused jet of liquid droplets.

2. There exists a relationship between the liquid film thickness and the droplet velocity. Higher droplet velocities resulted in more spreading of film. As a result, lower film thickness values were obtained. Low droplet velocities,
lead to slower spreading of the liquid film and consequently, higher film thickness was recorded for such a scenario.

3. The average film thickness values can be used to explain the variation in cutting forces for different combinations of spray parameters. The thickest and the thinnest liquid films, formed due to the impinging of droplets on a rotating cylinder, among the different spray conditions, resulted in generation of high cutting forces. In these situations the spreading fluid film might not have been able to reach the tool-chip interface and hence could not provide lubrication at the interface, resulting in generation of high cutting forces.

5.2 Recommendations for future work

Below are suggestions for extending the research in order to better understand and evaluate the performance of the ACF spray system in macro end-milling operations.

1. In the experiments that were conducted as a part of this study, only one set of nozzle is used. However, in the study conducted by Sasahara et al. [94] it was concluded that by using multiple nozzles instead of one, for the same cutting fluid consumption, leads to better machinability of aluminum alloy during milling with MQL, because by using two nozzles, greater area of the cutting tool remains lubricated during the metal cutting process. Therefore,
further studies should be conducted to assess the effect of number of nozzles on the machinability performance of the ACF spray system when milling titanium alloys. A starting point could be to use the configuration proposed in [94], where two nozzles are placed diametrically opposite to one another.

2. The cutting fluid used as a part of this study has a definite composition, 10% metal working fluid (MWF) and 90% water, resulting in fixed physical properties of the cutting fluid. Nath et al. [95] conducted turning experiments with the ACF spray system and concluded that the concentration of the MWF in cutting fluid affects the performance of the ACF spray system because different concentrations of MWF in the cutting fluid, lead to variation in viscosity and surface tension of the cutting fluid. These two physical properties have been shown to significantly affect the cooling and lubrication performance of the cutting fluid during machining [82]. Consequently, the effect of cutting fluid composition during end-milling of titanium alloy with the ACF spray system should be further explored.

3. Temperature measurement studies at the tool chip interface should be conducted. Such a study will help in defining the liquid film penetration capabilities in end-milling when the ACF spray system is used. Hoyne et al. [96] used thermocouples to measure the temperature at the cutting interface, when the ACF spray system is used for turning titanium alloys. Similar approach can be used for ascertaining the cutting zone temperatures during
end-milling. [97].

4. The ACF spray system should be evaluated for high speed end-milling operations. In the present study, experiments have been conducted with medium cutting speeds, however, the higher temperature gradients and material removal rates associated with high speed machining, can affect the film formation mechanism of the ACF spray system.

5. Further experimentation should be conducted to gain a better understanding of the lubrication and cooling mechanisms of spray-based systems in end-milling operations. Such an understanding would help in selection of the spray parameters and the composition of the cutting fluid. High speed cameras have been used by Hoyne et al. [21], to study the lubrication mechanism of the ACF spray system in turning operations, and the same can be used for end-milling processes.

6. In this research, the ACF spray system has been evaluated for machining titanium alloys. However, in the future, further machining experiments should be conducted using different difficult to machine materials, such as: tool steels and nickel based alloys, to name a few, in order to assess the range of materials that could benefit from the application of the ACF spray system.

7. A significant improvement could be made to the film formation model pre-
sented in this study by incorporating the effect that the presence of thermal
gradients, on the surface over which the film is flowing, would have on the
droplet impingement dynamics and the subsequent formation and spreading
of liquid film on the rotating surface. To model such a scenario, constant
heat flux boundary condition can be applied to the surface. Heat flux val-
ues can be selected on the basis of heat generated during milling of titanium
alloys.

8. The numerical model presented in this study should be modified to account
for the presence of surface roughness and ridges on the surface on which
the droplets are impinging and forming a film. Presence of such surface
features in the numerical model would give an accurate representation of
the spreading of the liquid film. The roughness of the surface could be
measured using a profilometer and this value could be used as an input to
the numerical model.

9. The numerical model should be modified to taken into account the inter-
action between a metal chip and the spreading film. Such a model would
not only give further insights into the cooling and lubrication mechanisms
of the ACF spray system but could also be used for modeling the cutting
forces encountered during end-milling with the ACF spray system.
References


