

CFRTP Drilling Using WC-Co CVD Diamond Coated Tools

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Master Dissertation

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*“Nothing in the world is worth having or
worth doing unless it means effort”*

Theodore Roosevelt

Abstract

CFRTP (carbon fiber reinforced thermoplastic) material's combination of high strength and low weight has resulted in their increased usage throughout industry. Due to their thermoplastic matrix, these materials have been gaining more relevance over their thermoset counterparts, owing to their better sustainability through enhanced recyclability. The production processes in these materials often require drilling operations for the joining of several parts, which presents issues related with the anisotropy of the material and abrasive nature of carbon fibers, with delamination, burr and thermal damages being common. To prevent these issues, an adequate selection of cutting parameters must be performed, along with picking a cutting tool with optimal geometry and material. The usage of numerical simulation techniques aids the research of the drilling process of these materials, presenting a less costly option to laboratory testing.

In the present work, the drilling of a carbon fiber reinforced polyamide 12 (PA 12) material using WC-Co chemical vapor deposition (CVD) diamond coated drills has been studied. After a state-of-art review, experimental studies were conducted considering combinations of two drill geometries (conventional and double-point angle twist drills) with two different diamond coatings applied through CVD (microcrystalline (MCD) and nanocrystalline (NCD)), at three feed and cutting speed levels. To compare the various combinations, the cutting loads, through peak thrust force and drilling torque, and the hole damage, through delamination, burr, surface roughness and accuracy, were registered and analyzed. An examination onto the machining chips and tool wear was also conducted. The material's tensile, compressive and shear properties were determined, with whose a finite element analysis (FEA) model was developed for the drilled material, to serve as the base for a future drilling numerical simulation.

It was found that the feed had the most influence towards the peak thrust force, delamination and torque, with higher values of each output obtained at higher feed values. The combination of high feed and cutting speed values resulted in holes with better wall surface finish and less burrs. Regarding hole accuracy, larger diameter holes with higher positive deviations from the nominal value were observed at higher cutting speeds. The MCD coated tools induced both lower thrust forces and more accurate holes than their NCD counterparts, partly owing to the lower thickness of the MCD coating resulting in a smaller cutting edge radius (CER). The chips exhibited a continuous ribbon-like morphology, with a tendency to clogging the cutting tool, partly caused by the high ductility of the material's PA matrix. The analysis of conventional twist drill with a MCD coating after 3800 mm of total drilled distance exhibited a tendency for abrasion on the tool's flank face near the cutting edge, along with catastrophic failure through coating delamination at the drill's corners. The developed material FEA model accurately depicted its behavior when under tensile and compressive loads, but further work is required regarding shear loads.

Keywords: CFRTP, Drilling, CVD diamond coatings, Microcrystalline diamond coating, Nanocrystalline diamond coating, Machining thrust forces, Delamination, Burr, Conventional twist drill, Double-point angle twist drill, FEA.

Resumo

A combinação de boas propriedades mecânicas e baixo peso resultou numa maior utilização industrial de materiais compósitos de matriz termoplástica reforçados com fibras de carbono. Em relação aos materiais cuja matriz é termoendurecível, a matriz termoplástica destes permite a criação de materiais mais sustentáveis, visto que são mais facilmente recicláveis. Os processos de fabrico utilizados nestes materiais requerem operações de furação, de modo a que as várias partes individuais possam ser montadas entre si, o que apresenta bastantes problemas particularmente associados à anisotropia do material e à natureza abrasiva das fibras de carbono, sendo comum o aparecimento de delaminação, fibras por cortar e danos térmicos. De modo a prevenir estes problemas, é essencial a seleção de parâmetros de corte correctos, e a escolha de uma ferramenta de corte cuja geometria e material sejam adequados. A utilização de técnicas de simulação numérica auxilia na investigação do processo de furação nestes materiais, sendo uma opção menos dispendiosa face a experiências laboratoriais.

No presente trabalho, foi estudado o processo de furação de um material compósito com matriz poliamida 12 reforçado com fibras de carbono utilizando ferramentas em WC-Co revestidas em diamante por CVD. Após uma revisão do estado-da-arte, realizaram-se experiências laboratoriais considerando brocas com duas geometrias distintas (convencional e com dois ângulos de ponta) e com dois tipos de revestimentos de diamante aplicados por CVD (microcristalino e nanocristalino), a três níveis de avanço e velocidade de corte. De modo a comparar as várias combinações, foram registadas e analisadas as solicitações mecânicas durante a furação, por meio dos picos de força axial e binário, e a qualidade do furo, através da delaminação, fibras por cortar, rugosidade da parede e precisão. Foi ainda realizada uma análise das aparas resultantes do processo de maquinagem e do desgaste de ferramenta. As propriedades mecânicas do material em solicitações de tração, compressão e corte foram determinadas, para que um modelo de elementos finitos do material pudesse ser desenvolvido, servindo de base para uma futura simulação numérica do processo de furação.

O avanço foi o parâmetro de corte que demonstrou uma maior influência na força axial máxima, delaminação e binário máximo, em que maiores avanços resultavam em maiores valores de cada um destes resultados. A combinação de avanços e velocidades de corte elevadas demonstrou-se benéfica no sentido de melhorar a qualidade da parede do furo e diminuir a quantidade de fibras por cortar. Em relação à precisão, velocidades de corte altas produziram furos de maior diâmetro, com um maior desvio positivo face ao valor de diâmetro nominal. As ferramentas com revestimento em diamante microcristalino atingiram, particularmente quando utilizadas a velocidades de corte e avanços mais elevados, valores máximos de força axial mais reduzidos e furos mais precisos do que as ferramentas com revestimento em diamante nanocristalino, o que deverá estar parcialmente associado à menor espessura do revestimento em diamante microcristalino levar a que a ferramenta tenha um menor raio de aresta de corte. As aparas obtidas apresentaram uma morfologia contínua e ondulada, com tendência para entupir a broca, o que deverá ser em parte causado pela ductilidade elevada da matriz poliamida. A análise de uma broca de geometria convencional e revestimento em diamante microcristalino após 3800 mm de distância de furação demonstrou alguma abrasão da face de saída da broca junto à aresta de corte, bem como uma falha catastrófica da broca resultante de delaminação do revestimento. O modelo de análise por elementos finitos do material foi capaz de simular o comportamento a solicitações de tração e compressivas com precisão, mas é ainda necessário mais desenvolvimento em solicitações ao corte.

Palavras-chave: Materiais compósitos de matriz termoplástica reforçados por fibras de carbono, Furação, Revestimentos em diamante por CVD, Revestimento em diamante microcristalino, Revestimento em diamante nanocristalino, Força axial de furação, Delaminação, Fibras por cortar, Broca com geometria convencional, Broca com dois ângulos de ponta, Análise por método dos elementos finitos.

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List of Abbreviations

ABS	Acrylonitrile Butadiene Styrene
ANOVA	Analysis of Variance
CER	Cutting-Edge Radius
CFRP	Carbon Fiber Reinforced Plastics
CFRTP	Carbon Fiber Reinforced Thermoplastic
CLC	Combined Loading Compression
CT	Compact Tension
CVD	Chemical Vapor Deposition
DCB	Double Cantilever Beam
DIC	Digital Image Correlation
ENF	End Notched Flexure
FEA	Finite Element Analysis
FEM	Finite Element Methods
FRP	Fiber Reinforced Polymer
HFCVD	Hot Filament Chemical Vapor Deposition
HSS	High Speed Steel
MCD	Microcrystalline Diamond
MMB	Mixed-Mode Bending
MLQ	Minimum Quantity Lubrication
NCD	Nanocrystalline Diamond
PA	Polyamide
PA 12	Polyamide 12
PAN	Polyacrylonitrile
pCBT	Poly-Cyclic Butylene
PCD	Polycrystalline Diamond
PEEK	Polyetheretherketone
PEKK	Polyetherketoneketone
PI	Polyimide
PMC	Polymer Matrix Composite
PVD	Physical Vapor Deposition
Ra	Arithmetic average roughness
Rz	Average maximum height of the profile
SEM	Scanning Electron Microscopy
SLB	Single-Leg Bending

List of Symbols

A	Cross-section area
a_0	DCB specimen pre-crack length
A_{del}	Delamination area
A_{max}	Maximum delamination area
A_{nom}	Nominal hole area
Al_2O_3	Alumina
b	DCB specimen width
Co	Cobalt
d	Scalar damage variable
D_{core}	Diameter of core part of step drill geometry
D_{step}	Diameter of step part of step drill geometry
D_{min}	Minimum delamination area diameter
D_{nom}	Nominal hole diameter
E_i	Young's/Elasticity modulus along i direction
E_{ic}	Compressive modulus in i direction
f	Feed
F	Force perpendicular to cross-section
F_a	Two-dimensional delamination factor
F_d	Conventional delamination factor
F_{da}	Adjusted delamination factor
F_{dmin}	Minimum delamination factor
F_{ed}	Equivalent delamination factor
F_i	Second rank stress tensor
F_{ij}	Fourth rank stress tensor
F_v	Three-dimensional delamination factor
G^C	Total dissipated energy due to failure
G_{ij}	Shear modulus along i-j plane
$G_n/G_s/G_t$	Instantaneous fracture energies along normal, first and second shear directions
$G_n^C/G_s^C/G_t^C$	Critical fracture energies along normal, first and second shear directions
G_{1c}^c/G_{2c}^c	Compressive intralaminar fracture energy along the longitudinal/transverse direction
G_{1c}^t/G_{2c}^t	Tensile intralaminar fracture energy along the longitudinal/transverse direction
h	DCB specimen thickness
K_i	Linear elastic behavior stiffness

L	DCB specimen length
l_g	Length of gage section
P	DCB specimen load
$t_n/t_s/t_t$	Components of normal, first and second shear tractions
$t_n^0/t_s^0/t_t^0$	Peak nominal stress in normal, first and second shear directions
TaC	Tantalum carbide
TiC	Titanium carbide
TiCN	Titanium carbo-nitride
TiN	Titanium nitride
V_c	Cutting speed
WC	Tungsten carbide
ZrO₂	Zirconium
γ	Shear strain
$\epsilon_{+45^\circ}/\epsilon_{-45^\circ}/\epsilon_t/\epsilon_l$	Strain along +45°, -45, transverse and longitudinal directions
η	Material parameter from MMB testing
θ_{step}	Angle between core and step parts of step drill geometry
ν_{ij}	Poisson's coefficient along i-j plane
σ_i^{cu}	Ultimate compressive strength along i direction
σ_{ij}	Stress components along ij
σ_i^{tu}	Ultimate tensile strength along i direction
τ_{ij}	Shear stress along i-j plane
τ_{ij}^u	Ultimate shear strength along i-j plane

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Chapter 1

Introduction

1.1. Framework and Motivation

Carbon fiber reinforced plastics (CFRP) have been increasingly employed in structural applications, throughout several industries. This is attributed to these materials' combination of high strength and stiffness with low weight, which is an attractive combination in both high-performance applications, such as in the aerospace industry, or when looking to maximize efficiency, like in the automotive industry. These materials can be divided in thermosets and thermoplastics (in which case they are designated as CFRTTP), dependent upon the nature of its resin.

The manufacturing processes behind CFRP allows the materials to be obtained in a near net shape state, but to assemble the several parts into a final product, joining processes are required. This is usually achieved through using mechanical fastening (i.e. bolted connections), which requires hole drilling. The drilling process in these materials presents several challenges, related with their high degree of anisotropy and to the very abrasive nature of the reinforcement (i.e. carbon fiber), which may significantly affect cutting tool wear. The appearance of defects like delamination or burr are also common and quite problematic, as they negatively impact the material's performance, and their repairing processes are time consuming, increasing the parts costs and decreasing the production volumes. Thereby, it is necessary to optimize several aspects of the drilling process, so that these kinds of damages are prevented. The selection of adequate cutting parameters is very important, as these have a significant influence upon the damage to the workpiece. Besides, the tool geometry can be optimized towards reducing both burr and delamination, and the tool material and coating can both influence its cutting performance and improve its wear resistance. FEA methods are a powerful tool towards finding the answer to most of these issues, as they can provide a cheaper option in research instead of experimental testing. Most of the research related to the drilling of CFRP is concerned with thermoset resins, but still CFRTTP materials have been gaining more relevance due to their superior recyclability, and, thus, enhanced sustainability.

The present work focuses on the drilling of a carbon fiber reinforced material, more specifically CFRTTP with a PA12 matrix, using WC-Co diamond coated tools. The aspects that will be varied to both examine their influence and find an optimum solution will be the drilling tool's geometry and coating, and the drilling parameters, feed and cutting speed. The outputs that will be analyzed to compare the several combinations will be the cutting loads, both in the form of the thrust force and drilling torque, and the hole quality indicators, such as

delamination, burr, hole accuracy and the hole's surface roughness. Another focus will be to both analyze the chips resultant from the drilling process, and the wear patterns upon the drilling tool, to assess the tool life and failure modes. The material's mechanical properties will also be tested, so that the material can be modelled into a FEA software, setting the base for a possible future drilling process FEA model.

1.2.Objectives

The main goal of the present study is to assess the machinability of drilling operations on CFRTTP material using CVD diamond coated tools, allowing for the identification of efficient operation conditions by reducing the damage exerted upon the workpiece. The main steps followed towards this goal will be:

- Literature and state-of-art review of the drilling process on CFRP materials, concerning the drilling process, damage control on the machined parts and the application of FEA in the drilling of CFRP;
- Planning of drilling experiments, selecting the cutting tools and parameters;
- Characterization and assessment of the drilling process through the measurement of cutting loads and analysis of the drilled holes and cutting tools;
- Determination of material's mode I fracture energy for critical thrust force assessment;
- Mechanical characterization of the drilled material, and subsequent material modeling through numerical simulation.

1.3.Document's Structure

This thesis is divided in a total of 6 chapters. In the first chapter, a brief introduction to the research theme is provided, objectives are established, and an overview of the document's structure is presented.

In chapter 2, a literature review is conducted. This includes an overview regarding composite materials, followed by an explanation regarding the basics of drilling process. The mechanics behind the chip removal process are also explored, approaching delamination, burr formation and the hole's surface roughness. With the delamination, a brief review regarding fracture modes and fracture energy is provided since this parameter can help predict delamination. Several drilling tool geometries and materials are then explored, and an overview regarding tool wear is also provided. Finally, a more detailed review of drilling of CFRTTP materials is also performed.

In chapter 3, all the conducted experimental methodologies as well as the several analysis techniques are exposed. This chapter begins with the material testing methodologies, both in terms of tensile, compressive and shear properties, that will be later used in the FEA models. In addition, the drilling parameters, hole analysis techniques and other drilling process aspects are exhibited.

In chapter 4, all the experimental results are exposed and analyzed, starting with the material testing, and followed by the drilling experiments.

In chapter 5, FEA models of the material testing procedures are developed, using the previously determined properties in chapter 4.

Chapter 6 is the final chapter of the thesis, where some final conclusions are elaborated and some suggestions for future works are provided.

Chapter 2

State-of-art and Literature Review

2.1.Composite Materials

In its most general definition, a composite material is a material with two or more components. The most popular composite materials are fiber reinforced composites (FRC), which are materials composed of a rigid matrix, that holds the material' shape and transfers the load to the reinforcements in the form of fibers. This way, these materials take advantage of the individual properties of each of its components, resulting in a material with superior properties. Out of all the types of FRC, polymer matrix composites (PMC) or fiber reinforced polymers are the most popular, and the ones that will be focused on in this review. These materials allow to produce lightweight parts, with high specific strength and stiffness. They have a wide range of applications along several industries, which include aerospace, automotive and sports equipment [1-6]. Taking the example of the automotive industry, these kinds of materials are mainly used for body panels, both in the form of glass and carbon fibers. Their main advantages in these applications are their high energy absorption capabilities, resistance to corrosion and, most importantly, weight reduction. This significantly benefits the transportation sector, where CFRP have been reported to be able to reduce fuel consumption, and in high performance or motorsport, where a lower weight can greatly increase the performance [7]. An example of an application is in the Porsche Carrera GT, where the usage of CFRP is claimed to allow a weight reduction of up to 40%, with the monocoque presented in Figure 1 having 100 kg [8].



Figure 1 - Porsche Carrera GT chassis, sub-frame and crash structures (adapted from [8]).

A FRC material is usually composed of several layers, which are denominated as plies or lamina. A ply is, in the case of unidirectional composites, made from several fibers along a single direction (as represented in Figure 2a) or, in the case of a fabric, a single layer of woven fibers. A laminate corresponds to a set of stacked (and bonded) plies. In Figure 2b an example of laminate with unidirectional plies is presented, where the top and bottom plies have a 0° orientation, and the middle ply has a 90° orientation, even though other directions like, for example, 45° , are also used. These orientations are used to characterize the architecture of the material. A two-dimensional fabric is composed of several fibers along different directions on the ply's plane. One example of these kinds of fabrics are woven, composed by a set of fibers in a 0° orientation, called the warp, and another at 90° , called the weft. In Figure 3, some examples of two-dimensional fabrics are represented, mainly the plain woven (Figure 3a), the twill weave (Figure 3b) and the satin weave (Figure 3c). Apart from two-dimensional fabrics, there are other kinds of fabrics such as three-dimensional and non-crimp fabrics [1, 5, 9, 10].

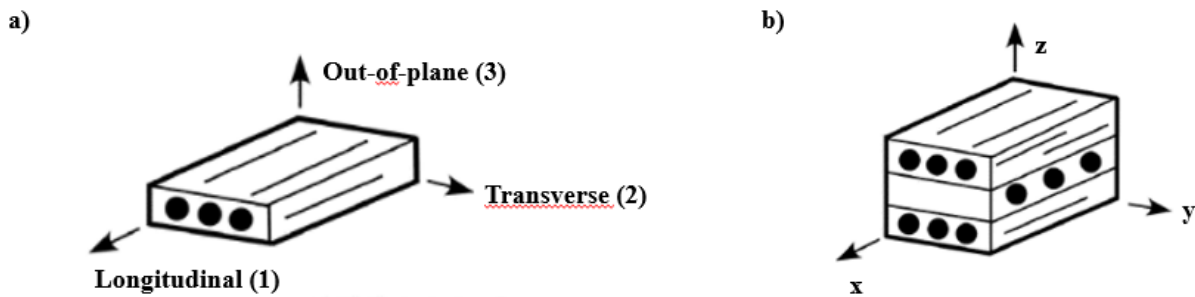


Figure 2 - Classification of composite materials according to ply directions and stacking (adapted from [9]): (a) unidirectional ply directions; (b) laminate composed of unidirectional plies.

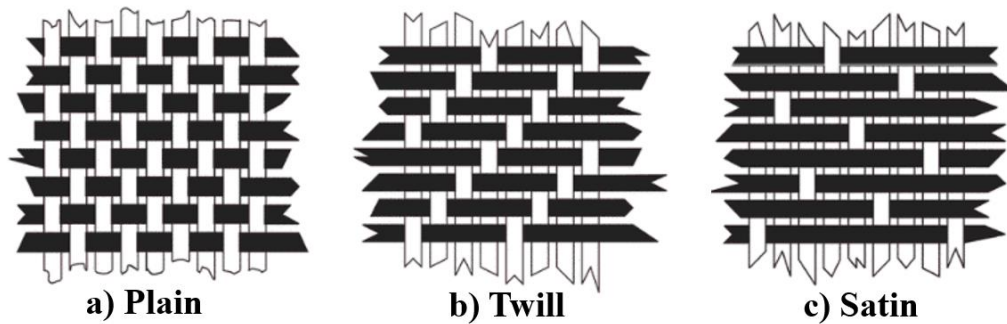


Figure 3 - Common types of composite fabrics (adapted from [10]): (a) Plain weave; (b) Twill weave; (c) Satin weave.

PMC are anisotropic, meaning that the material's mechanic response is different considering the loading direction. Inside the class of anisotropic materials, they are considered orthotropic, because their properties vary in three perpendicular directions, as represented in Figure 2a: longitudinal, which is the direction along the fibers, transverse, that corresponds to the direction across the fibers but in-plane, and out-of-plane or through thickness. The anisotropy of the materials can be verified when a tensile load is applied for example, as in the case of an unidirectional laminate, the elasticity modulus along the longitudinal direction will usually be up to 50 times higher than the one in the transverse direction, with the first being closer to the fiber's modulus, and the second to the matrix's modulus [1, 9].

The matrix has several functions, including holding the fibers together and transferring the loads to them. Even though the properties of a composite material are dependent on both the matrix and the reinforcement, some are mostly influenced by the matrix. One of which is the resistance to some environments, such as high temperatures and corrosion. It also has a

great influence into the material's ability to diffuse energy from an impact. In PMC, both the decomposition temperature, which is the temperature at which the polymer breaks apart, suffering degradation, and the glass transition temperature, which corresponds to the temperature where the material suffers a thermal transition, going from a solid state to a more pliable (but still solid) state, are two transition temperatures that should be considered. The two main types of PMC have either thermoset or thermoplastic matrix or resin. Thermosets are cross-linked polymer chains composed of monomers. At ambient temperature they are usually liquid, being formed through a mold, that is heated to cross-link the several molecules, during a process designated as curing. This process is irreversible, meaning that after its end, they can not be remelted. The most common example of these resins is epoxy, but there are some other examples such as polyurethane or polyesters. Thermoplastics are also composed of monomers, but without cross-linking. They are solid at room temperature, acquiring shape by being heated above their melt temperature. At this stage, they enter a liquid state, allowing them to be formed through (usually) a mold, and then left to cool and solidify. This forming process is reversible, which is an important consideration to have, since in high temperature environments the produced part can soften, exhibiting a plastic behavior. Examples include polyamide (PA), polyetheretherketone (PEEK) and polyethylene [1, 5, 6].

Regarding the fibers, their most important functions include to supply the composite material with high strength and stiffness and carrying most of the load. They are characterized for being larger along their length than in their width or thickness, usually having a round cross section. A very important aspect to consider about the fibers is their content in the composite material. Generally, the higher the fiber volume content, the higher the tensile strength, tensile modulus, flexural strength and lower the elongation. There is, however, a saturation point of around 70%, above which the matrix can not fully wet the fibers. Several types of fibers are used in the industry, with the selection mainly depending on the mechanical requirements, application and the budget. The most common are carbon, glass and aramid, but some other types exist, like natural (such as flax or spider silk) or boron [1, 5, 6].

Glass fibers are mainly based on silica, being an economically advantageous option when compared to carbon or aramid fibers. When used with polymeric resins, the produced parts are usually denominated as fiberglass reinforced plastics. These are the most commonly used fibers for structural applications, mostly due to their lower costs. Several types of these fibers exist, like C-glass, A-glass and S-glass, but the most common is E-glass. Carbon fibers are resultant of the demand for a replacement of glass fibers with higher stiffness and strength. These are usually based on polyacrylonitrile (PAN) and pitch, with some rarer examples of rayon, which are all materials with a high carbon content. The fibers are also classified by manufacturers considering their tensile modulus, with levels from low to ultra-high. PAN-based fibers have lower costs and are specifically relevant for standard modulus products. Pitch-based fibers present a high modulus coupled with good thermal conductivity. As for rayon fibers, they were the first to be produced, but are in much less usage now. Aramid fibers have linear polymer chains, presenting a tensile modulus and strength that is between glass and carbon fibers. Their most common application is in the form of Kevlar, for ballistic body armor, due to their high impact resistance, despite lacking compressive strength. They also present an unique failure mode through fiber breaking into small fibrils. Regarding Kevlar, three main types exist, being Kevlar 149 (ultra-high modulus), Kevlar 49 (high modulus) and Kevlar 29 (high toughness) [1, 5, 6].

Apart from fibers and matrix, some other components that are sometimes used in PMC. Fillers are one of these additives, that are usually used to reduce the costs, as they are cheaper than the resin's material. Pigments and dyes may also be used, either for aesthetic reasons or to alter light absorption properties. Some elements may also be added to specifically alter the viscosity of the resin, which is particularly important in some manufacturing processes, even though the fillers can sometimes fulfill this task. Another

possible added materials are surface treatment agents, that can, for example, prevent the foaming that some resins tend to present [5].

There are several manufacturing processes for composite materials. One of which is injection molding, where pellets composed of resin and chopped fibers are injected into a mold through a worm screw mechanism, after which it is left to cure and ejected. Due to the fibers having a randomized orientation, the mechanical properties of these composites are usually worse than when produced through other processes. Another process is resin transfer molding, in which a preform is placed onto the mold cavity, with the resin being injected into the mold. In some cases, this process is used with higher pressures, to ensure infiltration with higher volume fractions. The process can also be applied using vacuum, where the resin's introduction into the mold is assisted by vacuum. Hand-lay-up is probably the simplest manufacturing process, but also the most manual. In this, one side of the component is placed into the mold with a release agent, while the other half is left exposed, with a gel coat of resin being applied with a roller. This process is then repeated for every layer of fiber material, until the desired thickness is achieved. In fiber deposition, spools of either fibers or tape materials are automatically placed, being then cut to size and heated to cure. In filament winding, the fibers are used in a continuous form along with the resin in a liquid state. The fibers are wound around a mandrel, with one of several winding patterns. As for the resin application, the fibers can either be pushed through a resin bath before being wound, be pre-impregnated with resin or can be wound in a dry state, being impregnated later. It is used to produce rotationally symmetric parts. Compression molding employs an upper and lower half of the molds, with the molding compound being placed between the two halves. For thermoset materials, this molding compound usually takes the form of a sheet molding compound, where the material is a flat mixture of fibers and matrix, or bulk molding compound, where the material used has a shorter fiber length, being identical to the material used in the injection molding process. When the halves are closed, the curing process takes place, using both heat and pressure. As for pultrusion, the fibers are firstly pulled through a resin bath, drawing chamber or injection system to be impregnated with resin, and then through heated forming dies, being lastly cut to the desired length. It is a commonly used to produce beams of composite materials. In vacuum bag or autoclave molding, the composite laminate is placed into a flexible film bag, and the laminate is consolidated to ensure good adhesion between the matrix and fibers and to prevent voids. This could be done either through vacuum, in vacuum molding, or through heat and pressure, in autoclave molding. Liquid composite molding is a process that begins with cutting parts of a textile material, that are stacked. After this dry material is formed to the correct shape, it is infused with the resin. Thermoforming is a process that can only be applied to thermoplastic resins, where a part is formed by heating it inside the mold, after which it is left to cool inside of it [2, 3, 6].

2.2. Drilling Process

Even though FRP are produced in a near-net-shape state, they can require some finishing processes, applied through machining. This can be due to several reasons, like, for example, the need to meet dimensional tolerances. Several conventional machining processes can be applied, such as milling or conventional drilling. The interest in the drilling process is mainly related to joining the composite parts, usually achieved through bolts or rivets. In this process, a drilling tool is used, with a specific feed, usually measured in millimeters per revolution, and a cutting speed, measured in meters per minute, as it is visible in Figure 4a. The cutting speed is imposed by the spindle speed, measured in rotations per minute. During the drilling process, the main loads to be considered are the drilling torque and the thrust force, both measured along the direction of the feed [2, 11-15]. The drilling tool has a very specific and complex geometry, with the conventional twist drill being the most common

drilling tool, as it is presented in Figure 4b. It has three main parts: the shank, that is clamped to the drilling machine, the flutes and the point [16].

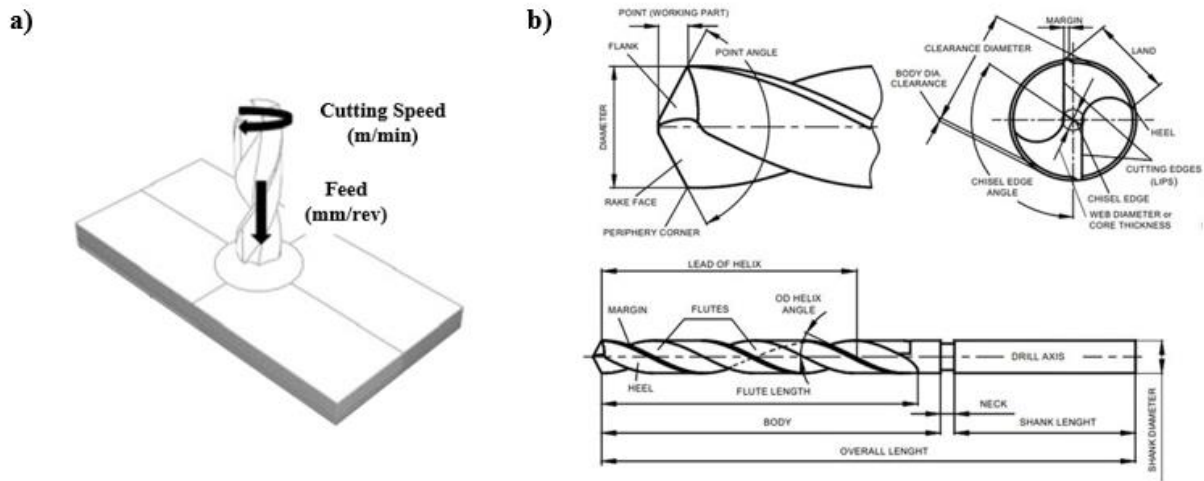


Figure 4 - Drilling process: (a) parameters (adapted from [17]); (b) drill bit schematic (adapted from [16]).

The drilling process in PMC is very complex, because they present an anisotropic behavior, they are not homogenous and the fibers are quite abrasive on the cutting tools. This may result in a high tool wear rate and high values of thrust force, which damages the workpiece material. The most common form of damage is delamination, but other types of defects also occur, such matrix cracks, thermal damage or burrs [2, 11-13].

Besides conventional drilling, other hole-making processes have been applied to PMC, even though their usage is not widespread. Some of these processes are still based on conventional drilling, like high speed drilling. In this process, a very high cutting speed is employed, to reduce both delamination and processing times. However, it presents several issues, mainly associated with high tool wear and high drilling temperatures. Gaitonde et al. [18] conducted a parametric analysis of the influence of the cutting parameters in the delamination when applying high speed drilling to CFRP and concluded that this process can greatly reduce the entrance delamination in a composite material, if the high speeds are used along with low feeds and drilling tools with lower point angles. Another process is vibration assisted drilling. In this, a low amplitude vibration is imposed on a drilling tool along the thrust direction, which leads to an intermittent contact between the drill and the workpiece, resulting in less heat generation due to less friction. This results in up to a 40% reduction in thrust force, and consequently less delamination, along with an extended tool life. In orbital drilling, the tool has three movements: a spindle rotation, feed and an orbital rotation, with the tool that is used being denominated end-mill. Because this tool's diameter is smaller than the drilled hole, both chip extraction and heat transfer are improved when compared to the conventional drilling process. Geier et al. [19] conducted an optimization of the cutting parameters for both conventional and orbital drilling processes, and concluded that orbital drilling was advantageous when considering the hole's quality, but was a slower process. Wobble milling is a hole-making process that begins with normal hole drilling, where the tool first enters the workpiece. Afterwards, the tool oscillates around the vertical axis, ending with the hole's finishing through a milling movement. Pereszalai et al. [20] compared wobble milling with both conventional and orbital drilling, and found that even though it was the slowest process, wobble milling was the superior process when the objective was to minimize the amount of uncut fibers [2, 11, 12, 21].

Some non-traditional machining processes can also be applied to hole making in PMC. These processes do not use conventional cutting tools, which eliminates the tool wear problems of the conventional processes. An example of them is abrasive water jet machining,

which removes the material through erosion. This eliminates all the issues related with heat generation, and greatly reduces delamination when compared with more conventional processes. Electro discharge machining has also been applied to both metal matrix composites and carbon fiber reinforced composites. In CFRP, this process has issues, because it creates a heat affected zone, which can vary in size depending on the parameters. Laser beam cutting in composite materials is more difficult to apply than in metals, due to the differing thermal conductivities of the several material constituents. It is, however, a very precise process, allowing for the drilling of holes with a diameter of 25 μm , and it eliminates delamination, as there is not contact between a cutting tool and a workpiece. However, this process creates a heat affected zone, with the span of it being dependent on the cutting parameters [11, 22-24].

2.3. Chip Removal Mechanisms

Even when considering drilling or milling processes, the orthogonal cutting process serves as representative material cutting scenario to analyze the chip formation process, due to its simplicity. Regarding PMC, the matrix nature will have a significant impact on the machining process, as thermoset matrices are more fragile and tend to create more discontinuous chips, while thermoplastics are more ductile, generally leading to more continuous chips. Thereby, the strains reached before fracture of thermoplastics are usually higher than in their thermoset counterparts [15]. The ductile nature of thermoplastics usually results in a better surface finish of the machined surfaces when compared with a thermoset material. It should be mentioned that the cutting speed, CER and rake angle have an influence on the obtained results when machining PMC [15, 25, 26].

The chip removal mechanism can be divided into five types, which vary depending on the fiber cutting angle (angle between the fiber orientation and the cutting orientation). Type I (Figure 5a) occurs at positive rake angles for a fiber cutting angle of $0/180^\circ$, and is characterized by Mode I fracture, as the tool's rake face delaminates the composite's fibers when advancing through the material. This causes peeling and buckling of the fibers. As for Type II (Figure 5b), it occurs for the same orientation as Type I, but with negative rake angles. In this case, a compressive load is applied in the direction of the fibers, causing them to buckle and fracture. As the tool advances, Mode II fracture occurs. Turki et al. [27] claim that the best surface quality is obtained within these conditions. When considering a fiber cutting angle between (but not including) 0 and 90° , Type III (Figure 5c and d) fracture is observed, regardless of the rake angle. This type is firstly dominated by compressive shear, followed by interlaminar shear. When the fiber cutting angle gets closer to 90° , interlaminar shear becomes more prominent. In this situation, the surface is more irregular than when the chip removal occurs through delamination or buckling, as in this case the cutting tool crushes the fibers. Type IV (Figure 5e) is observed when the fiber cutting angle is 90° . In this case, the chip removal occurs mainly through crushing of the fibers in their transverse direction. At Type V (Figure 5f), for a fiber orientation angle of 135° , chip removal is dominated by microfracture, where the cutting tool bends and then crushes the fibers [15, 25, 27-29]. Turki et al. [27] refers that the worst surface quality is obtained here, as it presents a stepped morphology.

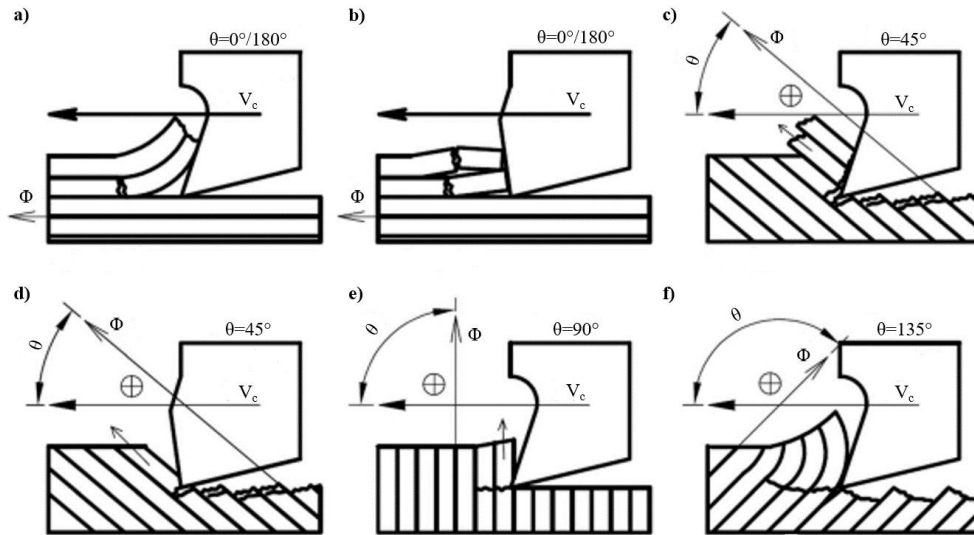


Figure 5 - Chip removal mechanisms for fiber-reinforced composite materials (adapted from [26]): (a) type I; (b) Type II; (c) Type III with negative rake angle; (d) Type III with positive rake angle; (e) Type IV; (f) Type V.

Even though the fiber cutting angle in the machining of PMC is the main influential factor towards the fiber cutting process, both the tool's rake angle and CER also have an impact. According to Ahmad [15], a higher rake angle tends to produce a better quality surface with less matrix smearing. A positive rake also limits the compressive-bending effect on the fibers, which is known to result in bad surface quality. Wang et al. [30] has presented that a small CER results in a smoother machined edge, as the fiber removal process becomes crushing dominated. Also, the authors refer that CER is particularly important when the fiber cutting angle is less than the tool's rake angle plus 90° , because in these situations, a high CER results in a bending dominated instead of crushing dominated process. In this region, the tool deforms the material, which returns to its initial shape after the tool has passed, as it is represented on Figure 6 [15, 25, 30].

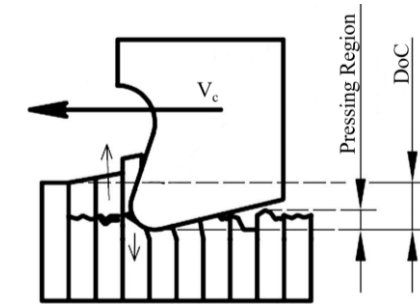


Figure 6 - Influence of CER, showing the pressing region and depth of cut (DoC) (adapted from [25]).

Due to the different chip removal modes, both the thrust force and torque tend to present a high degree of fluctuation, as the chip removal mode varies through the cutting process. This rationale applies for the drilling process, as it is represented through the scheme in Figure 7, where throughout the drill's movement, there are two cutting edges separated by around 180° at the same fiber orientation angle [15].

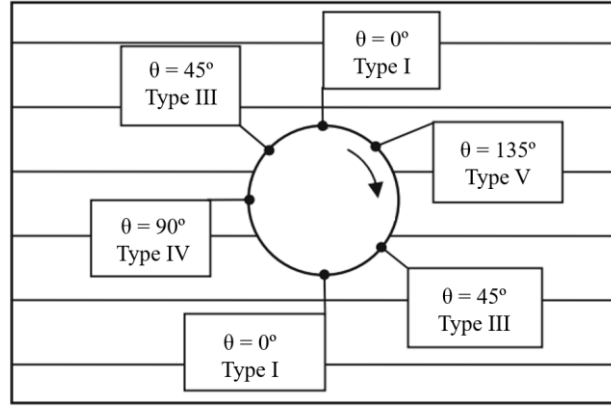
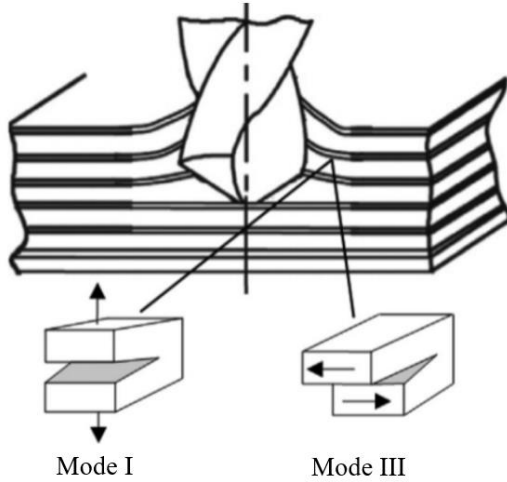


Figure 7 - Chip removal mechanisms for the drilling process in an unidirectional composite (adapted from [15, 26]).

2.4. Delamination

Delamination is the most relevant form of damage when considering PMC, where the plies get separated due to the cutting forces. It brings several issues to the produced component, like worsening the surface finish, and reducing the fatigue strength. This form of damage was previously observed to be the cause of composite part rejection in 60% of the times [31]. The repair of delamination can also take up to 6 hours, which further enhances the importance of avoiding it. There are two types of delamination when considering the drilling of PMC: peel-up delamination or entry delamination (Figure 8a), which occurs at the hole's entry, and push-out delamination or exit delamination ((Figure 8b), which occurs at hole exit [25, 31, 32].

a) Peel-up (entry) delamination



b) Push-out (exit) delamination

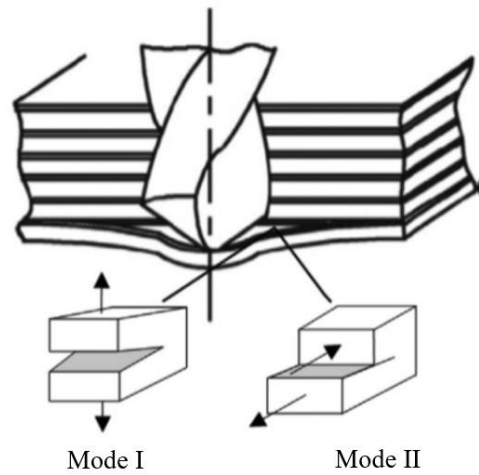


Figure 8 - Delamination schematic: (a) Peel-up (entry); (b) Push-out (exit) (adapted from [31]).

Delamination at the hole's entry is mainly due the drill lifting the fibers by the helical groove instead of cutting it, where it continues to exert an upwards force as the drill moves downwards, and the cutting edge is still unable to cut the fiber. The delamination propagation mode can then be considered as mode I, as the fibers are being pulled up instead of being cut, and mode III fracture, mainly due to the slope of the drill's flute exerting a peeling force. This delamination type is thereby mainly associated with the drilling torque, flute shape and helical angle [31-34]. Tang et al. [35] conducted a numerical investigation of peel-up delamination and concluded that the amount of delamination is associated with the fiber cutting angle. At 0°, fiber compression was found to be the main cause of delamination. When considering 45° and 90°, there was mostly delamination due to mode III fracture. With mixed mode I and III

fracture, 135° was the angle in which the delamination was worse, due to being the point at which the peeling force reaches its maximum. Feito et al. [36] studied the entry delamination for several drill geometries. The authors found that even though the cutting speed had a negligible effect in both the thrust force and drilling torque, it had a noticeable effect on the entry delamination. Regardless, in general, the feed was found to be more influential than the cutting speed.

Push-out delamination is usually caused by the lack of bottom support when drilling the last plies, which causes the bending of the last layers. This means that this kind of delamination occurs when the amount of applied stress is greater than the interlaminar bond strength. Thrust force is, by far, the most relevant aspect to consider, as it is the main cause for the pushing of the last plies. The fracture is then of mixed mode I and II. Usually, push-out delamination is more severe than peel-up delamination, due to the lack of support to compensate the deformation caused by the thrust force. The feed is the most relevant cutting parameter in terms of push-out delamination, with its effect being more prevalent than the cutting speed, because higher feed results in higher thrust forces. When there is an increase in the feed, the chip thickness is also increased, resulting in both higher thrust force and drilling torque. The drilling temperature also has an influence on push-out delamination, being that higher temperatures result in more damage to the workpiece [31-34, 37, 38]. Phapale et al. [39] conducted an experimental drilling analysis, accessing the delamination levels for several feed and cutting speed values. The authors found the feed to be the most influential parameter towards delamination damage, with higher feeds producing more damage. Regarding the cutting speed, they also found that an increase in this value also resulted in increased delamination, but its effect was not as pronounced. Feito et al. [36] conducted testing for several drill geometries, finding that, in general, the feed was the most influential parameter towards delamination. As for the cutting speed, the authors found that it had different effects for different tool geometries, with higher cutting speeds increasing the delamination factors in some cases but decreasing it in others. Melentiev et al. [40] also claimed that the feed has the strongest influence into delamination. Also, the author referred that the workpiece's material and tool geometry had an influence on delamination, with the influence of the cutting speed being dependent of both. Geng et al. [32] claimed that higher feeds always result in more delamination due to higher thrust forces, but the effect of cutting speed is more debated.

Some authors also focus on the way that the composite material is clamped to the drilling machine, referring that it may influence the thrust force, which in turn ends up affecting the delamination. The higher the distance between the point of drilling and the clamping position, the more prone is a certain workpiece to suffer damages such as delamination. This is because with an increase in the supported distance, the workpiece deflects more, and, at a certain critical distance, the drilling tool breaks through the workpiece instead of cutting it, leading it to climb through the drill's helical grooves, and resulting in severe delamination [41]. Ciecielag [42] verified that when drilling a CFRP, an increase in unsupported length resulted in higher peak thrust forces, leading to more severe delamination and burrs, along with less hole accuracy. Through a finite element method (FEM), Feito et al. [17] compared several different diameter rings around the hole clamping the plate, along with a situation without any clamping. The authors found an increase in thrust force as the clamping situation became less restrictive, along with a 20% increase in the delamination factor. Klotz et al. [41] studied the influence of the workpiece fixation through several clamping systems. Two of the setups used a four- and three-point clamping device, that are presented in Figure 9a and b, respectively, varying the distance between the supports for both situations. It was found that an increase in the distance between the supports led to higher thrust forces and consequently delamination factors. This evolution was not, however found to be linear, being that below a certain support distance value, the delamination increases were negligible, but above that critical value they became more severe. The authors concluded

that, depending on the workpiece's material, there was a critical support distance value above which a damage mechanism occurred. In further research, Klotz et al. [43] used the same four-point setup, and applied an empirical model which approximated the profile of deflection of the workpiece material during the drilling process. Due to the deflection of the workpiece, the feed per tooth was found to be variable during the drilling process given the differences in the clamping conditions. Based on this research, Bey-Temsamani [44] et al. proposed a new clamping system with three suction cups. These are fixed to the toolholder so that they move along with the drill, providing top support to the drilled part, as it is presented on Figure 10. After testing, the system allowed for a reduction in delamination through a severe reduction to deflection, enabling higher feeds and, thereby, improving productivity. Weckx et al. [45] developed a cloud-base digital twin for monitoring this system, which analyzed the quality of the drilled holes, monitored the drilling tool's wear and detected if the clamping device was operating correctly, warning the operator if any action was necessary. This system was claimed to both increase the production rates, as the operators do not need to spend time analyzing the drilled holes, and reduce the amount of scrap material, as a decrease of quality below the standards would not go unnoticed.

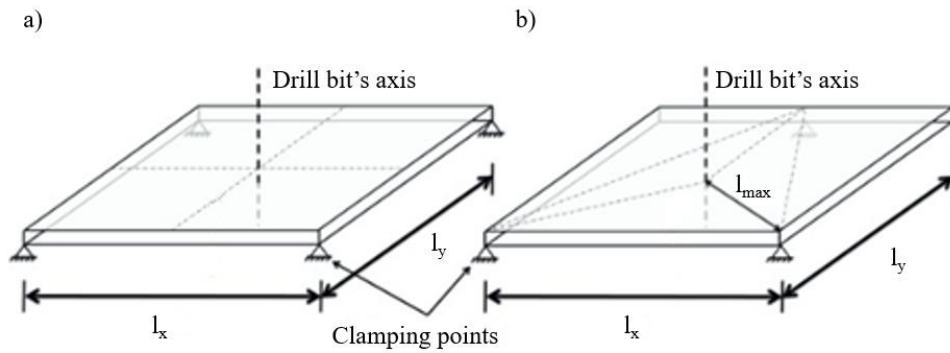


Figure 9 - Clamping devices: (a) four-point; (b) three-point (adapted from [41]).

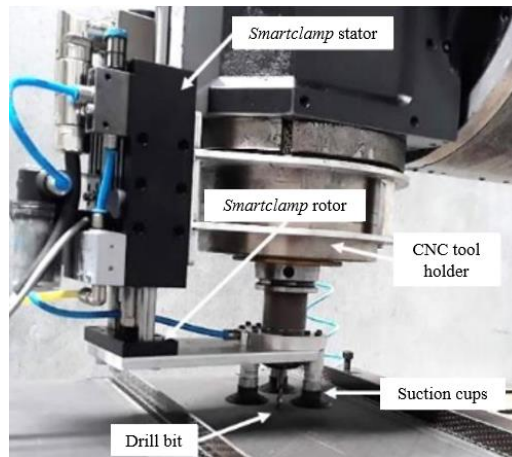


Figure 10 - *Smart clamp* system (adapted from [44]).

There are several ways to prevent and reduce the delamination damage. The selection of a proper drill geometry and adequate cutting parameters for that geometry and material is crucial in reducing delamination. Besides that, the application of techniques such as orbital drilling or high-speed drilling can reduce delamination, along with non-traditional machining processes like waterjet. The usage of backup support techniques, namely support plates as represented in Figure 11, is also beneficial. These plates support the workpiece material, so that it does not deflect under the thrust force imposed by the drilling tool. Its effect on

reducing damage has been confirmed by Feito et al. [46]. Besides support plates, there are also forms of active support where the force can be varied. The pilot hole strategy is another possible approach to reduce delamination. In this technique, a smaller diameter than nominal hole is firstly drilled, after which it is enlarged by another drilling tool. The main advantage of this procedure is that, in the second part of the drilling process, the contribution of the chisel edge in the thrust force is eliminated. Thereby, the diameter of the pilot hole should be correspondent to the chisel edge of the second drill [32, 33, 38, 47]. Wang et al. [48] confirmed that by using a pilot hole strategy there was a reduction of around 55% in the thrust force when compared with a conventional drilling procedure.

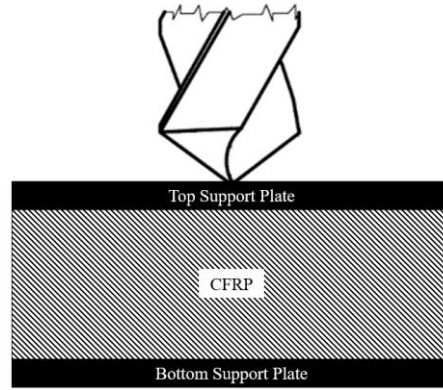


Figure 11 - Top and bottom support plates (using adapted drilling tool image from [25]).

The amount of delamination can be quantified through the delamination factor. These factors can either be one-dimensional, two-dimensional or three-dimensional, depending on the dimension used for evaluation. Some of these factors are presented on Table 1. They are defined based on several diameters and areas, like it is presented on Figure 12, where D_{nom} is the nominal hole diameter, and D_{min} and D_{max} are the minimum and maximum diameters of the delamination area, respectively. As for the areas, A_{nom} is the nominal hole area, A_{del} is the delamination area, and A_{max} is the area corresponding to D_{max} . All the presented criteria have shortcomings when accessing delamination. For example, both F_d and F_{dmin} are only accurate when the delamination is uniformly distributed around the hole. Regarding F_a , it fails to distinguish a hole with a regular delamination pattern around it, to another with a smaller delaminated area but with large cracks. Even though they both can result in the same F_a value, the one with the cracks presents a more serious issue in terms of damage, usually dependent on their length. This issue also occurs for both F_{ed} and F_v , where the latter is distinct, being a three-dimensional factor, that thereby also considers interlaminar damage. As for F_{da} it considers the maximum crack length, solving the issue of the previously mentioned factors, but it presents inaccuracies when evaluating holes with multiple fine cracks [31, 33].

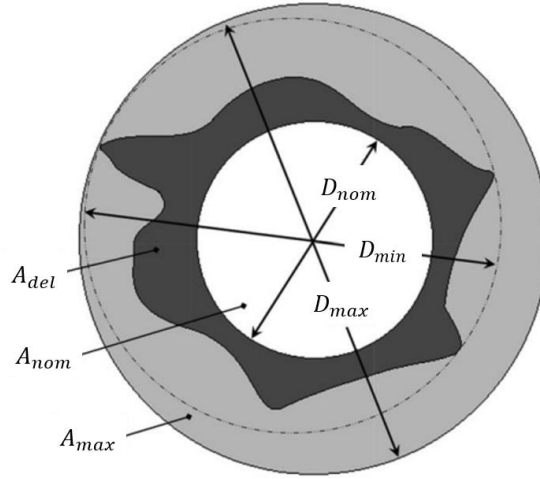


Figure 12 - Delamination factor calculation parameters (adapted from [31]).

Table 1 - Delamination factors [31, 33].

Delamination factor	Expression
F_d (conventional delamination factor)	$F_d = \frac{D_{max}}{D_{nom}}$
F_{dmin} (minimum delamination factor)	$F_{dmin} = \frac{D_{min}}{D_{nom}}$
F_a (two-dimensional delamination factor)	$F_a = \frac{A_{del}}{A_{nom}}$
F_{da} (adjusted delamination factor)	$F_{da} = F_d + \frac{A_{del}}{A_{max} - A_{nom}} (F_d^2 - F_d)$
F_{ed} (equivalent delamination factor)	$F_{ed} = \frac{1}{D_{nom}} \sqrt{\frac{4(A_{del} + A_{nom})}{\pi}}$
F_v (three-dimensional delamination factor)	$F_v = \frac{1}{p} \sum_{k=1}^p \frac{A_{del}^k}{A_{nom}}$

Nondestructive testing is of the utmost importance to characterize delamination damage, regarding its size, shape and location. The kind of technologies employed for these usually involve image acquisition and processing. There are several image acquisition techniques that have been applied to composite material's analysis. Visual measurement techniques, like is the case for microscopy, scanning electron microscopes (SEM) or digital scanning have been used to access delamination, but present limitations due accessing it at a superficial extent. Because of this, X-ray radiography and computerized tomography are more common. These techniques can, however, be quite costly and time consuming. Regarding image processing, it is a vital step, as it affects image quality, and consequently the accuracy of the measurements [31, 33]. Another delamination assessment technique was developed by Abdullah et al. [49], where a two-dimensional profile acquisition is applied through software, with the results showing it to be both faster and more accurate than conventional methods.

The critical thrust force corresponds to the thrust force value above which push-out delamination is observed. The models that allow the calculation of this value are usually based on linear elastic fracture mechanics and classic laminate plate theory. The first model of critical thrust force in twist drills was developed by Ho-cheng and Dharan [50], and considered mode I fracture, the material as isotropic, the thrust force to be concentrated in a single point, and the delaminated area having a circular shape. Due to its simplifications, this model tends to be quite conservative, and so, others were developed, with several different aspects improved. Firstly, all the of the following models started to consider the material as orthotropic, such as the one developed by Lachaud et al. [51], which also considered the thrust force's load to be concentrated at the center of the hole. Both Jain and Yang [52, 53] and Zhang et al. [54] differentiated by their models by considering delaminated area to be elliptical instead of circular, which is more realistic when considering an unidirectional composite material. Regarding the load distribution, instead of considering its application on a single point, Piquet et al. [55] defined uniformly distributed pressure, and Rahmé et al. [56] divided the stress in two regions, considering half of it to be a point load at the drill's web, and the other half to be uniformly distributed at the cutting edges. Girot et al. [57] assumed delamination to occur in mixed-mode I and II. Ojo et al. [58], besides considering the delamination to occur in mixed-mode I and II, also based their model on first-order shear deformation theory instead of classical laminate plate theory, assuming the delaminated area to have an elliptical shape. Massoom and Kishawy [59] presented a model that also considered mixed-mode delamination and the delaminated area to be ellipsoid shaped, but took into account thermo-mechanical loads. Both Girot et al. [57] and Massom and Kishawy [59] compared some of the already presented models with experimental results, as it can be observed on Figure 13a and b. From the two figures, it is possible to conclude that the models which consider mixed-mode I and II delamination are the ones that provide the most accurate results, which shows that pure mode I fracture does not fully represent the delamination mechanism when drilling composite laminates. However, considering mixed-mode I and II delamination requires the determination of both mode I and II critical fracture energy, as it will be explored in further detail later, while considering pure mode I delamination only requires the determination of the critical fracture energy for mode I fracture, thereby requiring less testing. As for the models that consider pure mode I fracture, Piquet et al. [55]'s model seems to be the most accurate. It should be however mentioned that the accuracy of the models is dependent on the methodology used to compare them, as different cutting parameters and materials will produce different results. Also, it is important to highlight that all these models were developed for conventional twist drills. Models for other drilling tool's geometries do exist and are based on the same rationale as the ones presented, though they are not as common. All the equations for each model are present in their respective references [32, 57].

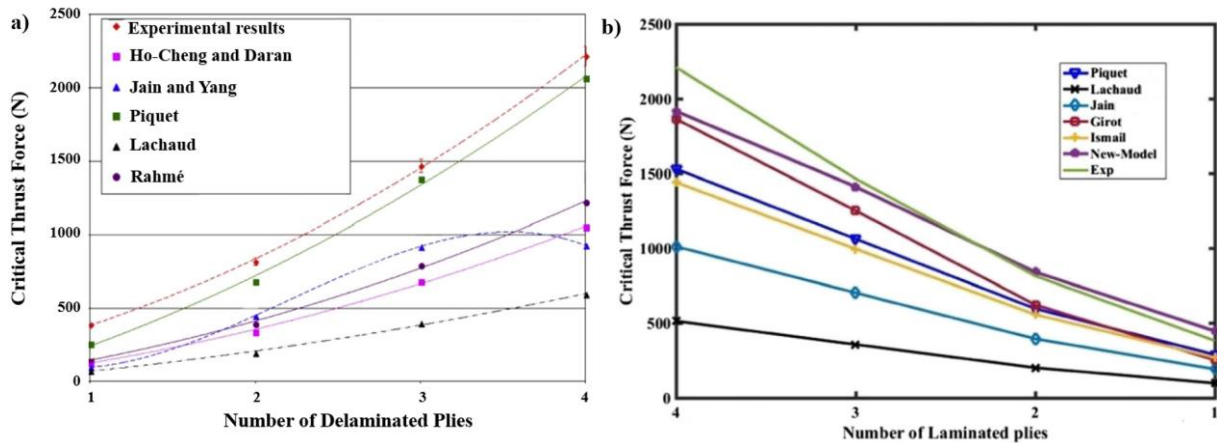


Figure 13 - Comparison of several critical thrust force models: (a) Girot et al.'s (adapted from [57]); (b) Massom and Kishawy's (adapted from [59]).

2.4.1. Fracture Modes

In order to calculate the critical thrust force, it is necessary to determine the material's fracture energy (respective to its delamination) through experimental testing, based on the fracture modes. These modes correlate to the type of relative displacement that fractured surfaces go through upon loading. There are three fracture modes, which can either occur alone or combined, depending on the region of the crack that is being analyzed. Mode I, presented in Figure 14a, occurs when the crack is opening orthogonally to the fracture plane. As for Mode II, presented in Figure 14b, the fracture surfaces slide upon each other in opposite directions. Regarding Mode III, presented in Figure 14c, the fracture planes suffer lateral displacement, but with a tearing instead of sliding loading [60, 61].

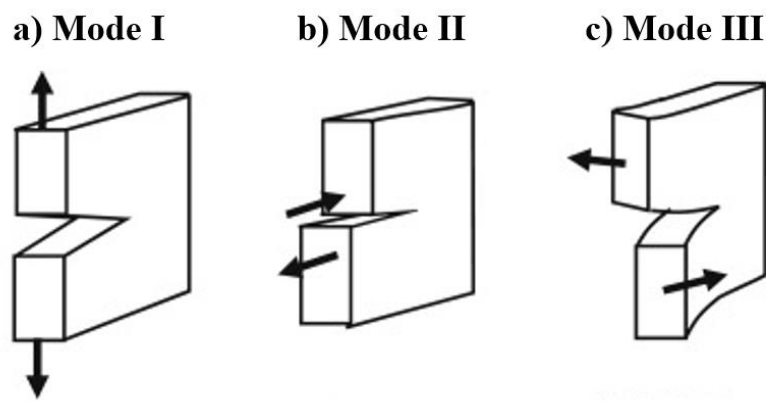


Figure 14 - Fracture modes: (a) Mode I; (b) Mode II; (c) Mode III (adapted from [60]).

By definition, the material's fracture energy or critical energy release rate, is the necessary energy value at the crack tip necessary for its propagation, under a specific loading condition. For pure mode I fracture testing, one of the most common tests is the double cantilever beam (DCB) test, in which two specimens with the same length (L) and thickness (h) are loaded at their tip (with a load value of P), with an initial region free of adhesive (a_0) that is denominated the pre-crack, as it is represented on Figure 15a. The compact tension (CT) test is another possibility. This test is standardized for fiber-reinforced composite materials in ASTM D5045, with the respective specimen's geometry presented in Figure 15b. Almost all the dimensions are dependent on a fixed W value, including the pre-crack length. Regarding pure mode II fracture, the most common test is end-notched flexure (ENF), mainly due to its simplicity. In this test, a load is applied in the middle of the specimens, which are simply supported at their extremities, and are bonded together throughout their length except in the pre-crack region, inducing shear loading at the crack extremity. As for mixed mode I and II bending, there are a lot of possible tests. One of them is single leg bending (SLB), which is based on ENF, but uses a shorter beam, with the loading in the middle. This test has a slight dominance of mode I. It also has an asymmetric version, called asymmetric single leg bending, where there is also a predominance of mode I bending, but the bottom specimen does not have the same thickness as the top one. The DCB also has an asymmetric version that tests mixed mode bending the mode I predominance, where the only difference is the thickness of the top and bottom specimens. Mixed-mode bending (MMB) is a combination a pure mode I (DCB) and pure mode II (ENF) test. It takes the ENF test setup, adding an additional loading, and the ratio of the applied loadings shifts the mode dominance towards mode I or II [62-64].

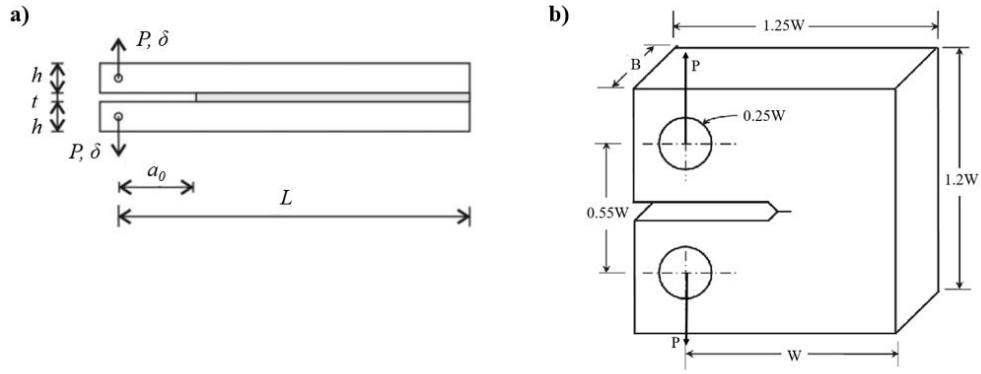


Figure 15 - Fracture testing procedures schematics: (a) DCB testing (adapted from [62]); (b) CT testing (adapted from [65]).

2.5. Burr

Burrs can be classified as macro-geometrical defects caused by the machining process, often appearing as excess material. This kind of defect is observed whenever the cutting tool can not remove all the material up towards the nominal machining depth. Burrs are problematic in these materials, as they can hinder the assembly process, often even requiring deburring operations which are costly and time consuming. Also, they can compromise the surface's integrity. Research has been made towards the influence of several aspects of the machining process in burr formation, as well as ways to reduce and eliminate burr [30, 38, 66].

Wang et al. [30] and Xu et al. [67] found that burr formation depends on the fiber cutting angle, as it is defined through Figure 16. The authors found that 135° was the fiber cutting angle around which burr tended to occur, which was justified by the fact that at these angles, the machining process is bending-dominated, and as such, the fibers deform instead of being cut. Also, it was found that when drilling an unidirectional composite material, the burrs' location tended to be symmetrical and coincident with the 135° cutting angle. Similarly to delamination, the lack of support when machining the last layers also facilitates the appearance of burrs. Due to their deformation, the cutting tool does not make contact, and these are left uncut. Wang et al. [30] found that the tool's CER also has an effect on burr. Larger CER worsen the amount of burrs, because fiber fracture becomes bending dominated, which tends to result in burr. Regarding the cutting parameters, it has been found that higher feeds lead to more burrs. This is because, as Xu et al. [68] and Xu et al. [67] pointed out, higher feeds tend to lead to higher thrust forces, which lead to higher deformation of the last plies of the composite and thereby to more burrs. Higher cutting speeds also seem to promote burr damage, given that higher temperatures reached tends to soften the materials, making the fiber-cutting process more challenging. Lee et al. [69] found that the burrs tended to worsen with drill wear. This can most likely be attributed to the increase in the CER as the drill gets worn [38, 66, 70].

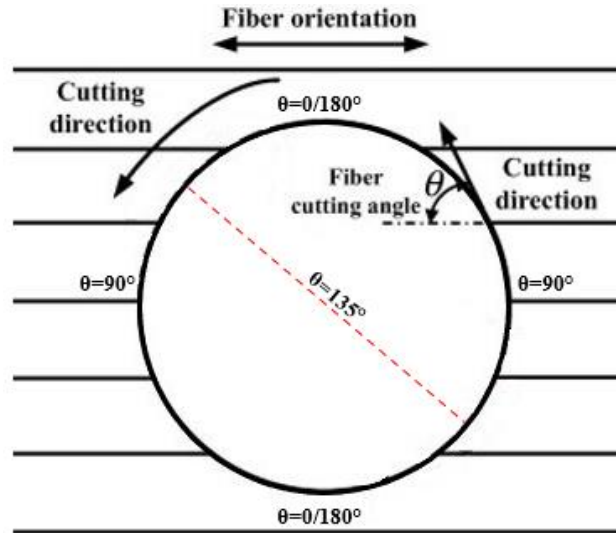


Figure 16 - Relation between burr formation and fiber cutting angle (adapted from [67]).

Cryogenic cooling has been shown to reduce the amount of burrs and uncut fibers [71]. This is related to the stiffness increase of the composite with the cooling conditions, which results in fewer burrs. Poór et al. [66] claim that the usage of back-up support has also been found to reduce burr formation by preventing the last plies from buckling, which has been verified by John and Kumaran [47], who also studied the influence of tool geometry on burr formation.

2.6.Hole Surface Quality

The most common ways to measure the hole's surface quality is through surface roughness parameters like arithmetic average roughness (Ra) and average maximum height of the profile (Rz). Gao et al. [72] studied the influence of several parameters on the hole's surface roughness through FEM. The authors concluded that the fiber cutting angle was the most influential parameter towards the hole's surface roughness, with 135° being the value at which worse surface quality was obtained, and 45° the best. Beyond that, the feed was the most relevant, followed by the cutting speed, with the lowest Ra value being obtained at high cutting speeds and low feeds. Shunmugesh and Panneerselvam [73] confirmed that the feed was the most relevant parameter, followed by the cutting speed. These authors also compared different tool materials, but found it to have less influence into the Ra values than the cutting parameters. Shahabaz et al. [74] claim that the reduction in Ra at higher speeds is due to less formation of the built-up-edge observed at lower speeds, while the increase of the Ra value with the feed is due to higher strain rates obtained at higher feeds and the thicker chips. Teicher et al. [75] claim that the Rz values have higher sensitivity to severe surface damages, like fiber pull-out or delamination, making it a more suitable parameter than Ra to access the surface's damage. Geier and Pereszalai [76] claim that the ratio Rz/Ra can be beneficial in the analysis of a machined surfaces, as higher values are most likely associated with more uncut fibers, fiber pull-out and delamination.

2.7.Drilling Tool Geometry and Materials

Several drilling tool geometrical parameters can significantly impact the quality of the drilled holes, such as the point angle, helix angle, CER, chisel edge, rake angle, clearance angle, among others. A considerable amount of research has been made regarding this matter, testing different tool geometries and optimum parameters regarding each tool, from which several requirements were extracted.

Firstly, the drilling tool should always minimize delamination. As it has already been discussed, the thrust force is the main cause of delamination, occurring whenever the critical thrust force level is surpassed. Regarding the geometry of the cutting tool, lower thrust force values can be achieved by decreasing the point angle, as it has been found that a lower point angle reduces thrust force with the drilling torque remaining constant [25, 36]. Tejas and Cadambi [77] tested drill geometries with point angles of 90°, 118° and 135°, and found that the drill with 118° resulted in the least delamination. Feito et al. [78] tested tools with 90°, 118° and 140°, and found that point angles comprised between 90° and 108° to be the range that reduced delamination, also recommending that the cutting speed should be increased as the drill gets more worn out. However, as Geier et al. [79] pointed out, the point angle should not be excessively reduced, or otherwise it can result in long tools, that negatively impact the machining cycle time, and, thus, productivity. Regarding the helix angle, Geier et al. [25] and Geier et al. [79] claim that a lower value tends to minimize burrs. As for its impacts on delamination, there are distinct results. Shyha et al. [80] used drills with helix angles of 24° and 30°, finding that this variation in the helix angle did not have any significant impact into delamination and hole wall damage. However, Suthar et al. [81] compared a 22°, 27° and 35° helix angle in terms of delamination damage, finding that the higher helix angle minimized the amount of delamination. Regardless, both authors agreed that the feed was the most influential parameter towards delamination.

The cutting edge should be sharp to result in lower thrust forces and reduce the occurrence of burrs and uncut fibers. This can be achieved either by using uncoated drills, that have the inconvenience of becoming worn out easily, or to extend tool life, by applying thin coatings. The smaller CER also reduces the heat produced through friction during the drilling process, that should be avoided, as it can cause thermal damages. In this regard, it is also beneficial to use diamond coatings, as they usually have a lower coefficient of friction. Besides, drills with a high clearance angle also reduce the amount of heat produced, because after the workpiece endures intense plastic deformation promoted by the cutting tool, it recovers elastically and contacts the clearance face of the tool, causing friction [25, 79, 82].

As for the drill tool's material, high hardness and toughness are required. As such, there are three options that can be considered: high speed steel (HSS), cemented carbide or ceramic/superhard materials [15]. Regarding HSS, it has been claimed not to be a very suitable material, like by Tejas et al. [77]. At high temperature, HSS does not retain its hardness, making it very susceptible to wear from the fibers in CFRP materials, which are very abrasive [15].

Cemented carbide tools are produced through compressing a mixture of carbide powders, which are usually tungsten carbides (WC) but can also be others like titanium (TiC) or tantalum (TaC), with a binder, which is usually cobalt (Co). These materials maintain their properties at high temperatures, and their technology is sufficiently developed to allow their mass production at a relatively low cost. The most common of these materials is WC-Co. The main limitation of these materials is their low wear resistance when drilling abrasive materials, as is the case of CFRP, but their performance can be enhanced using coatings. The application of these coatings is achieved through the CVD or physical vapor deposition (PVD) processes. PVD involves lower temperatures than CVD, allowing it to produce coatings with finer microstructures and higher toughness. However, CVD coatings have superior wear resistance. The used coatings can be ceramic, characterized by their high hardness and thermal stability, which include TiC, titanium nitride (TiN), titanium carbonitride (TiCN), along with others. Through CVD, it is also possible to apply thin diamond coatings, with a thickness of around 6 to 20 μm , that provide excellent toughness and hardness, but present problems related to adhesion. In these cases, the coatings can be NCD or MCD. MCD coatings have a crystal size that is usually around 30 μm , while in NCD is 100 nm. These differences result in the MCD cutting having higher surface roughness and friction

coefficients, but also higher hardness and better adhesion. Sometimes, various layers of NCD and MCD are used simultaneously, to take advantage of both structures [15, 83, 84]. Ameer et al. [85] compared an HSS tool, an WC cemented carbide tool and a WC-TiN coated tool in terms of drilling thrust force, torque, delamination factor and cylindricity error. The authors found the WC-TiN tool to be the optimal to obtain the least cylindricity error and delamination, with the HSS drill producing the highest thrust forces. Zhang et al. [83] tested WC-Co cemented carbide drills with 6% of cobalt, with MCD, NCD and mixed NCD and MCD coatings (inner layer of MCD and outer layer of NCD), all with around 6 μm of thickness, applied through the Hot Filament CVD (HFCVD) process. The mixed coating layer achieved the lowest thrust forces, with better adhesion and wear resistance than the single coating layers.

As for ceramic materials they are usually based on alumina (Al_2O_3), mixed with another ceramic such as TiC or zirconia (ZrO_2). Despite their excellent thermal stability, their lower toughness makes them prone to chipping. Silicon based ceramics are also used due to their good adhesion to diamond coating applied through CVD. As for superhard materials there are polycrystalline diamond (PCD) tools, which are produced at high temperatures and pressures by being mixed with a binder, such as cobalt. Afterwards, they are cut to the desired shape and brazed to the substrate. Even though these tools provide excellent resistance to wear, their usage is limited due to high costs [15, 84]. Uhlmann et al. [84] tested different substrates with diamond coatings applied through the HFCVD process. Thereby, the authors considered three different WC-Co drills, with Co contents of 5%, 6% and 10%, as well as two different ceramic silicon substrates, with a multi-layer coating of MCD and NCD with a thickness of around 12 μm . The authors found the silicon-based substrates to withstand higher thrust forces, due to enhanced adhesion, and thus wear resistance. In terms of the WC-Co substrates, the lower percentages of cobalt offered both better wear resistance and hole quality.

Several geometries for the tools have been tested through research, with some of the most common being conventional twist drills, double-point angle twist drills and step drills. Conventional twist drills (Figure 17) are analyzed by a lot of researchers, often serving as comparison with other geometries. Due to having a relatively large point angle and chisel edge, the chip removal mechanism is prone to bending, leading to higher thrust forces and resulting on delamination and uncut fibers [25]. Feito et al. [36] conducted a multi-objective optimization of the cutting parameters for this geometry, and found that for conventional twist drills, the feed is the most influential parameter for both the thrust force and exit delamination, but in terms of entry delamination the cutting speed is more significant.



Figure 17 - Conventional twist drill (adapted from [25]).

Double-point angle twist drills lead to lower thrust forces than conventional twist drills, due to their smaller second point angle, resulting in less delamination. These drills also do not suffer as much tool wear as conventional twist drills, due to reduced cutting force acting on the secondary cutting edge [25]. Al-wandi et al. [86] compared a conventional twist drill to a double-point angle twist drill through experimental and FEM. The authors found that

the double-point angle drill resulted in less delamination, which was attributed to it possessing a higher critical thrust force value. A schematic drawing of this kind of drills is presented on Figure 18.



Figure 18 - Double-point angle twist drill (adapted from [25]).

The step drill can be divided into a smaller diameter part (core), with a diameter of D_{core} presented Figure 19a, and another with a larger diameter part (step), with a diameter of D_{step} presented in Figure 19a, separated by the step angle θ_{step} in Figure 19a. When drilling, the core part produces a smaller hole with delamination and uncut fibers. The area affected by delamination decreases when considering smaller core diameters, and it is intended that this area is smaller than the one drilled by the step part of the drill, so that this part removes the delamination, reaming the final surface. It is important that, during the step part drilling process, the thrust force does not go above a critical level, or otherwise it can produce additional delamination. When designing a step drill, finding the optimal core diameter is paramount, because if it is too large, it will produce more delamination than the step part can remove, and if it is too reduced, it can result in the step part producing delamination. The step angle and the ratio between the core and step diameters are also important parameters [25, 87]. Kwon et al. [87] optimized the step drill's geometric parameters values to minimize both delamination and uncut fibers. The authors found lower core diameter values were the ones that reduced both delamination and uncut fibers for the same cutting conditions. As for the step angle, the authors found 40° to be the optimum value, with values below 40° presenting severe friction, inducing significant delamination, and the values above 40° greatly increasing thrust force values. Feito et al. [36] observations confirmed that the feed had a greater influence on exit delamination than the cutting speed and a far greater influence in the thrust force. The authors also found that lower values of cutting speed reduce both entry and exit delamination. Figure 19b shows a schematic drawing of a step drill.

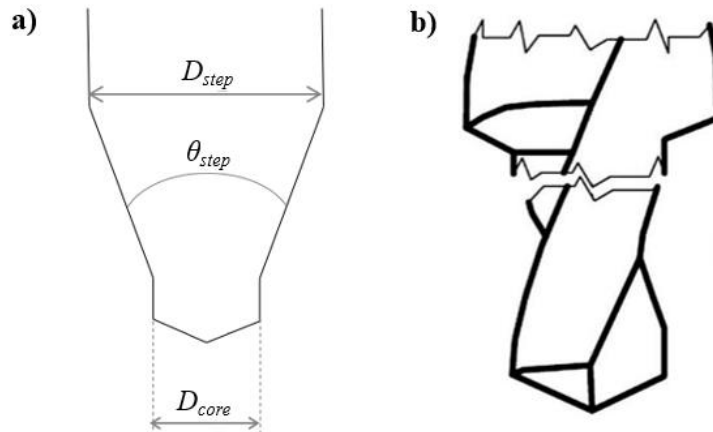


Figure 19 - Step drill: (a) configuration with main dimensions(adapted from [87]); (b) shcematic drawing (adapted from [25]).

Some authors have proposed alternative geometries to the more conventional. Yu et al. [14] proposed a drilling tool with a helical groove edge, presented on Figure 20. This drill was

based on a double-point angle drill, in order to reduce thrust forces and consequently delamination. The helical groove's upper edge removed the peel-up burr, while the lower edge removed exit burr both during the drilling process and when the tool is exiting the hole. Comparing it to a conventional twist drill, it was found that this design greatly reduced both exit burrs and thrust forces, which resulted in less delamination. The hole's surface finish was also greatly improved.

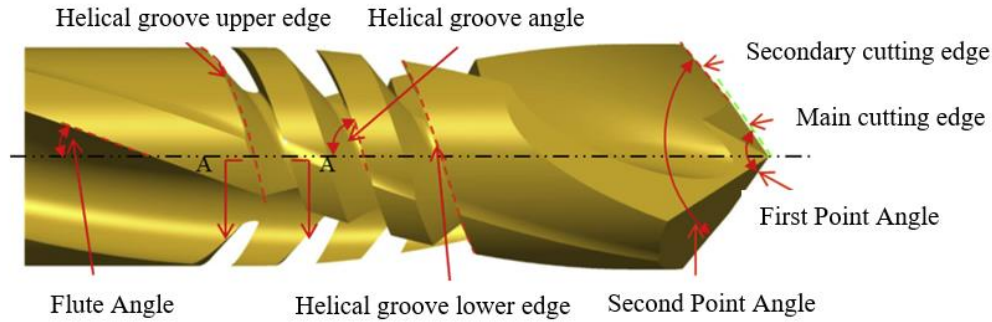


Figure 20 - Drill with helical groove edge (adapted from [14]).

Sugita et al. [88] proposed a drill design to reduce both burr and delamination consisting on web thinning, which is a method that allows a positive rake angle at the chisel edge, reducing the cutting forces, with a web shape that was shown to both reduce the cutting forces and improve chip evacuation. This web shape creates a new cutting edge, denominated the thinning edge, as it can be seen in Figure 21a [89]. Apart from web thinning, the proposed tool, presented in Figure 21b, also had a point angle of 130° , a helix angle of 18° and chip breaking grooves along the side edge, to promote chip fragmentation and cooling. To improve stability, the land was widened and the two margins on the land reduced vibrations, burr and delamination. The tool was manufactured in cemented carbide with a diamond coating. For its testing, a total of 4600 holes were drilled, where the maximum burr height was, at most, $80\text{ }\mu\text{m}$ with no delamination, and the maximum accuracy error was $15\text{ }\mu\text{m}$.

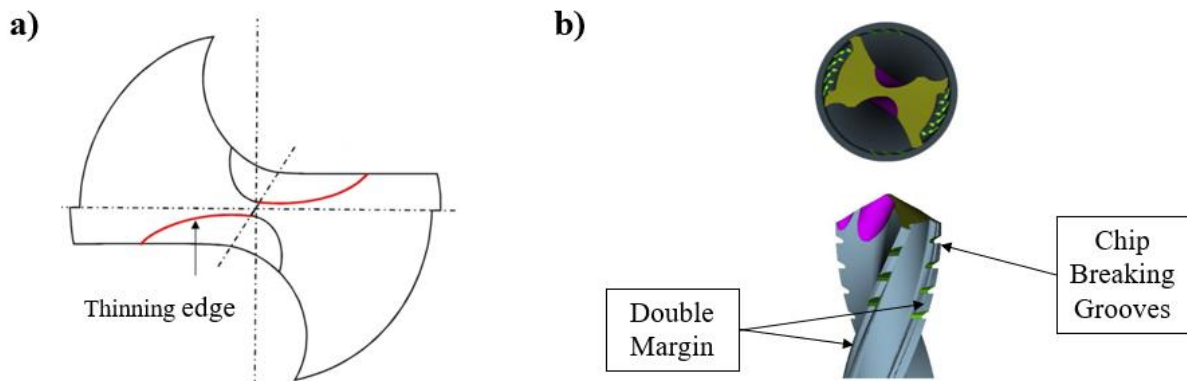


Figure 21 - Geometrical features of proposed drill bit: (a) thinning edge (adapted from [89]); (b) design features for reduction of burr and delamination (adapted from [88]).

The usage of cooling through fluids when drilling CFRP materials is not common, since these fluids can get mixed with the carbon chips and damage the drilling machine [25]. Besides, as Turner et al. [90] found, the exposure of composite materials to machining coolants can lead to the CFRP absorbing moisture and to degradation of its mechanical properties. In their work, the authors found that water caused more damage than other machining coolants. Nagaraj et al. [91] studied the drilling of CFRP at dry, cryogenic and minimum quantity lubrication (MQL) environments. The authors found that both cryogenic and MQL increased the drilling thrust force, resulting in more severe delamination. In terms

of hole diameter and roundness, both MQL and cryogenic cooling proved to be advantageous in this regard. Under cryogenic cooling, this was thought to be related to cutting temperature reduction and thereby improving the tool's life. The results obtained by Batista et al. [71] were in agreement, stating that the cryogenic cooling increased the material's stiffness, resulting in higher cutting loads for both thermoset and thermoplastic matrix materials. The authors also found an increase in hole quality for both materials, with a reduction in the amount of uncut fibers.

Tool wear is defined by Ahmad [15] as the modification of the tool's geometry, resulting in a loss in effectiveness and performance from the cutting tool. The wear can be usually divided into three stages, as it is presented in Figure 22. In stage I, there is a quick wear increase, as the weaker spots in the sharper edges wear out. Afterwards, in stage II, there is a steady increase in wear with constant tool material removed. In stage III, the wear increases quite rapidly, as the tool suffers catastrophic damage. Examples of tool wear on conventional twist drills are presented in Figure 23.

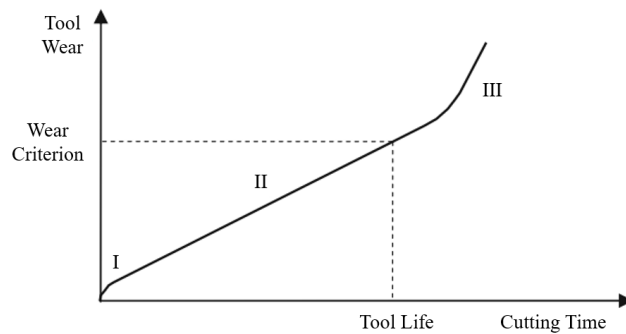


Figure 22 - Usual tool wear evolution (adapted from [15]).

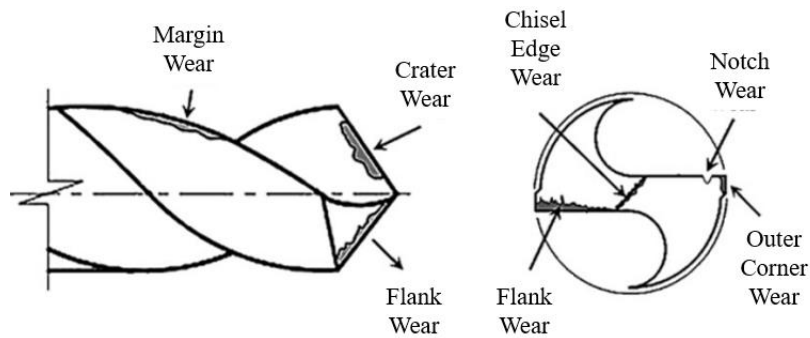


Figure 23 - Possible wear types in conventional twist drills (adapted from [92]).

Fernández-Pérez et al. [93] conducted an analysis on the wear of diamond coated cemented carbide drills. The authors found that the adherence of the coating to the substrate is crucial in delaying the tool wear, as coating delamination is the main cause for critical tool failure. Tool wear increased with both thrust force and torque, which seemed to be related with flank wear. The increased thrust force caused more delamination damage. Both low feeds and cutting speeds were found to reduce tool wear. Ji-Yong and Dong-Gil [94] compared cemented carbide drills with different particle sizes to access the optimum substrate for gradient-NCD (MCD on the substrate interface and NCD on the outer surface) and MCD coated drills. The 2.3 μm particle sized drilling tool was found to be the optimal substrate, as it presented less edge wear than its counterparts with smaller particle sizes, and a sharper cutting edge than its larger particle counterparts. Due to its higher wear resistance and lower coefficient of friction, the diamond coated tools were found to have a tool life four times greater than the uncoated drills, with the gradient-NCD drill producing superior quality holes

in terms of accuracy and delamination, but being less wear resistant than the MCD drills, mainly due to the better adhesion of the latter coating. Hrechuk et al. [95] compared both uncoated and diamond coated cemented carbide to a PCD drill. The PCD drill exhibited superior performance in terms of tool life and hole quality, combined with lower thrust force and torque values. The wear in the PCD drill was found to be dominated through a decrease of the CER, while the cemented carbide drills presented significant flank wear.

2.8. Numerical Simulation of Drilling Operations

When applying the drilling process to PMC, several challenges arise when compared to metallic materials. Even though most research is conducted through experimental means, the increasing CPU processing capabilities enhanced the usage of FEM software, like ABAQUS in PMC drilling analysis. Simulation processes are usually cheaper, not requiring some of the destructive means that conventional research employs. The required numerical modelling for predicting drilling induced damages is, however, a complex process, due to the large number of variables that are involved [13, 96, 97].

The models used for finite element approaches can either have a macro-mechanical, micro-mechanical or a multi-scale approach. Macro-mechanical models consider the material as a whole and its global mechanical response, while in micro-mechanical models, both the fibers and matrix are modeled separately. For drilling models, the micro-mechanical approach is less common, due to the complexity behind the contact definition and establishment of boundary conditions. Macro-mechanical three-dimensional analysis is thereby usually preferred [96].

The accuracy of a finite element simulation relies heavily on the accurate definition of material properties. In a FRP, the failure can have two different types: intra-laminar or inter-laminar [13, 96].

Intra-laminar damage results of fiber and matrix damage, and there are several criteria to describe it. Hashin's criterion [98] is, of all the available, the most popular. It considers four different modes of failure in composite materials: tensile fiber mode (when $\sigma_{11} > 0$, equation 2.1), fiber compressive mode ($\sigma_{11} < 0$, equation 2.2), matrix tensile mode ($\sigma_{22} + \sigma_{33} > 0$, equation 2.3) and matrix compressive mode ($\sigma_{22} + \sigma_{33} < 0$, equation 2.4), where the various σ_{ij} represent the stress components, σ_1^{tu} and σ_2^{tu} the ultimate tensile strengths in the longitudinal and transverse directions, σ_1^{cu} and σ_2^{cu} the ultimate compressive strengths in the longitudinal and transverse directions, and τ_{12}^u and τ_{23}^u the ultimate shear strength in the 1-2 and 2-3 planes [13, 99].

$$d_{ft}^2 = \left(\frac{\sigma_{11}}{\sigma_1^{tu}} \right)^2 + \frac{\sigma_{12}^2 + \sigma_{13}^2}{\tau_{12}^u} = 1 \quad (2.1)$$

$$d_{fc}^2 = \left(\frac{\sigma_{11}}{\sigma_1^{cu}} \right)^2 = 1 \quad (2.2)$$

$$d_{mt}^2 = \frac{(\sigma_{22} + \sigma_{33})^2}{(\sigma_2^{tu})^2} + \frac{\sigma_{23}^2 - \sigma_{22}\sigma_{33}}{(\tau_{23}^u)^2} + \frac{\sigma_{12}^2 + \sigma_{13}^2}{(\tau_{12}^u)^2} = 1 \quad (2.3)$$

$$d_{mc}^2 = \left[\left(\frac{\sigma_2^{cu}}{2\tau_{23}^u} \right)^2 - 1 \right] \left(\frac{\sigma_{22} + \sigma_{33}}{\sigma_2^{cu}} \right) + \frac{(\sigma_{22} + \sigma_{33})^2}{4(\tau_{23}^u)^2} + \frac{\sigma_{23}^2 - \sigma_{22}\sigma_{33}}{(\tau_{23}^u)^2} + \frac{\sigma_{12}^2 + \sigma_{13}^2}{(\tau_{12}^u)^2} = 1 \quad (2.4)$$

Hou's criterion [100] considers both intra-laminar and inter-laminar failure, with the latter in the form of delamination. For the intra-laminar damage, it considers fiber failure (equation 2.5), matrix cracking (equation 2.6) and matrix crushing (equation 2.7). In comparison to Hashin's criterion, this one differs in the matrix related criteria and in the fact that it includes a delamination criterion (equation 2.8). In these equations, τ_{ij} represents the tensile stress along the i-j plane, and t_n , t_s and t_t are the components of the normal and first and second shear tractions, respectively [13, 99].

$$d_f^2 = \left(\frac{\sigma_{11}}{\sigma_1^{tu}} \right)^2 + \left(\frac{\tau_{12}}{\tau_{12}^u} \right)^2 \leq 1 \quad (2.5)$$

$$d_{mt}^2 = \left(\frac{\sigma_{22}}{\sigma_2^{tu}} \right)^2 + \left(\frac{\tau_{12}}{\tau_{12}^u} \right)^2 \leq 1 \quad (2.6)$$

$$d_{mc}^2 = \frac{1}{4} \left(\frac{-\sigma_{22}}{\tau_{12}^u} \right)^2 + \left(\frac{(\sigma_2^{cu})^2 \sigma_{22}}{4 \sigma_2^{cu} (\tau_{12}^u)^2} \right) - \left(\frac{\sigma_{22}}{\sigma_2^{cu}} \right) + \left(\frac{\sigma_{12}}{\tau_{12}^u} \right)^2 \leq 1 \quad (2.7)$$

$$d_{del}^2 = \left(\frac{\sigma_{33}}{t_n} \right)^2 + \left(\frac{\sigma_{13}}{t_s} \right)^2 + \left(\frac{\sigma_{23}}{t_t} \right)^2 \geq 1 \quad (2.8)$$

Puck's criterion [101] is often used together with Hashin's criterion, modelling matrix failure, as in matrix-dominated failure situations, this criterion has presented more accurate predictions for both compressive and in-plane shear loads, because it considers the failure related to interaction between the fibers and the matrix. The proposed equation is 2.9 [13, 102].

$$\left[\left(\frac{\sigma_{11}}{2\sigma_1^{tu}} \right)^2 + \frac{\sigma_{22}^2}{\sigma_2^{tu} \sigma_2^{cu}} + \left(\frac{\sigma_{12}}{\tau_{12}^u} \right)^2 \right] + \sigma_{22} \left(\frac{1}{\sigma_2^{tu}} + \frac{1}{\sigma_2^{cu}} \right) = 1 \quad (2.9)$$

Another criterion is the Tsai-Wu's [103] criterion, which is particularly useful when considering woven composites. It is quite simple to apply, but it lacks in not distinguishing the several failure types in composites. It is defined by equation 2.10, where F_i and F_{ij} represent second and fourth rank stress tensors, respectively [104].

$$F = F_{11}\sigma_{11}^2 + F_{22}\sigma_{22}^2 + F_{33}\sigma_{33}^2 + 2F_{23}\sigma_{22}\sigma_{33} + 2F_{13}\sigma_{11}\sigma_{33} + 2F_{12}\sigma_{11}\sigma_{22} + F_1\sigma_{11} + F_2\sigma_{22} + F_3\sigma_{33} + F_{44}\tau_{23}^2 + F_{55}\tau_{31}^2 + F_{66}\tau_{12}^2 \quad (2.10)$$

Regarding inter-laminar damage, most commonly referred to as delamination, there are two common approaches, which are surface-based cohesive behavior and cohesive zone elements. In surface-based cohesive behavior, the delamination is defined between two adjacent plies, where one is the master and the other is the slave. It considers a linear elastic response up to the point where delamination occurs, after which there is a linear softening phase with the increase in delamination. Cohesive zone elements are introduced into the layers' interfaces, with their respective properties. In comparison to the usage of surface-based cohesive behavior, cohesive zone elements provide a more detailed simulation of the interaction but have a higher computational cost [13, 97].

The response of the cohesive interfaces is usually modelled through the traction-separation law, as it is presented in Figure 24, where t_n^0 , t_s^0 and t_t^0 represent the peak values of nominal stress in the normal, first or second shear directions, δ_n^0 , δ_s^0 and δ_t^0 represent the effective displacements in the normal, first or second shear directions at damage initiation, δ_n^f , δ_s^f and δ_t^f represent the effective displacements in the normal, first or second

shear directions at complete failure and K_i is the linear elastic behavior stiffness. In section A, the material exhibits linear elastic behavior, until point C, where delamination initiation occurs. Material' stiffness degradation takes place in section B [13, 17, 97].

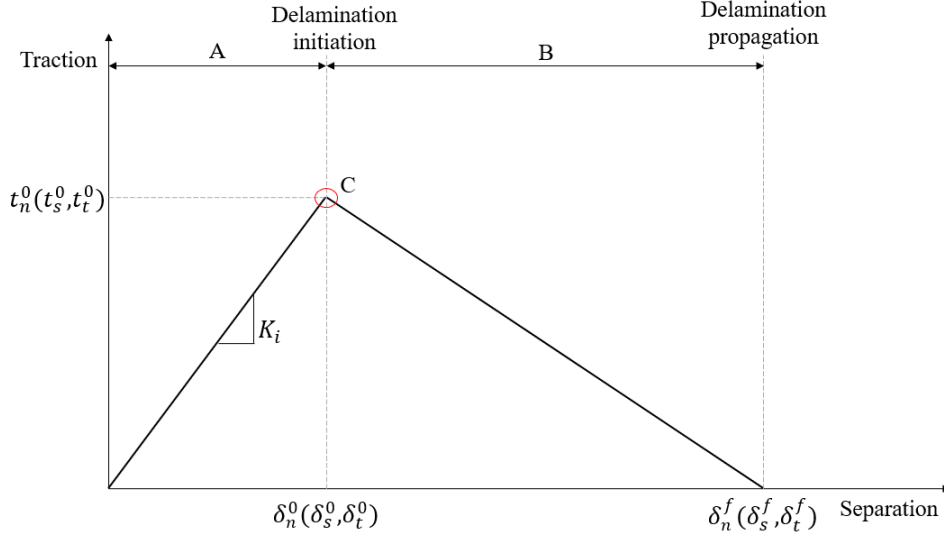


Figure 24 - Traction-separation law (based on [13, 17, 97]).

The overall damage of the cohesive interface is modelled through equation 2.11, where d is a scalar damage variable that varies between 0 and 1, and δ_m , δ_m^f and δ_m^0 are the instantaneous effective displacement, and the effective displacements at complete failure and damage initiation [13, 97].

$$d = \frac{\delta_m^f(\delta_m - \delta_m^0)}{\delta_m(\delta_m^f - \delta_m^0)} \quad (2.11)$$

The effective displacement can be obtained through equation 2.12, where δ_n , δ_s and δ_t are the instantaneous effective displacements along the normal, first or second shear directions, respectively [13, 97].

$$\delta_m = \sqrt{\delta_n^2 + \delta_s^2 + \delta_t^2} \quad (2.12)$$

Damage evolution laws are usually based in energy dissipation. The power law energy criterion allows for the calculation of the total fracture energy, as a sum of total normal, tangential and fracture energies, as it is presented in equation 2.13, where G_n , G_s and G_t are the instantaneous fracture energies along the normal, first or second shear directions, respectively, and G_n^c , G_s^c and G_t^c are the critical fracture energy values at which failure initiation occurs along the normal, first or second shear directions, respectively, and α is a numeric constant [13, 97].

$$\left(\frac{G_n}{G_n^c}\right)^\alpha + \left(\frac{G_s}{G_s^c}\right)^\alpha + \left(\frac{G_t}{G_t^c}\right)^\alpha = 1 \quad (2.13)$$

Another possible law is the Benzeggagh-Kenane, considers identical fracture energy along two shear directions ($G_s^c = G_t^c$), and is expressed through equation 2.14, where η is a material parameter determined through MMB tests, $G_T = G_n + G_s + G_t$, and G^c is the total energy dissipated due to failure [105-107].

$$G_n^c + (G_s^c - G_n^c) \left(\frac{G_s + G_t}{G_T}\right)^\eta = G^c \quad (2.14)$$

The damage initiation of the interface is usually determined by using the quadratic nominal stress criterion, presented in equation 2.15, that is designated as QUADS in ABAQUS [13, 97, 107].

$$\left(\frac{t_n}{t_n^0}\right)^2 + \left(\frac{t_s}{t_s^0}\right)^2 + \left(\frac{t_t}{t_t^0}\right)^2 = 1 \quad (2.15)$$

An alternative for damage initiation is the maximum nominal stress criterion, or MAXS in ABAQUS, presented in equation 2.16. The difference between them is that MAXS considers damage initiation when one of the stress ratios reaches a value of 1, while QUADS considers the concurrent quadratic ratios in all the directions [107, 108].

$$\max\left(\frac{t_n}{t_n^0}, \frac{t_s}{t_s^0}, \frac{t_t}{t_t^0}\right) = 1 \quad (2.16)$$

Several models have been created in this regard to study several aspects related to FRP drilling. Some of these focus on the most relevant issue in composite's materials drilling, which is delamination. Feito et al. [17] developed both a simplified model, considering the drilling tool as a punch that pushes through the laminate, and another complete model, where the drill's feed and cutting speed are both simulated, to predict delamination during drilling of CFRP. Both models used Hou's criterion for intra-laminar damage, considering cohesive elements for inter-laminar damage. While the simplified model required less processing time, it also only provided delamination prediction, needing the thrust force as an input. On the contrary, the complete model also provided the delamination prediction along with the thrust force values. The authors found that the simplified model provided an overestimation of the delamination factor, being more conservative. Higuchi et al. [109] studied push-out delamination in CFRP. In the first stage of their research, they conducted experimental tests drilling a 16-ply laminate, interrupting the drilling process to access the delamination at several points. With the experimental observations, a simplified simulation model was developed. The authors concluded that after the drill penetrates the workpiece material, the plies on the bottom suffer transverse cracks, which reduce the bending stiffness. It is the bending of the material that causes delamination. They also found that push-out delamination can be controlled by optimizing the thickness and material properties of the outermost ply. A thicker ply will result in more delamination, because it is more prone to propagate transverse cracks. Using a stiffer ply was also found to be beneficial for reducing delamination. The developed model allowed for the optimization of both these parameters, along with predicting the peak thrust force, delamination area and propagation. Seo et al. [97] conducted a numerical investigation, supported by experimental results, of the delamination when drilling FRP, considering the usage of back-up plates and various sets of cutting parameters. The proposed model allowed for the prediction of thrust forces and delamination using as inputs the tool geometry, cutting parameters and material properties. For damage criteria, the model considered cohesive surfaces, along with Hashin's criterion for fiber failure together with Puck's criterion for matrix damage. In terms of thrust force, the authors observed that the simulation results had an average error of 10.7%, with the lower cutting speeds presenting an under-estimation, and the higher ones presenting an over-estimation, which could partly be attributed to the friction coefficient of the simulation being considered as a fixed value. Regarding delamination, the simulations using the back-plate had closer results to reality than the ones not using them, with average errors of 5.32% and 9.17% respectively. The backup-plates, as expected, reduced exit delamination, by concentrating the fracture damage within the hole of the backup-plate, preventing it from propagating along the fibers.

FEM simulations have also been conducted to optimize the drill's geometry to reduce delamination. Isbilir et al. [110] developed a finite element model to access the influence of

the drill geometry in delamination. The proposed model used Hashin's criterion for intra-laminar damage, considering surface-based cohesive behavior, with the quadratic stress criterion for damage initiation and power law energy criterion for damage evolution. The tested drill geometries were a conventional twist drill and a step drill with different geometrical parameters. The prediction's errors using a twist drill were of 1.3% for the thrust force, 3.5% for the torque and 8% for the delamination factor, which highlighted the potential of using FEM for optimization of a drill's geometry instead of the more conventional and expensive experimental means. The authors recognized that the usage of an accurate friction model, inclusion of thermal effects, consideration of tool wear and the drill's deformations are all factors that could improve the accuracy of the simulation. The simulation results were also in accordance with the experimental ones in comparing both geometries, meaning that the twist drill had higher thrust forces and torque than the step drill for the same cutting parameters. Tejas et al. [77] used FEM analysis to optimize a HSS twist drill's point angle and cutting parameters to reduce delamination. The simulation results were in accordance with the experimental ones, with the lower feed producing less delamination. The authors were also able to conclude that HSS was not a suitable tool material when drilling FRP. Sugita et al. [88] developed a dedicated drill design with the purpose of reducing both delamination and burr. In order to test the design, the authors developed a model based on Hashin's criterion for fiber damage and Puck's criterion for matrix damage. The model also used cohesive zone elements and the power law energy criterion for damage evolution. With the aid of the model, it was possible to optimize several geometric parameters of the drilling tool without experimentation, which would be more expensive, thereby confirming the capabilities of FEM in tool design.

Tool wear is also an aspect of the drilling process that has been studied through FEM, even though it is a very complex topic. Chen et al. [111] developed a model capable of predicting the thrust force when drilling a CFRP, considering the effect of tool wear. This model was produced using ABAQUS, applying an imported wear numerical model written with Python. It considered the Hashin's failure criterion for fiber failure and Puck's criterion for matrix damage. The authors found that the drilling process could be divided into three stages: the first stage, where drilling tool was entering the workpiece and the thrust force exhibited an upward trend, the second, when the drills cutting edges were all engaged in the drilling process and the thrust force stabilized, and a third stage, when the drilling tool started to exit the workpiece and the thrust force presented a downward trend. This was verified both in the simulation and experimental results. During the second stage, the simulation model predictions were very close to the values obtained experimentally, with the maximum error below 10%. Feito et al. [112] developed a finite element model to access the effects of tool wear when drilling woven composites, along with the effects of point angle and cutting parameters with a worn tool. The developed model used cohesive elements, based on the traction-separation law, considering the quadratic nominal stress criterion for damage initiation and the power law energy criterion for damage evolution. The authors found that the model was able to accurately predict both thrust force and delamination, with a slight overestimation of the latter, being applicable for simulating worn tools or complex geometries, which can avoid the costs associated with experimental testing. Regarding delamination, the authors found that both for new and worn tools, the feed had a much greater impact than the cutting speed, with lower feeds combined with high cutting speeds being beneficial. However, when considering the worn tool, the feed had less of an influence versus when considering a new tool, even though it produced holes with higher delamination factors.

Thermal effects are important to consider when developing a FEM of the drilling process. As the drilling tool goes through the workpiece, their temperature rises, which results in its thermal expansion. Given that CFRP materials have low thermal conductivity, the heat accumulates around the drilled hole, leading to a reduction of the material's elastic modulus,

along with an increased contact area between the drill and the workpiece material. Kubher et al. [113] developed a coupled thermo-mechanical model for multi-directional composites drilling. The proposed model simulated material removal and machining induced damages, with in-situ temperature and critical thrust force predictions. The results were validated through experimentation. The authors concluded that the inclusion of temperature dependent properties had a significant impact on the thrust force predictions, as they tended to be higher when these properties were not considered, because the material softening behavior was not considered. Karpas et al. [114] developed a thermo-mechanical model of the interface between the drill's margin and the hole's surface. The model allowed the authors to confirm that the inclusion of temperature-dependent material properties was very important to understand the contact conditions between the drill and workpiece. Also, they observed that despite resulting in more delamination due to higher thrust forces, using high feeds facilitated heat transfer to both the chip and the drill. Kubher [106] et al. developed a finite element model to predict the critical thrust force at different cutting temperatures for 1 and 2 plies, using cohesive elements and based on the traction-separation law, considering the Benzeggagh–Kenane criterion for damage evolution. The authors observed that with an increase in temperature, the critical thrust force decreased. This decrease was more prominent in the 2 plies rather than the 1 ply. The predicted values for the critical thrust force were also found to be in accordance with the experimental results. Jia et al. [108] conducted a study focused on delamination free drilling. The authors developed an analytical model to predict the critical thrust force, which considered the thermal effects. This model was implemented on ABAQUS, considering cohesive elements, using Hashin's criterion for intralaminar damage and considering the maximum nominal stress criterion. It was presented that the thrust force predictions were within 9.4% of the experimental values, and for a unidirectional laminate, the delamination had an elliptical shape, which was predicted by the model's implementation. The drilling temperature was found to have a significant impact on the critical thrust force. With an increase in temperature, more heat was found to be accumulated at the hole exit, leading to a decrease in the critical thrust force, which promoted delamination.

Zhenchao et al. [115] conducted a sensitivity analysis to assess the impact level of each property of the workpiece material in the thrust forces of the drilling process. The model used for this analysis used a 2D Hashin criterion and a twist drill, with a laminate composed of multi-directional plies. The authors concluded that the most influential parameters in terms of thrust force were the Young's modulus and the Poisson's coefficients. Increasing the Young's modulus along the transverse and out-of-plane directions by ten times resulted in a variation of the thrust force of 34% when comparing to the normal results. If, for example, the compressive longitudinal strength was increased ten times, the thrust force results would only vary 5.8%.

Zhang et al. [31] studied the variation of a woven CFRP load bearing properties due to drilling induced delamination damage. The aim of the study was to develop a finite element model that could predict the properties of a drilled composite due to delamination damage, based on experimental results. The model used the Tsai-Wu intra-laminar damage criterion, considering cohesive elements with the quadratic nominal stress criterion for damage initiation and the power law energy criterion for damage evolution. Regarding fracture failure of the drilled workpiece under either tensile or compressive loads, the authors find the experimental results to be in good agreement with the FEA model, with the material showing an elastic brittle failure as it was expected, and the stiffness in the simulation being slightly higher. Both the simulation and the experimental results showed that the failure starts at the hole, and then expands. A three point bending test was also performed, where in terms of damage mode and bending mechanical response, the experimental and simulation results were close. The delamination in the drilled workpiece was found to have a greater impact when it's

under compressive loads rather than tensile loads. This is because under such loading conditions, damage leads to the buckling of the different layers and reduces the ability to transfer the shear stress between the layers. The obtained results confirmed that the delamination needs to be considered when designing load bearing structures, due to otherwise potential premature failure, and that the proposed model is a crucial tool in the design of the structures.

2.9. Drilling of CF RTP

CF RTP materials have been gaining relevance relatively to the more popular CFRP thermoset matrix counterparts, as they are easier to store, and allow shorter manufacturing cycles that also are less energy intensive. Their thermoplastic nature allows them to be more easily repaired and recycled, as these materials can be melted and remolded, offering a more sustainable option to their thermoset counterparts [116-118].

When comparing the drilling process between carbon fiber reinforced thermoset and thermoplastic materials, there are some distinguishing factors. As it is claimed by Ahmad et al. [119], thermoplastic materials are more ductile than thermosets, leading their chips to be long and continuous instead of discontinuous powders. Xu et al. [120] compared the drilling process of carbon fiber reinforced epoxy (thermoset) and polyimide (PI, thermoplastic). In terms of thrust forces, the epoxy-based material produced higher peak values than the PI. Regarding the hole's accuracy, more consistent overall tolerances were obtained for the PI material, which were also closer to the nominal value. There was consistency in terms of the influence of the cutting parameters for both materials, being that higher cutting speeds led to worse accuracy, which was thought to be due to more intense tool vibration. In cylindricity, the PI matrix material also obtained superior results, which was thought to be due to the higher temperatures that were reached in drilling the epoxy material. Hocheng and Puw [121] compared both a carbon epoxy thermoset and a carbon acrylonitrile butadiene styrene (ABS) thermoplastic, in terms of their chip formation and hole's surface quality. In terms of chip formation, they found that the thermoplastic based material produced a continuous chip, which was associated to high ductility, particularly when its glass transition temperature was superseded, while the thermoset material produced discontinuous chip, given its relative brittleness. The thermoplastic material's hole surface was found to be of higher quality than the thermoset one, which presented fiber pullouts and matrix cracking.

The drilling of some CF RTP materials has been studied in literature, along with some relevant aspects related to its machining process. Xu et al. [116] studied the drilling process of both carbon fiber reinforced PI and polyetheretherketone (PEEK). The carbon/PEEK material produced higher thrust forces, which can be explained by continuous ribbon-like chips that lead to tool clogging. Also, lower cutting speeds produced higher thrust forces. Looking at the machining temperatures, it was found that the PEEK composite reached higher temperatures that were above its glass transition temperature, particularly at high cutting speeds, which lead the material to be in a softer and more rubbery state, making it more ductile and thereby more prone to plastic deformation, leading to continuous chips. The higher thrust forces in the PEEK composites also led to higher delamination factors, particularly at higher feed and lower cutting speed values. The continuous chip formation and higher ductility was however found to produce holes with better surface quality than in the PI material, which consequently led to superior hole accuracy. The wear tendencies were worse for the PEEK material, as it presented more severe chip adhesion, due to the higher cutting temperatures. Ge et al. [117] also studied the drilling of carbon/polyetheretherketone (PEKK) material, finding that the peak thrust force increased almost linearly with the feed, with a deviation from the tendency at the lower feeds, due to the continuous ribbon-like chips that were clogging the tool. At these lower feeds, the chips were also longer and more bent, most likely due to their lower

thickness and to being compressed by the drilling tool. The authors additionally found that higher feeds led to lower temperatures, as the tool was less time in contact with the workpiece. They also accessed the delamination factor at several feeds (between 0.025 and 0.20 mm/rev), and found that the delamination factor increased beyond 0.05 mm/rev, but decreased between 0.025 and 0.05 mm/rev, which was thought to be due to the loss in stiffness attributed to the higher machining temperatures at these feeds. Lopez-Arraiza et al. [122] studied drilling of poly-cyclic butylene (pCBT), stating that the continuous ribbon like chips were beneficial to the recyclability of the material, as they could be used as reinforcements in the manufacturing process. Meinhard et al. [123] looked into the effect that drilling induced defects could have on thermoplastic composites, considering a carbon fiber reinforced PA 12 material. The authors referred that unlike in CFRP composites, the most severe damage type was burrs instead of delamination, particularly at the hole's exit. Under tensile load, the authors found that the hole's quality had little to no effect on the mechanical properties, with the influence being mainly due to the hole's presence regardless of its quality.

Chapter 3

Experimental Procedures

This chapter explores the procedures and testing methodologies applied throughout the development of the experimental work. Firstly, the used CFRTP material is presented, along with the applied material testing procedures. Afterwards, the hole drilling setup, parameters and hole analysis techniques are exposed.

3.1. Material

In this study, a 16-ply 2x2 twill carbon fiber prepreg with a PA 12 resin will be analyzed under instrumented drilling tests and preliminary characterization (tensile, compression and shear loading). The plates, which are presented in Figure 25a, are 4 mm thick, with each ply having 0.25 mm, and a length and width of 200 mm. Owing to its architecture, this material does not exhibit different behavior along the longitudinal and transverse directions. One of the plates has been manufactured with an aluminum foil sheet in between the middle plies, in order to enable a pre-crack for fracture testing, which is presented in Figure 25b. These plates were manufactured through compression molding, at 260°C and 140 bar.

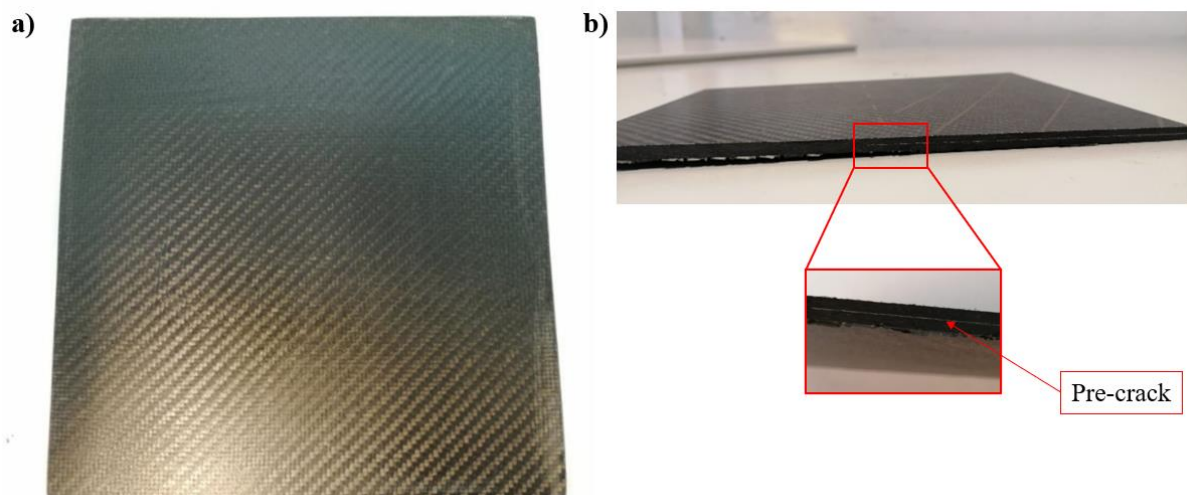


Figure 25 - CFRTP material used for testing: (a) plate; (b) plate with pre-crack.

3.2.Mechanical Testing

The material's mechanical tensile, compressive and shear property determination is necessary for the development of the FEA model. The required properties can be calculated through the standardized testing, with some of them presented in Table 2.

Table 2 - Standardized tests for determination of mechanical properties.

Test Type	Property	ASTM Standard
Tensile	$E_1, E_2, \sigma_1^{tu}, \sigma_2^{tu}, \nu_{12}$	D 3039
	E_3, σ_3^{tu}	D 7291
Compressive	$E_{1c}, E_{2c}, \sigma_1^{cu}, \sigma_2^{cu}$	D 6641
Shear	$G_{12}, G_{13}, G_{23}, \tau_{12}^u, \tau_{13}^u, \tau_{23}^u$	D 5379

Where E_1 , E_2 and E_3 represent the Young's modulus in the longitudinal, transverse and out-of-plane directions, respectively, and σ_3^{tu} is the ultimate tensile strength along the out-of-plane direction. As for ν_{12} it is correspondent the Poisson's coefficient in the 1-2 plane. Regarding E_{1c} and E_{2c} they represent the compression modulus along the longitudinal and transverse directions, respectively. As for G_{12} , G_{13} and G_{23} and τ_{12}^u , τ_{13}^u and τ_{23}^u , they are the shear modulus and the ultimate shear strength in the 1-2, 1-3 and 2-3 planes, respectively. Some of these properties were determined experimentally in this thesis, with the methodology used for each exposed next.

3.2.1.Tensile testing

For the determination of the material's tensile properties, ASTM D 3039 was followed. The procedure begun by manufacturing the testing specimens using a sawing machine from Wendt GmbH, with a cutting disc with a diameter of 335 mm, rotating at 2900 rpm, using water for cooling, as it is presented in Figure 26a. The specimens after manufacturing are presented in Figure 26b. Their width and thickness were measured within the gage section a total of three times using a flat anvil digital micrometer from Mitutoyo, with a measuring range of 0 to 25 mm and an accuracy of 0.001 mm. The obtained measurement results are present in Appendix A.

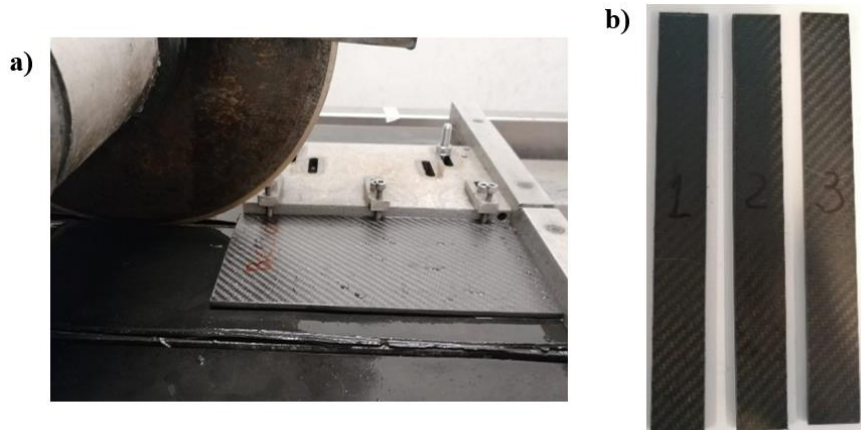


Figure 26 - Tensile specimen's manufacturing: (a) sawing machine; (b) tensile specimens after production.

The testing machine that was used was the Instron 5900R, presented in Figure 27a. To measure the strain, the digital image correlation (DIC) method was applied. In Figure 27b, it is possible to see both the DIC camera and spotlight. After capturing, the DIC data was processed using the VIC-2D software, in which virtual extensometers were placed. For tensile testing, such placement is presented in Figure 27c.

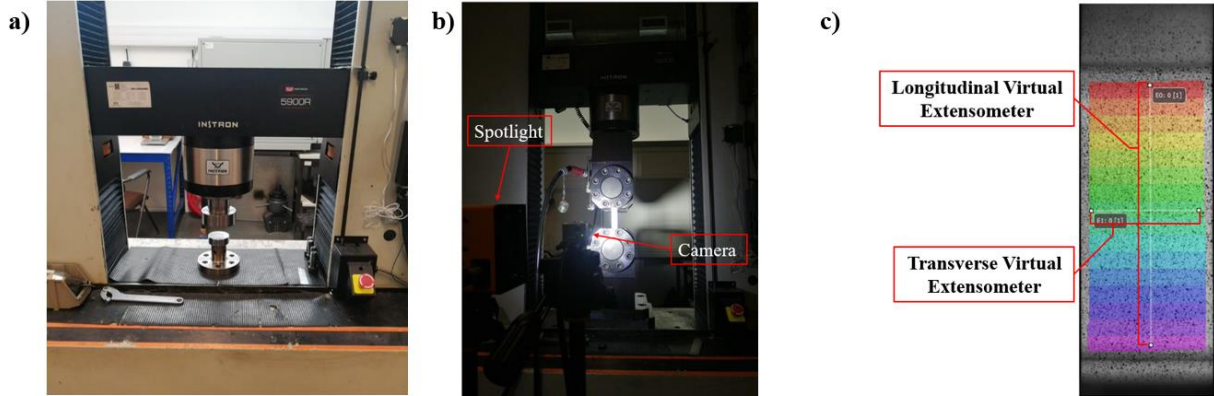


Figure 27 - Tensile testing: (a) Instron 5900R machine; (b) DIC testing setup; (c) virtual extensometer placement in tensile testing.

The speed of the test was 2 mm/min, with the force and displacement data being recorded through a 200 kN load cell. To grip the specimen to the testing machine, hydraulic grips were used, as presented in Figure 28 [124].



Figure 28 - Tensile testing setup with hydraulic grips.

With the testing results, it was possible to determine the tensile properties E_1 , E_2 , σ_1^{tu} , σ_2^{tu} and ν_{12} . The stress-strain curves were obtained using equation 3.1, where σ represents the stress, F is the force (obtained through the load cell data) and A the area of the cross-section of each specimen at the gage section (obtained by multiplying its thickness by its width).

$$\sigma = \frac{F}{A} \quad (3.1)$$

Using the longitudinal strain DIC data, it was possible to obtain the tensile stress-strain curves. The tensile strength was calculated using equation 3.1, where the considered force (F) value was the peak measured value. The Poisson's ratio (ν) was obtained using the strain data through equation 3.2, where ε_t is the strain along the transverse direction and ε_l is the strain along the longitudinal direction [124].

$$\nu = -\frac{\varepsilon_t}{\varepsilon_l} \quad (3.2)$$

Regarding the elasticity modulus (E), it was calculated considering the slope of the stress-strain curve on the linear-elastic region. Such was achieved using equation 3.3, where $\Delta\sigma$ is the difference in stress between those two points and $\Delta\varepsilon$ the difference in longitudinal strain [124].

$$E = \frac{\Delta\sigma}{\Delta\varepsilon} \quad (3.3)$$

3.2.2.Compressive Testing

The determination of the compressive properties was performed in accordance with ASTM D 6641. Three compressive testing specimens were manufactured (presented in Figure 29a) using the same sawing machine that was used to produce the tensile specimens. Their width and thickness were measured three times in the respective gage section, using the same micrometer as for tensile testing, along with their length, which was measured using a digital caliper, with an accuracy of 0.01 mm. The obtained measurements are presented in Appendix A. The virtual extensometer placement is highlighted in Figure 29b.

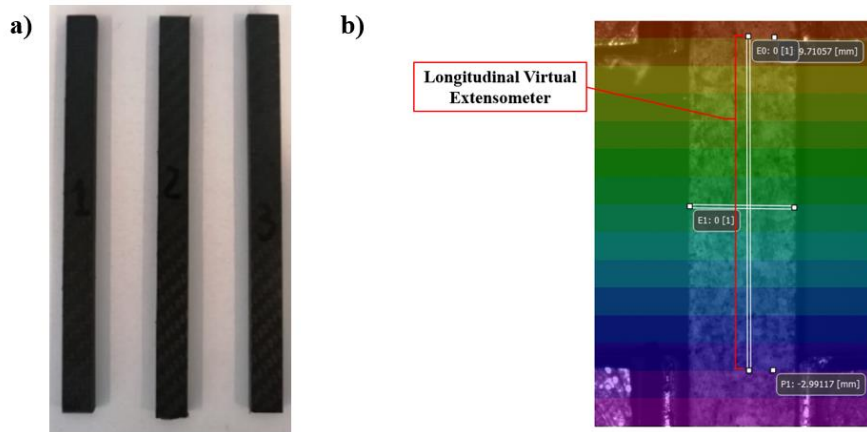


Figure 29 - Compressive testing: (a) compressive specimens; (b) virtual extensometer placement in compressive testing.

The testing machine and DIC setup were the same as for tensile testing. The testing speed was 1.3 mm/min, and the force displacement data was recorded using a 100 kN load cell. The testing specimens were mounted into the combined loading compression (CLC) test fixture, as it is represented in Figure 30, and were then placed into the testing machine, in which a compressive load was applied [125].

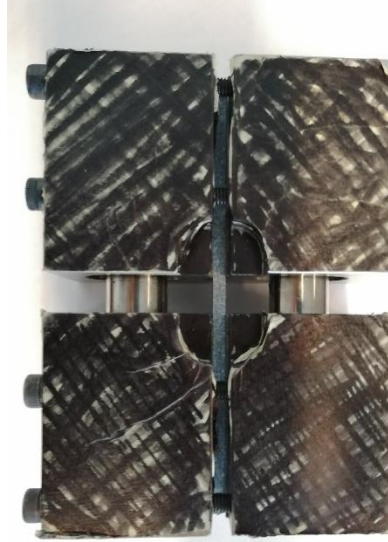


Figure 30 - CLC test fixture.

The determined compressive properties, E_{1c} , E_{2c} , σ_1^{cu} and σ_2^{cu} were obtained by following ASTM D 6641. These were all determined following the same procedure as for the tensile properties. In the testing standard it is established that the specimen's thickness should be verified to prevent Euler column buckling from occurring. This is verified by the relation presented in 3.4, where *thickness* corresponds to the specimen's thickness and l_g is the gage section, which in the performed testing was of 13 mm [125].

$$thickness \geq \frac{l_g}{0.9069 \sqrt{\left(1 - \frac{1.2\sigma_1^{cu}}{G_{12}}\right) \left(\frac{E_1}{\sigma_1^{cu}}\right)}} \quad (3.4)$$

3.2.3. Shear Testing

The shear properties were determined as specified in ASTM D 5379. A total of three testing specimens were manufactured (presented in Figure 31a), using the same sawing machine as for the tensile specimen and an end mill for the V-shaped notch. The milling process used a 1.5 mm mill with two teeth, at a cutting speed of 60 m/s, a feed of 0.03 mm/tooth, and axial depth of cut of 0.25 mm, as presented in Figure 31b. The same digital micrometer as the one used in tensile testing was used to measure the specimen's width and thickness at the V-notch, with the obtained dimensions presented in Appendix A. Two virtual extensometers were used at a 45° angle with the horizontal direction, as evidenced by Figure 31c.

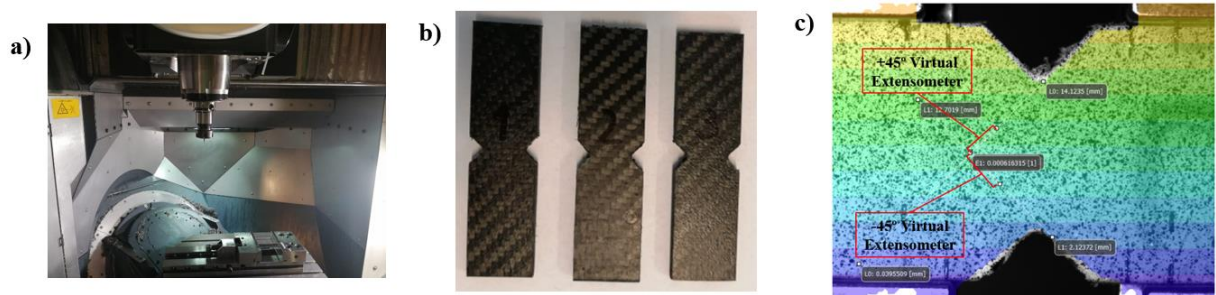


Figure 31 - Shear testing: (a) end-milling operation; (b) shear specimens; (c) virtual extensometer placement in shear testing.

The testing machine and DIC setup were the same as for tensile testing. The testing fixture used was the one presented in Figure 32, where a compressive load is applied to the right side to induce shear stress. The distance between the right and left parts of the fixture was set to 13 mm. The testing speed was set to 2 mm/min, as in accordance with the testing standard. The force and displacement data were recorded through a 100 kN load cell [126].



Figure 32 - Shear testing fixture.

Both G_{12} and τ_{12}^u were determined by following ASTM D 5379. The shear strain was obtained by equation 3.5, where γ is the actual shear strain and ε_{+45° and ε_{-45° are the strains along two directions that make a 45° angle with the horizontal axis (as represented in Figure 31c). As specified by the testing standard, the shear strength was calculated using equation 3.1, where the force (F) value corresponds to the lowest between the peak value and the value at 5 % strain [126].

$$\gamma = |\varepsilon_{+45^\circ}| + |\varepsilon_{-45^\circ}| \quad (3.5)$$

With the shear results, the shear modulus G was calculated through equation 3.6, considering a shear strain variation $\Delta\tau$ between two points where the difference in shear strain $\Delta\gamma$ should be, according to ASTM D 5379, of 0.004 [126].

$$G = \frac{\Delta\tau}{\Delta\gamma} \quad (3.6)$$

To obtain the material's interlaminar fracture energy in pure mode I loading, fracture testing was also conducted, so that the critical thrust force could be calculated. However, as this testing did not produce any valid results, it is reported in Appendix B. Thereby, it was not possible to calculate the critical thrust force value.

3.3. Instrumented Hole Drilling Tests

The hole drilling campaign begun with preliminary tests, where the cutting parameters were selected from literature. Afterwards, based on those results, a set of three feed and cutting speed values were selected, and a full factorial drilling campaign was developed, where different combinations of tool geometry, coating and cutting parameters were tested and analyzed. Some outlier drilling peak thrust force results were found during this campaign, which were consequently repeated, due to presenting results that did not follow an identified tendency.

Due to the long-winded process of tool wear analysis, the obtained results (from the major experimental campaign) allowed for the definition of a set of cutting parameters to be tested for one particular tool geometry, focusing on the assessment of wear tendencies. The following subsections explore the several aspects related with all these campaigns in terms of the setup and the analysis procedures.

3.3.1. Drilling Tests Setup

All the drilling experiments were carried out in a DMG Mori DMU 60 eVo CNC machine, pictured on Figure 33a. The clamping system used for the CFRTP plates is presented in Figure 33b (and the respective technical drawing is presented in Appendix C), where the holes were drilled along the red line. During all drilling tests, no coolant was used.

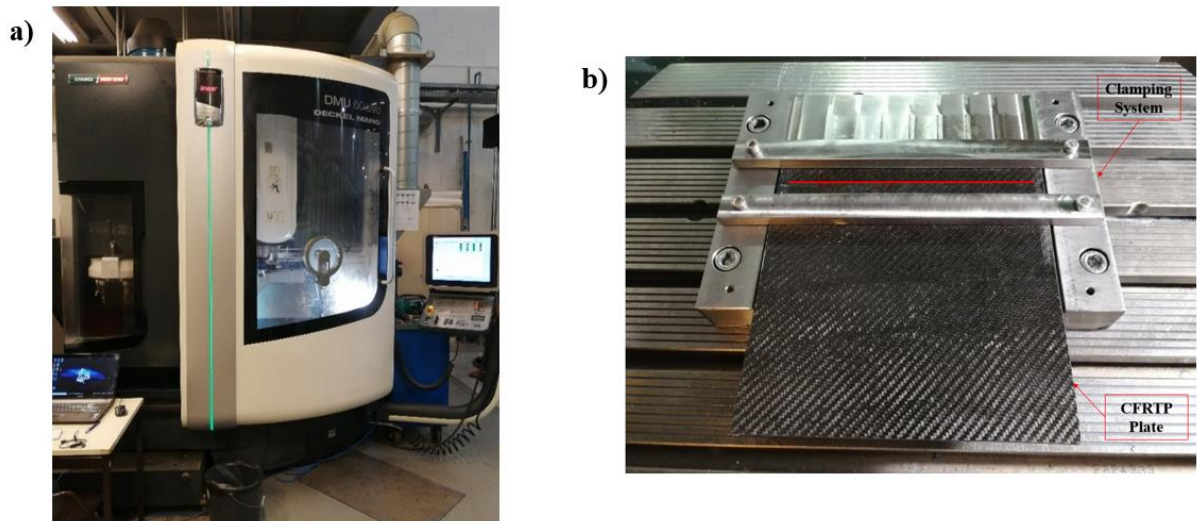


Figure 33 - Drilling experimental tests setup: (a) DMG Mori DMU 60 eVo CNC machine; (b) Clamping system.

As observable through Figure 34, the clamping system has a flat surface, where the composite plate rests, on which a slot was machined to allow the drilling process. Based upon the literature references exposed on subsection 2.4 regarding clamping systems, the width of this slot (which as seen in Appendix C was set to 16 mm) was kept at a low value, so that excessive plate deflection does not occur, which complicates the drilling process. The slot's shape also allows the plate to be supported at two ends, minimizing its deflection.

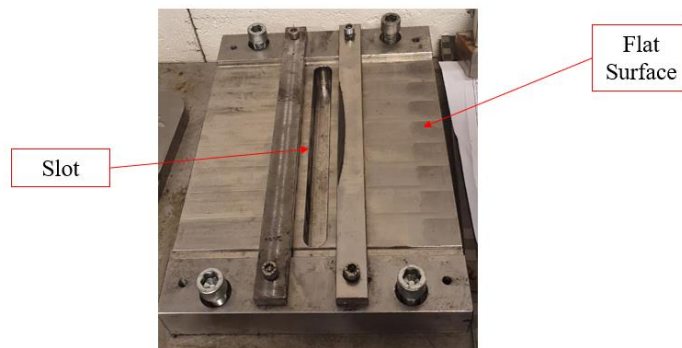


Figure 34 - Highlight of clamping system's features.

3.3.1.1. Drilling Tools

Regarding the drilling tools, two different geometries were used, a conventional twist drill (Figure 35a and Figure 36 on the right) and a double-point angle twist drill (Figure 35b

and Figure 36 on the left). Both have a first point angle of 120°, helix angle of 30° and a diameter of 6 mm. The second point angle is 60°.

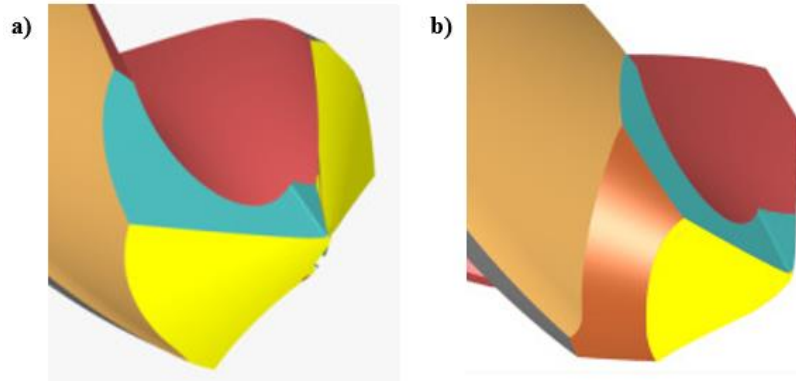


Figure 35 - Schematic of drilling tools geometry: (a) conventional twist drill; (b) double-point angle twist drill.

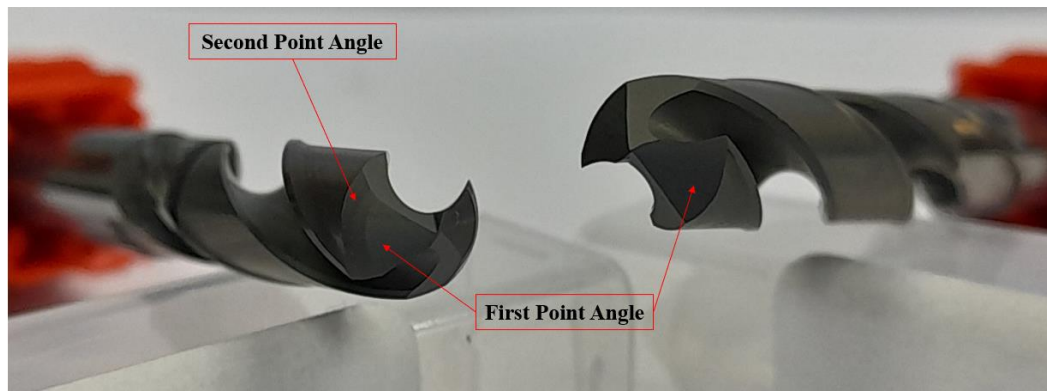


Figure 36 - Drilling tools.

The drills' substrate is WC-Co, with 7% of Co content. They are diamond coated through the HFCVD process, which begun with etching through a Murakami (KOH+K₃Fe(CN)₆+H₂O) and Caro (ethanol+diamond) solution. Two different specifications of diamond layers were deposited: MCD and NCD, with the respective application conditions presented in Table 3. During the deposition process, tungsten filament with 0.125 mm in diameter was used and kept at 2100±100 °C.

Table 3 - HFCVD parameters for each layer.

Layer	Flow Rate (mL/min)			Pressure (mbar)	Time (min)
	Total	H ₂	CH ₄		
MCD	1500	1480	20	60	450
NCD	1500	1440	60	60	800

Two coating specifications were considered: a one-layer MCD coating and a two-layer mixed coating, with an outer NCD layer and a MCD layer on the substrate's interface (designated simply as NCD coating hereinafter). These coatings exhibit different thickness, which was confirmed through an SEM analysis after the deposition process, where a thickness values between 7.5 and 7.8 µm were obtained for the MCD coating (Figure 37a), while the NCD coating was between 11 and 11.5 µm (Figure 37b).

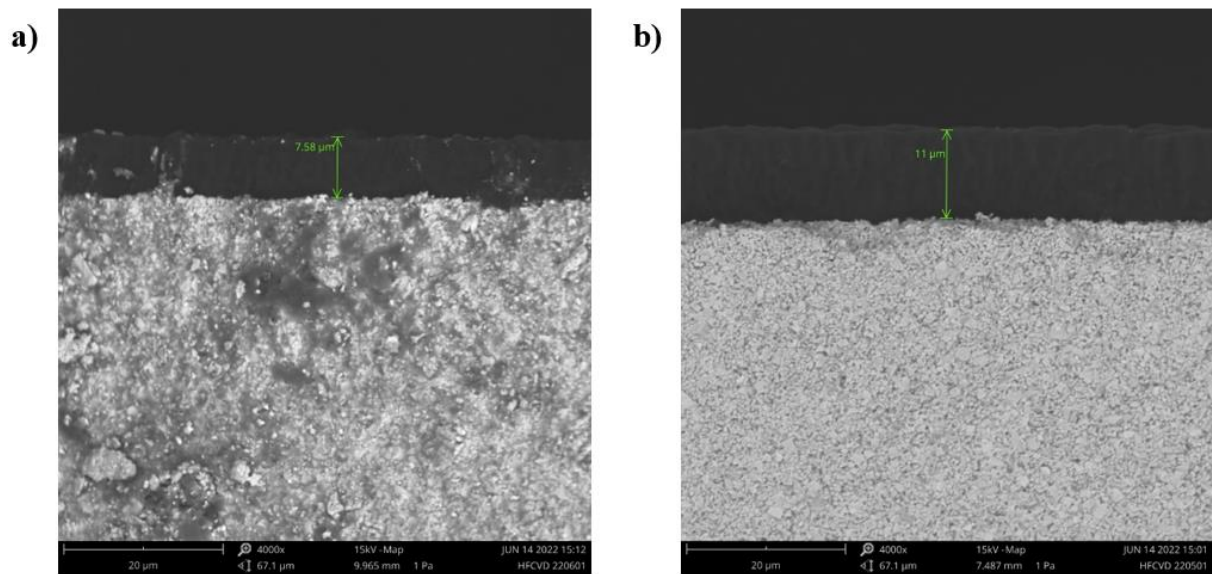


Figure 37 - SEM analysis of coating thickness after application: (a) MCD coating; (b) NCD coating.

Using a 3D optical measurement procedure, the drill's CER was measured to attest the impact of the different coating thicknesses. The results are presented in Table 4 for both the MCD and NCD coatings, along with an uncoated drill to serve as comparison. An uncoated hard metal drill was found to have a smaller CER than a drill with a MCD coating, which presents a difference of almost 7 μm in the CER when compared to a drill with a NCD coating.

Table 4 - CER of drills with different coatings.

Drill Coating	CER (μm)
Uncoated	3.484
MCD	7.208
NCD	14.032

With a similar procedure to the one followed for obtaining the hole wall' surface roughness (in 3.3.3.3), the Ra and Rz values for each coating were measured, with the main difference being the length of the profile. This measurement was conducted in the drill's flute, near the cutting edge, as highlighted in Figure 38. The obtained results are presented in Table 5, where the MCD coated drills present higher Ra and Rz values.

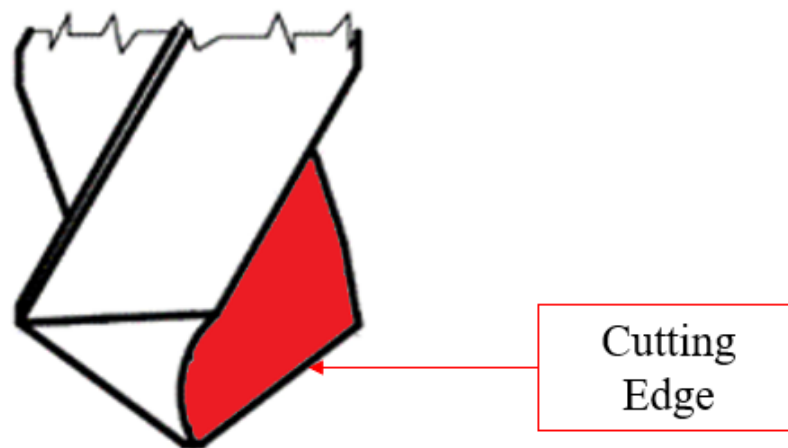


Figure 38 - Coating surface roughness measurement location (adapted from [25]).

Table 5 - Ra and Rz values obtained for each coating.

Drill Coating	Ra (μm)	Rz (μm)
MCD	0.285	6.444
NCD	0.136	1.425

Combining the two different coatings with the two different drill geometries, a total of four different drilling tools is reached, as it is represented through Table 6, where the nomenclature that will be used in the graphs of Chapter 4 is also presented.

Table 6 - Drill tools' geometry and coating combinations.

Geometry	Coating	Nomenclature in Graphs
Conventional twist drill	MCD	Conventional MCD
	NCD	Conventional NCD
Double-point angle twist drill	MCD	Double-Point MCD
	NCD	Double-Point NCD

3.3.1.2. Cutting Loads Acquisition

To record the cutting loads, with a focus in both the thrust force and the torque along the drill's vertical axis, the Kistler Type 9170A, which is a rotating cutting force dynamometer, was used. This device is mounted directly on the machine's spindle and has two parts: the stator and the rotor, as it is highlighted in Figure 39a. When mounting this dynamometer, the manufacturer recommends that the distance between the stator and the rotor should be between 1 to 10 mm. The stator is then connected to a Kistler Type 5238B signal conditioner, presented in Figure 39b, which is then connected to a Kistler Type 5697 data acquisition system pictured in Figure 39c, that allows integration with the Kistler DinoWare software, for results analysis and visualization. As recommended by the manufacturer, the spindle was left to rotate freely for 30 minutes before any recording was taken. When fixating the drilling tool onto the rotor, the distance between the drill's tip and the rotor was maintained at a constant value.

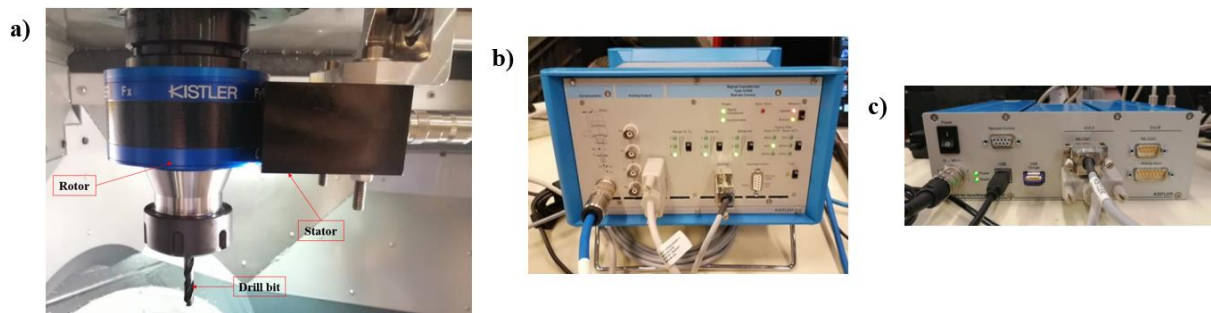


Figure 39 - Loads acquisition system: (a) Kistler Type 9170A rotating cutting force dynamometer; (b) Kistler Type 5238B signal conditioner; (c) Kistler Type 5697 data acquisition system.

Thrust force and torque results were analyzed in the Kistler DinoWare software. It was found that some thrust force signals presented drift, as shown in Figure 40a, potentially caused by heat exchange [127]. This issue required the usage of the drift compensation tool within the software, such as in Figure 40b. This drift compensation tool computes a straight line between the start and finish points of the data set, that is then subtracted to the measured values [128].

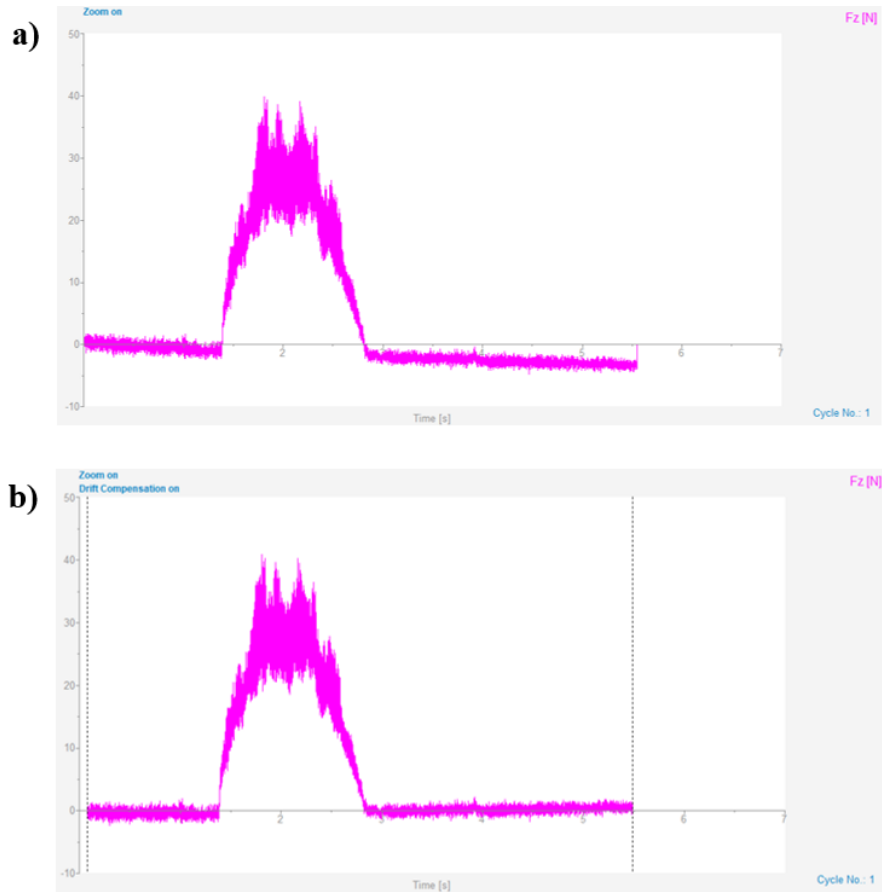


Figure 40 - Thrust force signals within the Kistler Dinaware software of a hole made with a conventional twist drill with a NCD coating at 00.05 mm/rev and 100 m/min: (a) thrust force signal with drift; (b) thrust force signal after applying drift correction.

In order to quantify the influence of each parameter towards peak thrust force and peak drilling torque, an analysis of variance (ANOVA) was conducted, using the IBM SPSS Statistics 26 software. This procedure was also used for the analysis of the delamination results.

3.3.2. Drilling Parameters

The testing parameters for the hole drilling were based on scientific literature, with the results from the conducted research presented in Appendix D. From this literature review, it was possible select and validate specific sets of maximum and minimum cutting parameters through preliminary testing, as presented in Table 7.

Table 7 - Preliminary testing parameters used with a double-point angle twist drill with NCD coating.

Feed (mm/rev)	Cutting Speed (m/min)
0.05	50
0.10	100
0.15	150

To better understand if the selected parameters were appropriate, 3 holes were drilled using a double-point angle twist drill with NCD coating, with first hole using a feed of 0.05 mm/rev and a cutting speed of 50 m/min, and the other two using a feed of 0.15 mm/rev and 150 m/min. The first two holes correspond to a combination of the least and most extreme cutting parameters. Through the second and third holes, this preliminary campaign also

attested if the planned hole spacing was appropriate. This spacing can greatly influence delamination extent, which can merge for different drilled holes in case they are too close to each other, as it was reported, for example, by Marques [129]. The planned spacing was 20 mm between consecutive hole's centers, along with 20 mm of distance between a hole's center and the plates edges, as it is figured in Figure 41.

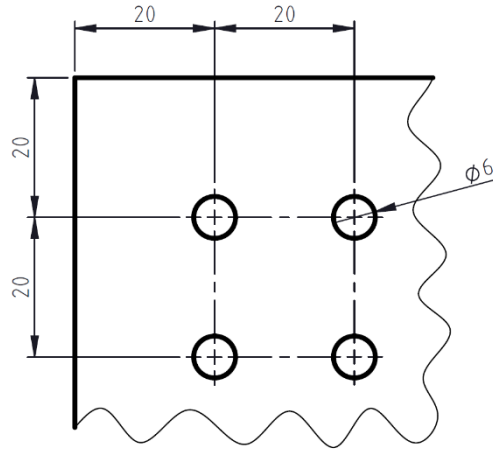


Figure 41 - Hole spacing.

An X-ray analysis of the preliminary holes was conducted to access the delamination damage, following a procedure that will be exposed in subsection 3.3.3.1. The obtained images are presented in Figure 42.

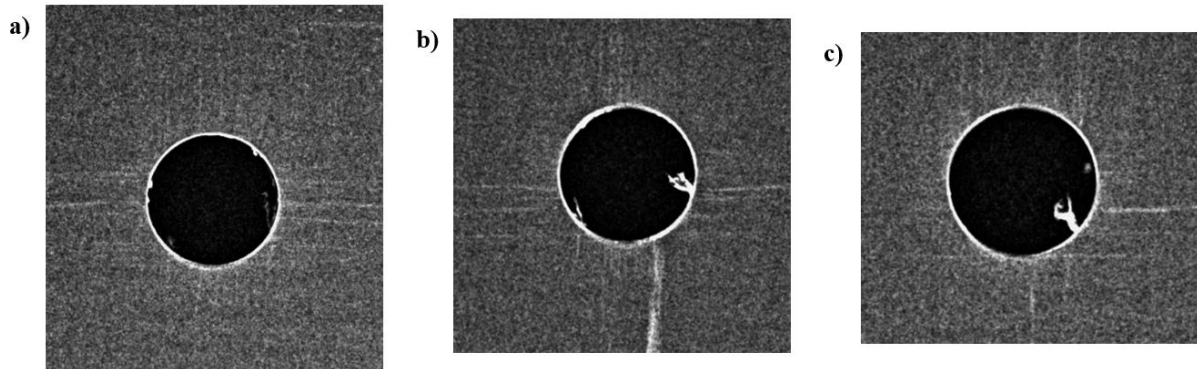


Figure 42 - X-ray of holes obtained from preliminary drilling campaign, using a double-point angle twist drill with NCD coating: (a) cutting speed of 50 m/min and a feed of 0.05 mm/rev; (b) first hole at a cutting speed of 150 m/min and a feed of 0.15 mm/rev; (c) second hole at a cutting speed of 150 m/min and a feed of 0.15 mm/rev.

The obtained results show the adequacy of the predicted 20 mm hole spacing. There was, however, slight ovalization of the holes with the higher speed and feed configurations (Figure 42b and c). For that reason, the maximum cutting speed was readjusted and limited to 130 m/min. Also, the minimum cutting speed was considered to be too low, and it was thereby increased to 70 m/min. The resulting selected testing parameters are presented on

Table 8. For every cutting tool in Table 6, full factorial testing was conducted considering these testing parameters, with every of the resultant 36 combinations presented in Appendix E. A new drill bit was used for each of the 36 combinations to drill 3 holes within the same parameters, resulting in a total of 108 holes.

Table 8 - Full factorial experiment drilling parameters.

Feed (mm/rev)	Cutting Speed (m/min)
0.05	70
0.10	100
0.15	130

As some of the peak thrust force results obtained from the full factorial drilling campaign did not follow the previously identified trends, a peak thrust force outlier correction campaign was conducted, with the procedure and rationale followed presented in Appendix F.

For the drilling wear assessment campaign, a total of 950 holes (correspondent to a total 3800 mm of drilled distance along the drill's axis) were performed. A conventional twist drill with a MCD coating was selected, based on the most promising cutting parameters identified in the main experimental drilling campaign, as it will be thoroughly explained in subsection 4.2.4. The hole spacing was reduced to 10 mm, as illustrated in Figure 43, to maximize the amount of available material.

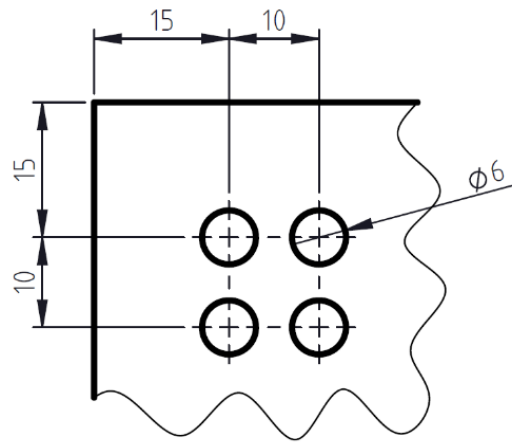


Figure 43 - Drilling wear campaign hole spacing.

3.3.3. Drilled Holes Analysis

3.3.3.1. Delamination

To access the delamination damage, the plates were firstly immersed into a methane di-iodine liquid bath for at least 30 minutes, which is a contrast liquid that penetrates the cracks inside the hole and allows the delamination to be observed. The images were captured by a Kodak RVG 5100 digital sensor, which is placed under the hole as it is highlighted in Figure 44a, after the X-ray emission from a SATELEC X MIND presented in Figure 44b. A Matlab script was then used to calculate the delamination factors from the obtained images. Figure 44c presents a schematic basic explanation of the Matlab script.

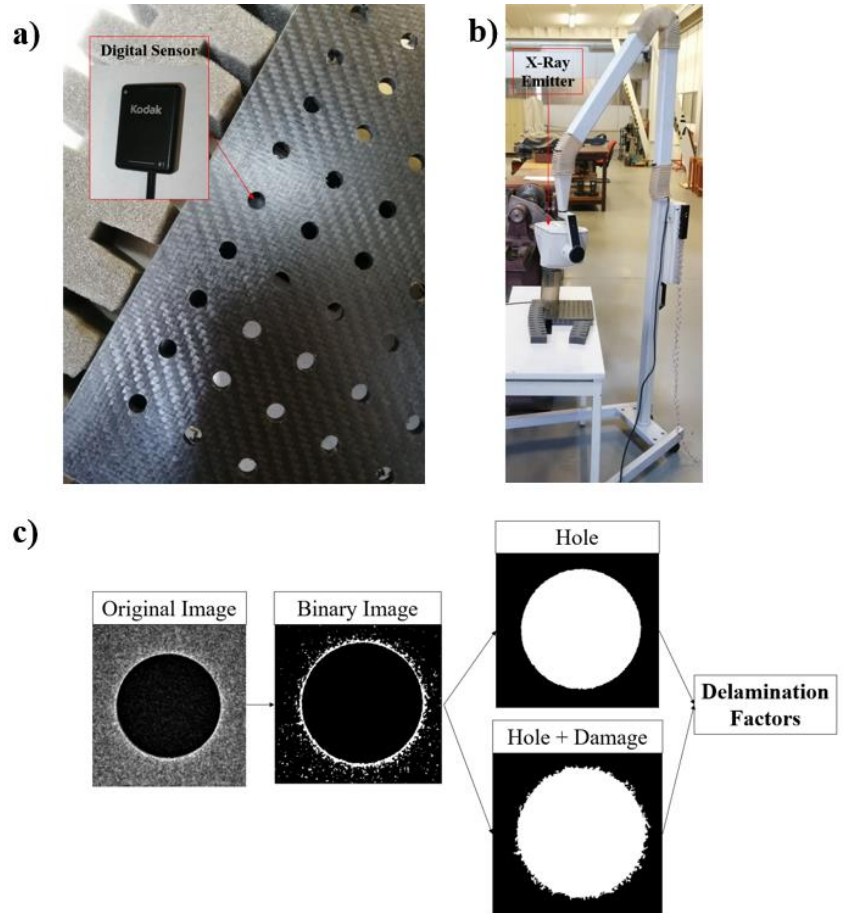


Figure 44 - Delamination assessment equipment and procedure: (a) Kodak RVG 5100 digital sensor placement; (b) SATELEC X MIND X-ray emitter; (c) Schematic of the basic functioning of the MATLAB delamination script.

This delamination analysis method is sensible to the threshold used when converting the X-ray image into a binary image. If the same threshold value was used, there would be an issue due to some X-ray images being brighter than others, as it can be seen by comparing Figure 45a and b. To compensate for this issue, the threshold value applied was varied for different situations. Also, the correct threshold level that correlated with the visible amount of delamination would sometimes result in a binary image where the contour of the hole was not complete, like in Figure 46a, which did not allow the MATLAB script to run correctly. In these situations, the binary image had to be adapted manually, by adding white pixels until a complete contour of the hole was achieved, as pictured in Figure 46b.

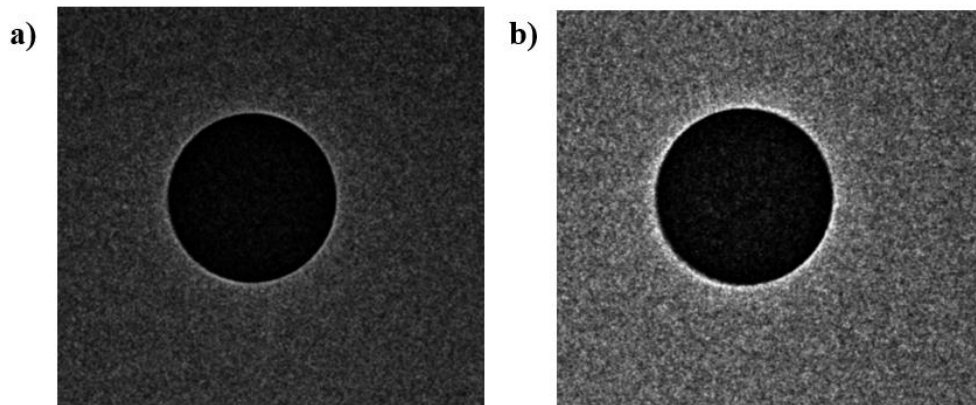


Figure 45 - X-ray of holes with distinct brightness levels performed with a conventional twist drill with MCD coating with a feed of 0.15 mm/rev: (a) example of hole with a darker X-ray, obtained at a cutting speed of 70 m/min; (b) example of hole with a lighter X-ray, obtained at a cutting speed of 100 m/min.

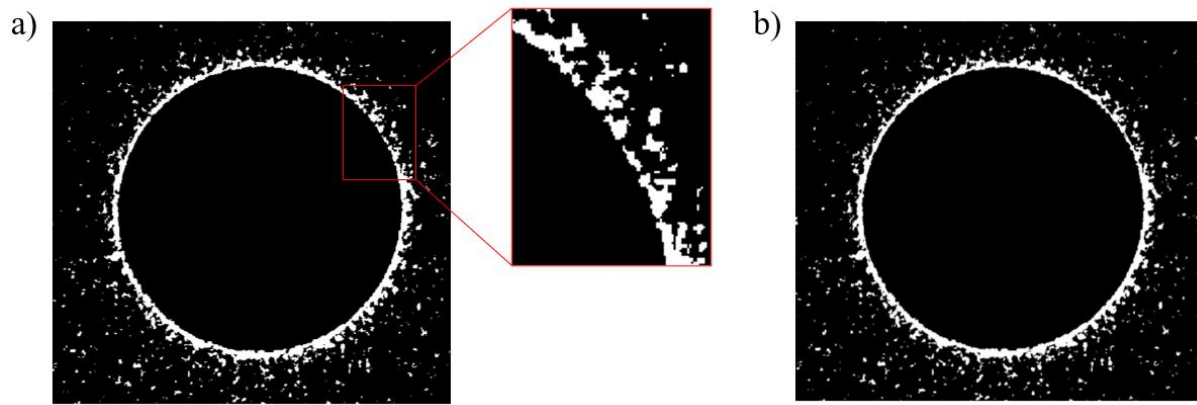


Figure 46 - Example of binary image of a hole with discontinuities in its contour, made with a conventional twist drill with MCD coating with a feed of 0.05 mm/rev and a cutting speed of 70 m/min: (a) highlight of some of the discontinuities; (b) hole after correction of the discontinuities.

3.3.3.2. Burr

The analysis of the presence of burr/uncut fibers in the drilled holes started with acquiring the pictures of each hole at the exit side of the plate, which was performed through optical microscopy. As observable through Figure 47, the used microscope was an Olympus SZH, with an MTV-3 camera adapter to which an image acquisition system from Leica was mounted.



Figure 47 - Microscope used for hole's pictures.

A qualitative scale was created with levels from 1 to 5, where 1 represents a low-quality hole, while 5 is attributed to a high-quality hole. Afterwards, a quality level was attributed to each hole.

3.3.3.3. Hole' Surface Roughness

The 3D measurement system ALICONA Infinite Focus SL (presented in Figure 48a) enabled the creation of 3D surfaces of the walls of the drilled holes. With this surface, a series of lines were drawn onto the surface at random (illustrated in Figure 48b), with a length of about 10 mm and a thickness of 2.5 μm , to replicate a rugosimeter's probe. The surface profile was obtained along this line, in which the Ra and Rz values were calculated. This process was repeated a total of three times for each hole.

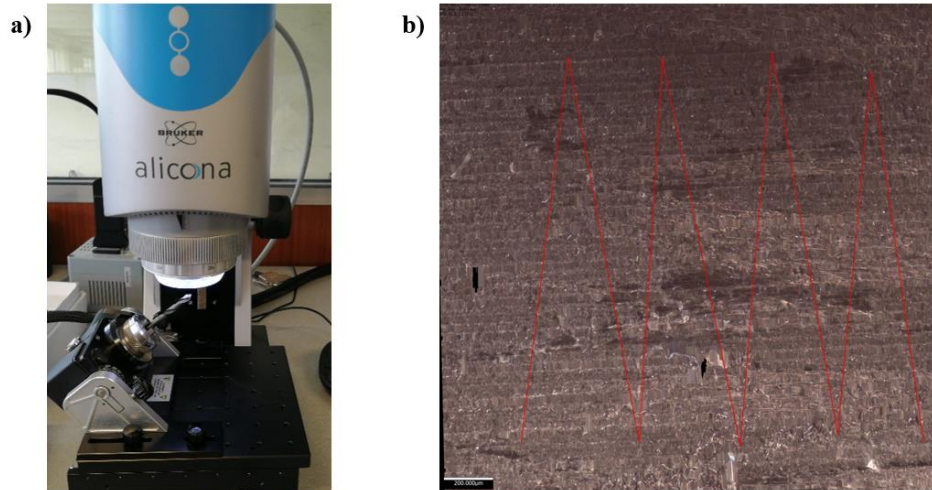


Figure 48 - Hole's roughness measurement equipment and procedure: (a) ALICONA Infinite Focus SL three-dimensional measurement system; (b) Example of a trajectory followed for the measurement, for a hole a conventional twist drill with NCD coating with a feed of 0.05 mm/rev and a cutting speed of 70 m/min.

3.3.3.4. Accuracy and Cylindricity

To measure hole's cylindricity and accuracy, a ZEISS DuraMax coordinate measurement system machine with a contact probe of 3 mm in diameter and 85 mm in length. The probe described a helicoidal trajectory inside the hole, through which it measured both parameters, as it is represented in Figure 49.

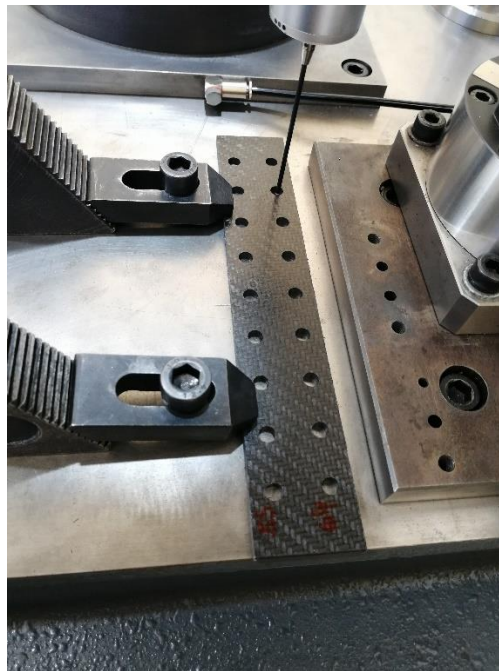


Figure 49 - Cilindricity and accuracy measurement using ZEISS DuraMax machine with contact probe.

3.3.4. Chip Analysis

The morphology of the machining chips was analyzed with pictures taken using a conventional camera, and at a higher magnification using the same microscope as for the burr analysis. The chips' thickness was also measured. Firstly, SEM images were captured, using Palbit SA's equipment. Afterwards, measurements were performed in 10 random locations along the chip using the Image J software (as exemplified through Figure 50), recording the

average value. This method was later employed to evaluate the coating delamination extent on the drill's SEM images in the wear campaign.

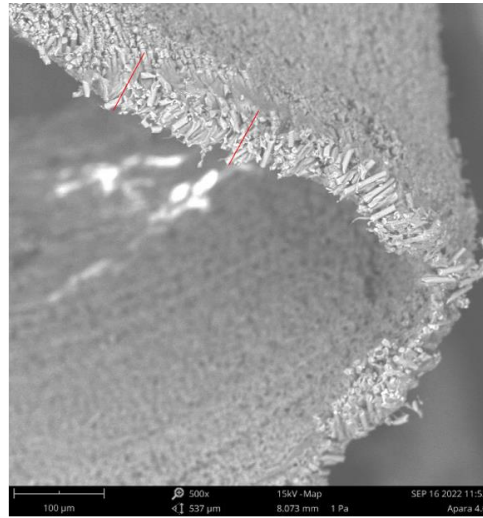


Figure 50 - Example of two chip thickness measurement location using the ImageJ software, for a chip obtained at 0.10 mm/rev and 70 m/min, using a conventional twist drill with a NCD coating.

3.3.5. Drilling Wear Analysis

To evaluate the wear, the drill's rake and flank faces were analyzed by obtaining images using a DinoLite AM-4013MZTL digital USB microscope (presented in Figure 51), which allowed image capturing through the DinoCapture 2.0 software. In each analysis, a picture of both rake faces and the flank face was captured. This analysis was conducted after different amounts of holes were drilled, with the points at which it was performed presented in Table 9. The number of holes between each analysis was increased, due to absence of wear signs, beginning with a difference of 10 holes (40 mm), then moving to 20 holes (80 mm), 30 holes (120 mm), 40 holes (160 mm), 50 holes (200 mm) and 100 holes (400 mm).



Figure 51 - DinoLite AM-4013MZTL digital USB microscope.

Table 9 - Wear analysis checkpoints

Hole Number	Total Drilled Distance (mm)
10	40
20	80
40	160
70	280
100	400
130	520
160	640
200	800
250	1000
300	1200
350	1400
450	1800
650	2600
750	3000
850	3400
950	3800

At 650 holes, corresponding to a drilling distance of 2600 mm, a SEM analysis of the drilling tool was performed. To serve as a point of comparison, a drilling tool with the same geometry and coating that had drilled 3 holes at 0.05 mm/rev and 70 m/min was also analyzed. Before the analysis, the drills were cleaned through an ultrasonic bath.

Chapter 4

Results Discussion and Analysis

In the present chapter, all the experimental results are presented and analyzed. It begins with the material's tensile, compressive and shear properties determination, that will be later used for the development of a FEA model. Afterwards, the experimental drilling results are considered, starting with the cutting loads and hole analysis, after which a chip analysis is performed. Finally, the results from the drilling wear campaign are exposed. Given the difficulty in obtaining good quality results for the material fracture energy, those results and different attempts towards their improvement are shown in Appendix B. Similarly, hole cylindricity results can be found in Appendix G.

4.1. Material Properties

4.1.1. Tensile Testing

Regarding tensile testing, the obtained stress-strain curves are presented in Figure 52.

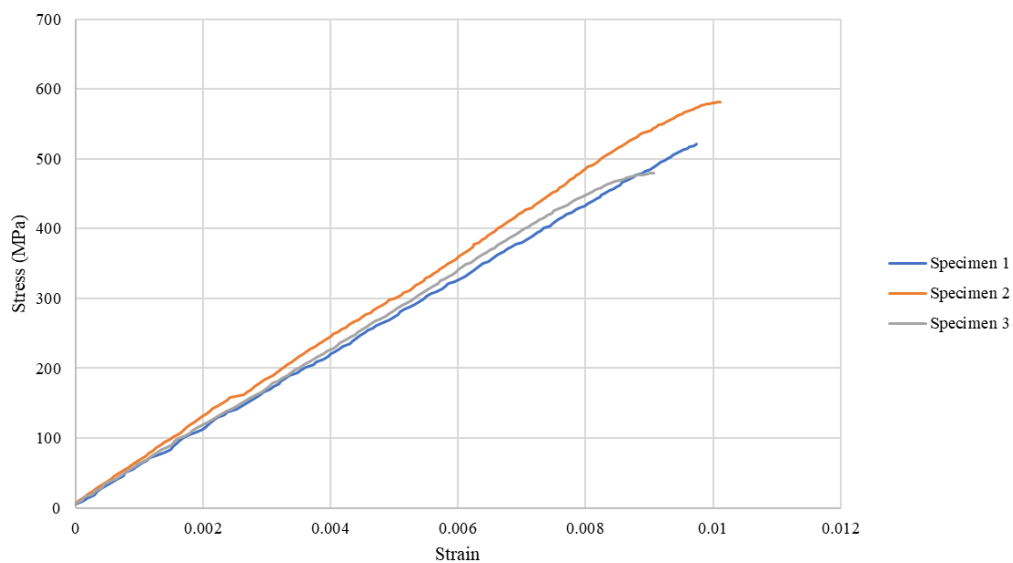


Figure 52 - Stress-strain curves obtained for tensile testing.

The presented curves exhibit a linear tendency until fracture, which is characteristic of a brittle material such as the one tested. Despite specimen slipping being sometimes observed,

the similarity between the curves seems to indicate that it did not have a significant impact upon the obtained results. For specimen 1, presented on Figure 53a and b, the failure occurred within the gage section, which according to ASTM D 3039 is considered a valid test. As for both specimens 2 and 3, their failure occurred between the spots where the specimen's gage section ends and the gripping of the material begins, as it is visible in Figure 53c in the case of specimen 2 and Figure 53e of specimen 3. This kind of failure usually invalidates the results in terms of the tensile strength, but does still allow for the calculation of both the Poisson's coefficient and the elastic modulus. This is because, as it was already mentioned, their tendency (exhibited by the respective stress-strain curves) follows specimen 1, proving these specimens successfully represent the mechanical behavior of the material. As it is visible in Figure 53d and Figure 53f, both specimen 2 and 3 presented a fracture in another spot besides the one which led to their failure, also in the place where the gage section ends and the gripping of the specimen begins. Regarding the tensile strength of the material, the obtained results are presented in Table 10, where specimen's 1 value is the most significant.

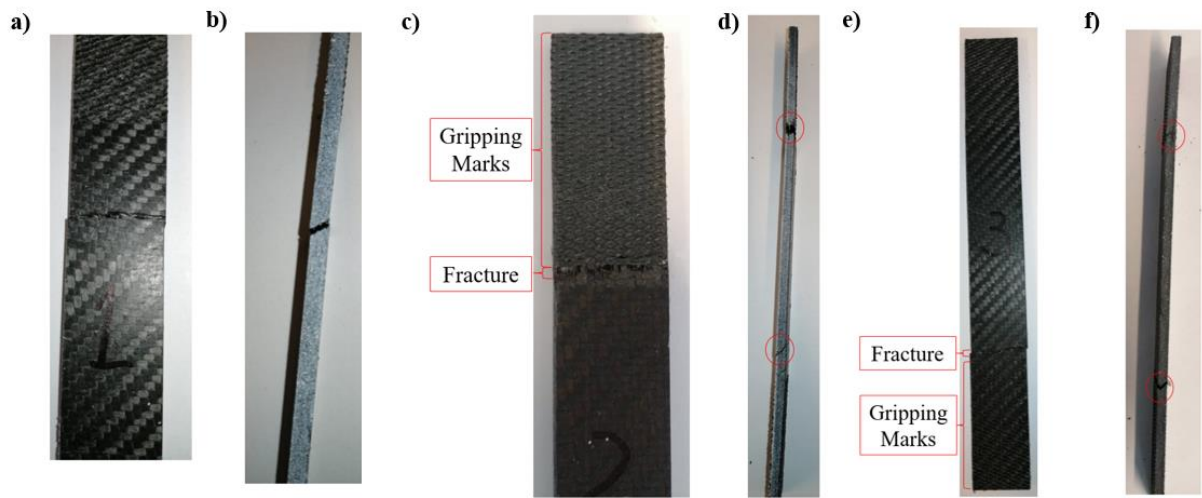


Figure 53 - Tensile specimens after fracturing: (a) specimen 1 front view; (b) specimen 1 side view; (c) specimen 2 front view; (d) specimen 2 side view; (e) specimen 3 front view; (f) specimen 3 side view.

Table 10 - Tensile strength of testing specimens.

Specimen	Tensile Strength (MPa)
1	520.76
2	581.26
3	479.09

As for the elasticity modulus, a linear regression was performed for each specimen considering all the recorded stress-strain points recorded until the material's fracture, where the intersection point between the regression line and the axis was set to the origin of the referential. The slope of the obtained line is correspondent to the elasticity modulus of the material. On Table 11, the slope values for each specimen are presented, as well as the R^2 for each regression, which is always above 0.9990. From these results, an average elasticity modulus value of 56.827 GPa is obtained.

Table 11 - Slope and R2 values for the tensile stress-strain curves.

Specimen	Slope (GPa)	R ²
1	54.387	0.9996
2	60.007	0.9995
3	56.094	0.9998

The Poisson's coefficient values were calculated considering a starting point at a longitudinal strain of around 0.003, and the ending point at a longitudinal strain of around 0.009. Within this range, the coefficient was calculated for each data point, and the average resultant value was considered, which are presented on Table 10. Except for specimen 3, the values are similar and, in general, consistent. With these results, an average Poisson's coefficient value of 0.1085 is obtained.

Table 12 - Poisson's coefficient of each tensile specimen.

Specimen	Poisson's Coefficient
1	0.1179
2	0.1191
3	0.0884

4.1.2.Compressive Testing

The obtained stress-strain curves for the compressive testing are presented in Figure 54, between the beginning of the loading process and the specimen's fracture at peak stress.

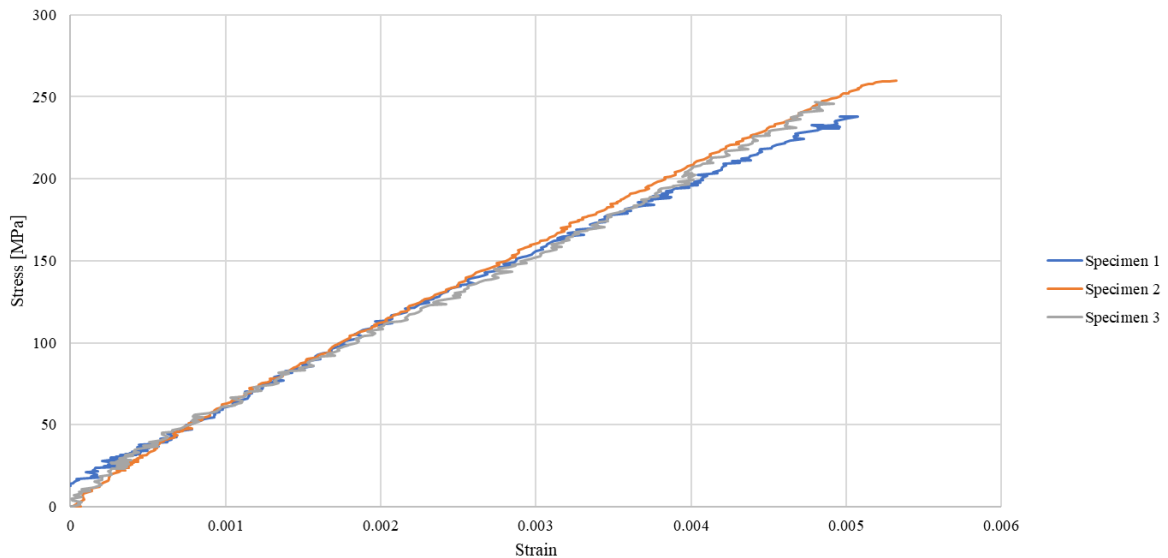


Figure 54 - Stress-strain curves obtained from compressive testing.

All the testing specimens presented an acceptable failure mode, with a through-thickness fracture in the respective gage section, as it is observable through Figure 55.

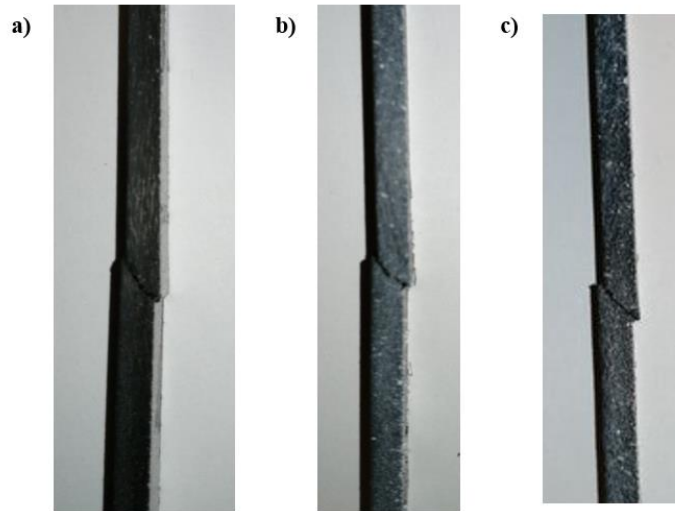


Figure 55 - Compressive specimens after fracturing: (a) specimen 1; (b) specimen 2; (c) specimen 3.

Considering the peak thrust force value, it was possible to calculate the compressive strength of each specimen, as it is presented on Table 13. The obtained average compressive strength value is 248.35 MPa.

Table 13 - Compressive strength of testing specimens.

Specimen	Compressive Strength (MPa)
1	238.08
2	260.12
3	246.86

With the stress-strain curves, it is possible to determine the compressive modulus. For this, a similar procedure to the one used for the determination of the elasticity modulus was applied, with the results presented in Table 14. An average compressive modulus value of 51.241 GPa was obtained.

Table 14 - Slope and R2 values for the compressive stress-strain curves.

Specimen	Slope (GPa)	R ²
1	49.984	0.9838
2	52.377	0.9978
3	51.362	0.9975

4.1.3. Shear Testing

With the DIC data, and calculating the respective stress, it was possible to obtain the stress-strain curve of the shear specimens, which are presented in Figure 56. This curve is limited at 0.05 shear strain, as specified by the testing standard.

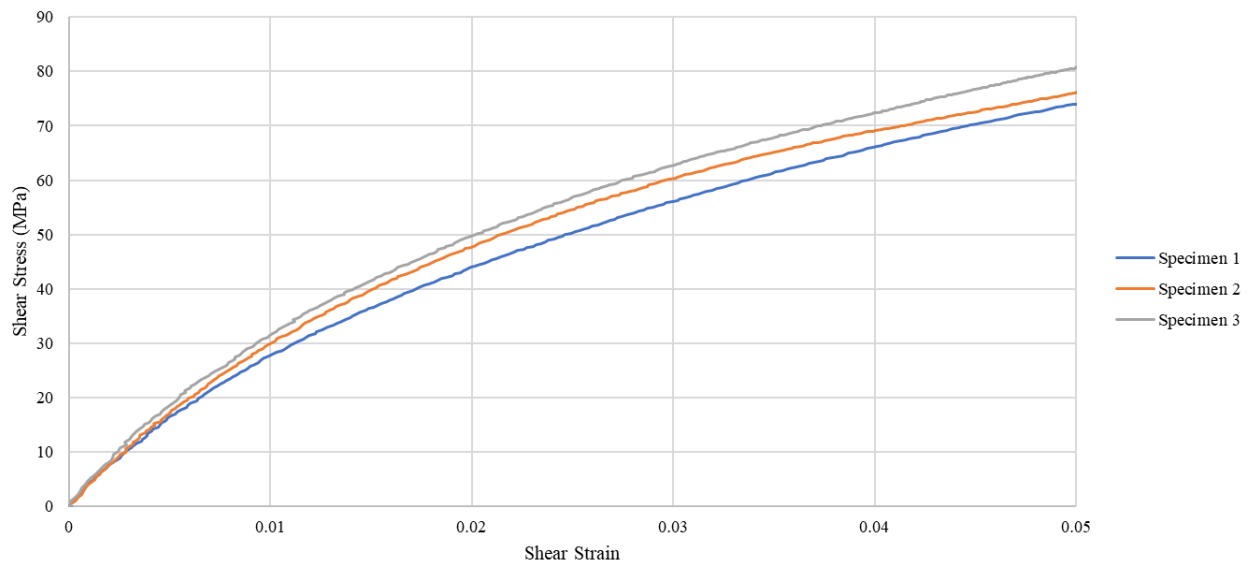


Figure 56 - Stress-strain curves obtained from shear testing.

In terms of failure, all specimens fractured within their gage section, as it is presented in Figure 57. The shear strength obtained for the specimens is presented in Table 15. With these values, an average shear strength of 76.97 MPa is obtained.

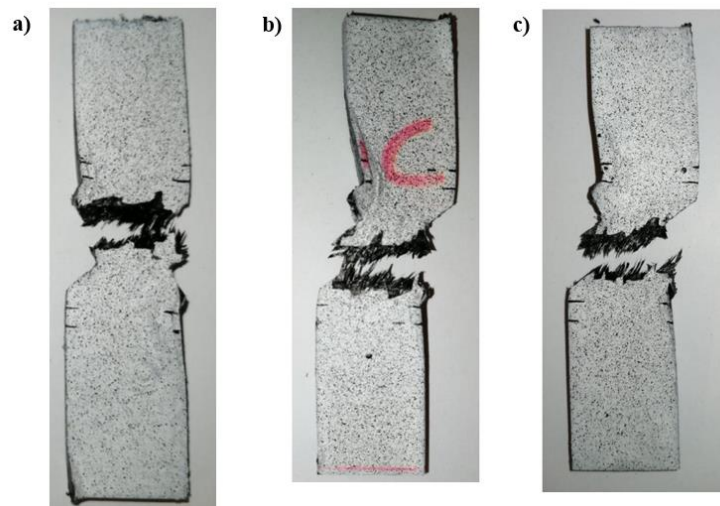


Figure 57 - Shear testing specimens after fracture: (a) specimen 1; (b) specimen 2; (c) specimen 3.

Table 15 - Shear strength of testing specimens.

Specimen	Shear Strength (MPa)
1	74.01
2	76.15
3	80.76

As specified by the testing standard, the shear modulus calculation was performed considering a shear strain difference of 0.004, beginning at around 0.0015. Within this range, a linear regression was performed for each of the specimens, forcing each curve to intersect the axis at its origin, and the resultant shear modulus are presented in Table 16 along with the respective R^2 values. An average shear modulus of 3.579 GPa is obtained.

Table 16 - Slope and R^2 values for shear stress-strain curves.

Specimen	Slope (GPa)	R^2
1	3.350	0.9982
2	3.540	0.9988
3	3.847	0.9975

In all the shear testing specimens, excessive deformation promoted by the testing fixture has occurred, as it can be observed in Figure 58. This observation is not reported within the testing standard as an invalidating occurrence. However, it most likely results in the testing specimen not being under a pure shear loading condition, being detrimental to the obtained results. This is supported by the stress-strain curves of Figure 56 presenting an increasingly nonlinearity with an increase in the shear strain, particularly at higher strain values. However, this nonlinearity is not observed at lower strain values, as all R^2 values presented in Table 16 are above 0.9975, meaning that the specimen's response between a shear strain of around 0.0015 to 0.0055 is close to linear. Thereby, it can be validated that at the lower strain values, the material still most likely is under an almost pure shear loading condition.

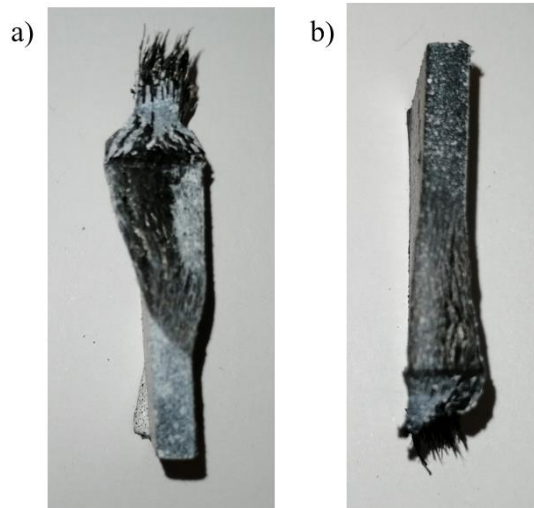


Figure 58 - Two halves of the shear specimen 1 highlighting material's severe deformation.

4.1.4. Euler Buckling Condition Verification

With the obtained values, it was also possible to verify the Euler column buckling stress relation expressed in ASTM D 6641, presented in subsection 3.2.2 equation 3.4. To verify the worst-case scenario, the considered values were the minimal for the shear modulus and the elasticity modulus, and the highest compressive strength. With these, a minimum thickness of 0.991 mm was obtained, and considering that the thinnest compressive specimen had 4.081 mm, this condition was fulfilled.

4.2. Instrumented Drilling Tests

4.2.1. Cutting Loads Analysis

4.2.1.1. Torque and Thrust Force Signals

Three drilling stages can be identified in the thrust force (F_z) evolution of Figure 59. In a first stage (up to instant A), there is an increasing trend in thrust force as the drill's point

enters the material. Afterwards, the thrust force seems to stabilize around a constant value between A and C, corresponding to the full engagement of the cutting edges on the drilling process. As the drill's point exits the material between C and D, the force exhibits a decreasing trend as there is only contact between the material and the drill's margin. The identified drilling stages are in accordance with literature [130]. When comparing the force and torque (M_z in Figure 59) signals among each other, it becomes apparent that the torque signal is still rising in A (where the force signal has already peaked and started to stabilize in value), only stabilizing in B. The observed difference is most likely because at instant A the drilling tool's point has already fully engaged the material, but the secondary cutting edges only fully contact the material at instant B. Additionally, until instant A, the cutting forces are lower and concentrated in a smaller diameter of the drilling tool, leading to lower torque values. The force signal begins to decrease in C, earlier than the torque signal, and when the drill's point has exited the material at D, the torque signal is still decreasing until instant E. This is because even though the drill's point exits the material between C and D, the secondary edges are still fully engaged until E. These observations are in accordance with Karpat et al.'s [114] work and mentioned by Ahmad [15]. All these instants are pictured in Figure 60.

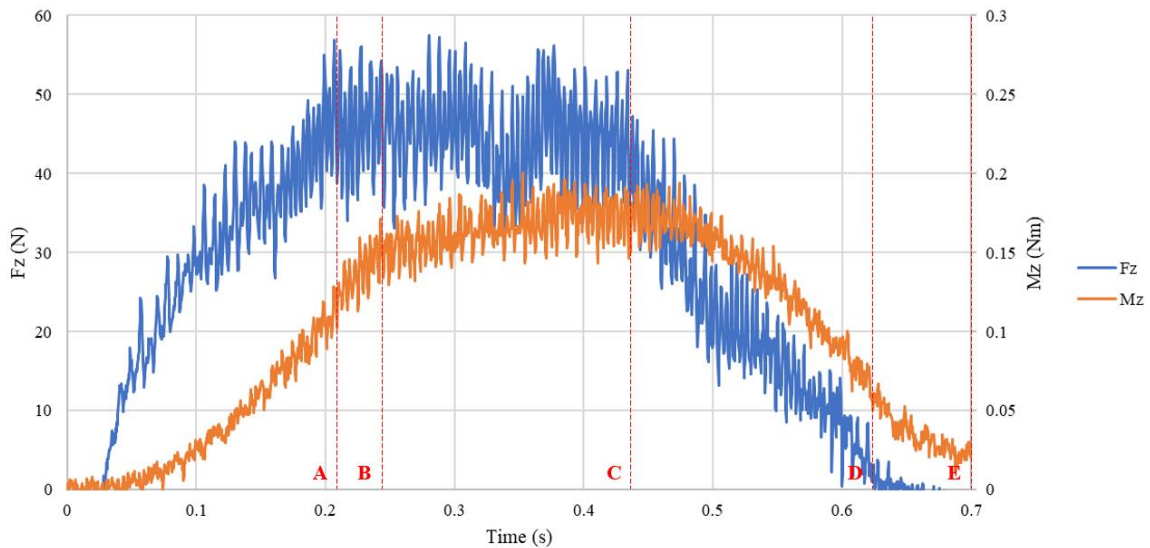


Figure 59 - Thrust force and torque signals of a hole drilled with a conventional twist drill with NCD coating, at a feed of 0.15 mm/rev and a cutting speed of 70 m/min.

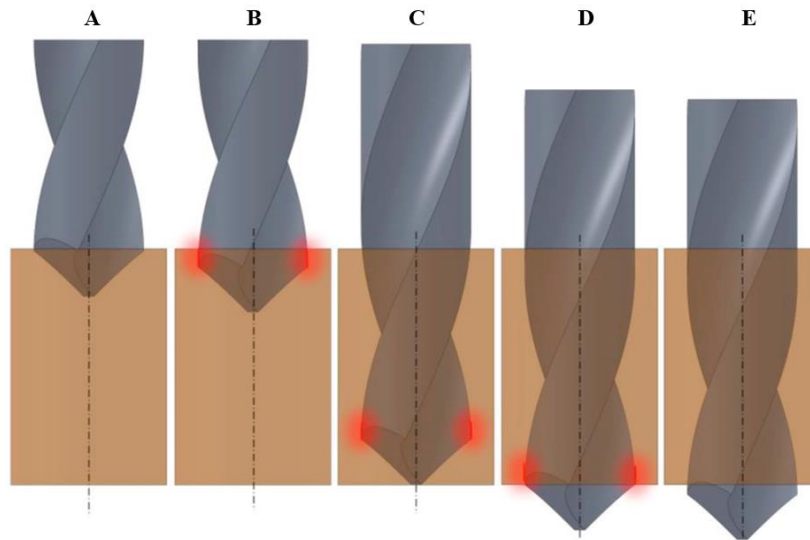


Figure 60 - Different instants of the drilling process (adapted from [114]).

4.2.1.2. Peak Thrust Force

The peak thrust force was obtained for all the 108 holes. When considering the 3 holes made with the same drilling tool and the same parameters, its average was calculated along with the respective standard deviation.

The ANOVA results correspondent to each drilling tool geometry with a respective coating are presented in Table 17, where V_c represents the cutting speed, f represents the feed and $f * V_c$ is the interaction between the two parameters.

Table 17 - ANOVA of peak thrust force for each drilling tool.

Drilling tool	Source of variation	Sum of Squares	DF	Mean Squares	F-value	P-value	Contribution
Conventional MCD	f	581.657	2	290.829	35.457	5.72E-07	43.16%
	V_c	326.430	2	163.215	19.896	2.76E-05	24.22%
	$f * V_c$	292.027	4	73.007	8.899	3.79E-04	21.67%
	Error	147.664	18	8.204	-	-	10.96%
	Total	1347.777	26	-	-	-	-
Conventional NCD	f	2538.780	2	1269.390	127.912	2.29E-11	78.72%
	V_c	269.447	2	134.724	13.576	2.54E-04	8.35%
	$f * V_c$	238.406	4	59.602	6.006	2.98E-03	7.39%
	Error	178.631	18	9.924	-	-	5.54%
	Total	3225.265	26	-	-	-	-
Double-Point MCD	f	927.147	2	463.574	45.492	9.15E-08	78.89%
	V_c	33.160	2	16.580	1.627	2.24E-01	2.82%
	$f * V_c$	31.517	4	7.879	0.773	5.57E-01	2.68%
	Error	183.425	18	10.190	-	-	15.61%
	Total	1175.250	26	-	-	-	-
Double-Point NCD	f	150.937	2	75.469	17.306	6.42E-05	7.31%
	V_c	1492.362	2	746.181	171.110	1.94E-12	72.29%
	$f * V_c$	342.484	4	85.621	19.634	2.27E-06	16.59%
	Error	78.495	18	4.361	-	-	3.80%
	Total	2064.279	26	-	-	-	-

From the results it is possible to observe that for every drilling tool except the double-point angle twist drill with NCD coating, the feed is the parameter with the most influence on the peak thrust force. Also, for every drill except the double-point angle twist drill with a MCD coating, the feed, cutting speed, and interaction between these two parameters were statistically significant, remaining below the critical P-value of 0.05. The cases in which the feed presents a lower contribution (conventional twist drill with MCD coating and double-point angle twist drill with NCD coating) correspond to the higher interaction values. A high interaction means that the independent variation of each parameter does not always produce the same effects. For example, a high interaction value between feed and cutting speed means varying from a low to a high feed will have a different impact on the

thrust force depending on the cutting speed. Another ANOVA was performed, considering the coating and tool geometry as variables (apart from the previously defined variables). The critical P-value was considered at 0.05, and the obtained results are in Table 18.

Table 18 - ANOVA of peak thrust force (including geometry and coating as variables).

Source of variation	Sum of Squares	DF	Mean Squares	F-value	P-value	Contribution
Geometry	158.598	1	158.598	19.413	0.000	1.46%
Coating	2885.947	1	2885.947	353.252	0.000	26.50%
V_c	418.444	2	209.222	25.610	0.000	3.84%
f	2889.971	2	1444.985	176.872	0.000	26.54%
Geometry * Coating	33.045	1	33.045	4.045	0.048	0.30%
Geometry * V_c	197.199	2	98.600	12.069	0.000	1.81%
Geometry * f	522.945	2	261.473	32.005	0.000	4.80%
Coating * V_c	841.231	2	420.615	51.485	0.000	7.72%
Coating * f	38.614	2	19.307	2.363	0.101	0.35%
V_c * f	67.875	4	16.969	2.077	0.093	0.62%
Geometry * Coating * V_c	664.525	2	332.262	40.670	0.000	6.10%
Geometry * Coating * f	746.992	2	373.496	45.718	0.000	6.86%
Geometry * V_c * f	222.466	4	55.617	6.808	0.000	2.04%
Coating * V_c * f	209.829	4	52.457	6.421	0.000	1.93%
Geometry * Coating * V_c * f	404.264	4	101.066	12.371	0.000	3.71%
Error	588.215	72	8.170	-	-	5.40%
Total	10890.161	107	-	-	-	-

From the presented analysis, it is possible to verify that, between the two cutting parameters, the feed is the most influential in the peak thrust force, which goes in accordance with literature [25, 36, 78]. Regarding the drilling tools, the coating was found to be much more influential than the geometry, which is most likely associated with the coating having an impact onto the drill's CER. It should be mentioned, however, that in this analysis some interactions are quite significant, particularly the one between the tool's coating and the cutting speed. Looking at the graphical representation of the estimated marginal means for both variables (presented in Figure 61), the interaction between the parameters is visible, as the curves are not parallel. It also seems that the NCD coating leads to higher thrust forces than its MCD counterpart, which is probably due to the NCD coated drills having a higher CER than the MCD tools, as it was referred in subsection 3.3.1.1. This means that they are not as sharp, thereby imposing a challenge to the fiber cutting process, thus increasing the thrust force, which goes in accordance with literature results [25, 79, 82]. Also, it seems that higher cutting speeds result in lower thrust forces, which is probably related with the higher cutting temperatures, as the matrix tends to soften. The first segment of both lines between 70 m/min and 100 m/min present a similar downwards tendency, with practically no interaction. However, when reaching the point of 130 m/min, there is a shift in the tendency of the NCD coating, where it starts to increase upwards, in contrast with the MCD coating. This difference could be due to the different CER of both coatings, where with the NCD coating, the effect of the higher temperatures at the higher cutting speeds is superseded by the effect of a blunter cutting edge. Franke [82] claims that this could be due to an increase in friction by the drill with the higher CER, thereby increasing the thrust force.

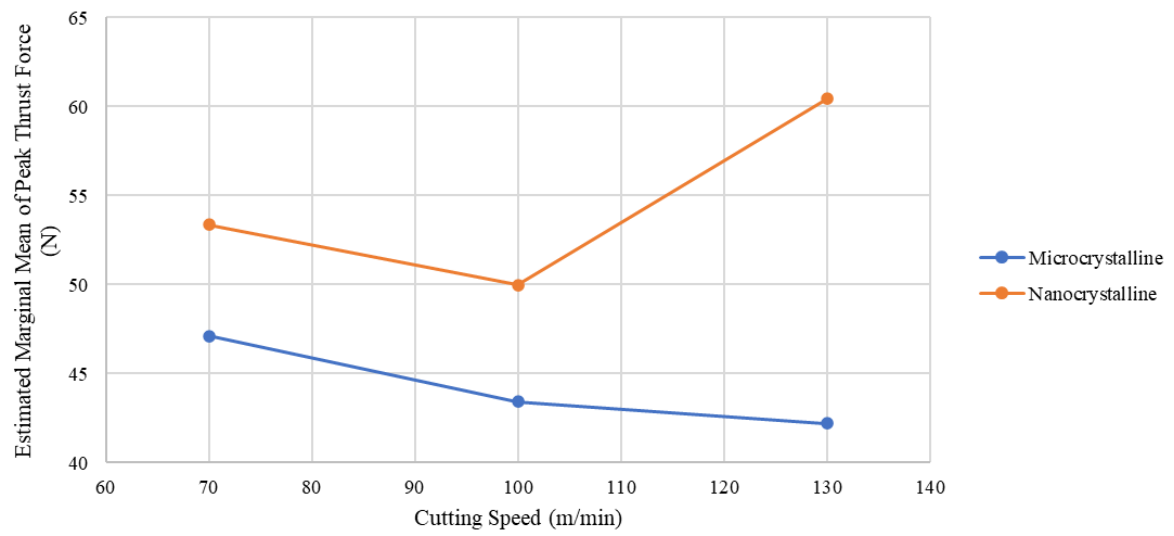


Figure 61 - Profile plot between coating and cutting speed.

In Figure 62 and Figure 63, graphs of the peak thrust force for each drilling tool at various cutting speeds and feeds are presented.

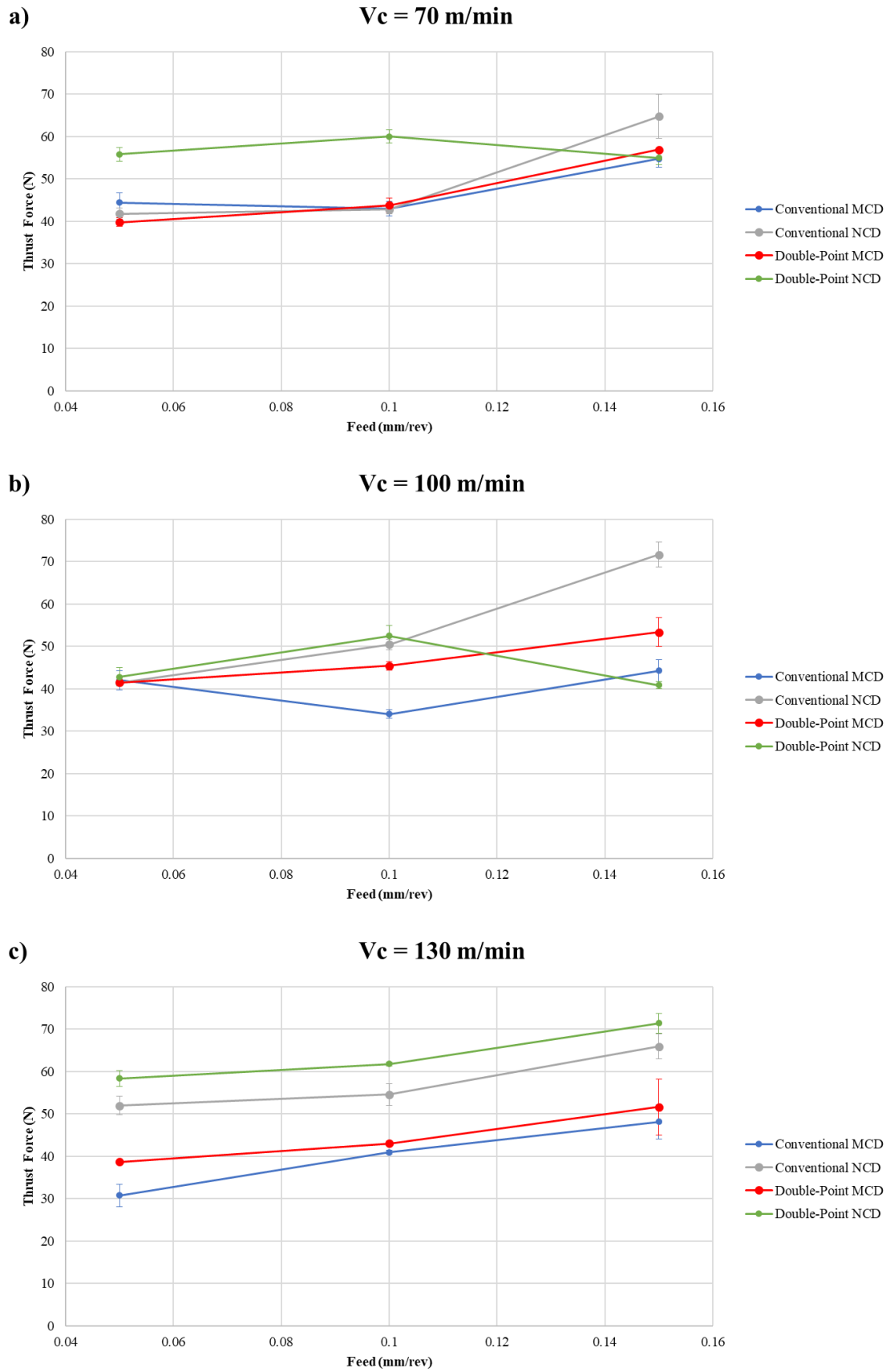


Figure 62 - Peak thrust force for each drill at a certain cutting speed for various feeds: (a) Cutting speed of 70 m/min; (b) Cutting speed of 100 m/min; (c) Cutting speed of 130 m/min.

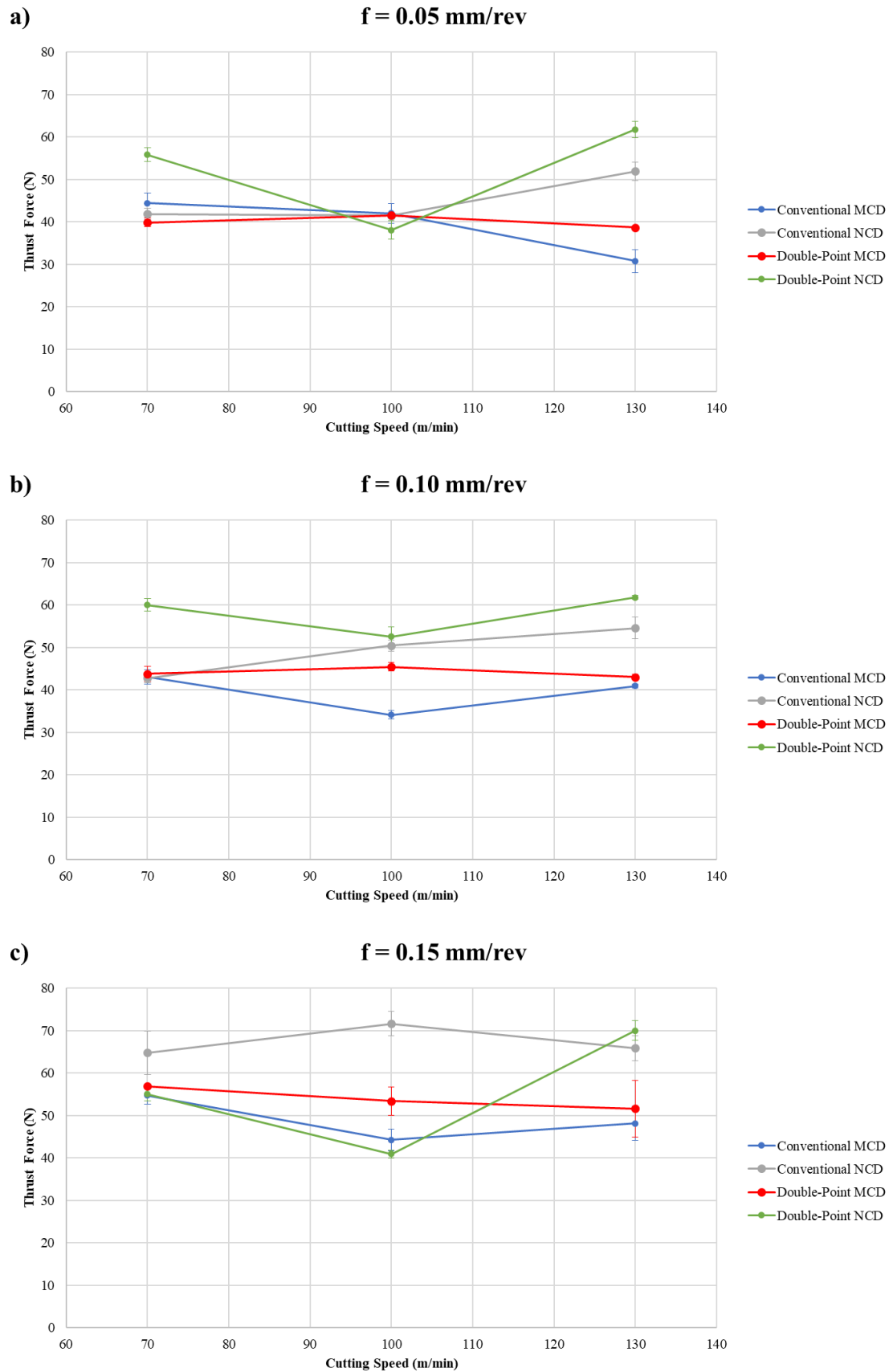


Figure 63 - Peak thrust force for each drill at a certain feed for various cutting speeds: (a) feed of 0.05 mm/rev; (b) feed of 0.10 mm/rev; (c) feed of 0.15 mm/rev.

From the graphs of Figure 62 it is possible to understand that in general, regardless of the drilling tool, with an increase in feed there is an increase in the peak thrust force. This

result is in accordance with several literature examples [25, 38, 116, 117] and may be explained by the increase in uncut chip thickness at higher feeds, as Ahmad [15] refers. From Figure 63, it is confirmed that the cutting speed does not have as great of an influence as the feed towards the peak thrust force. For the same cutting parameters, it seems that the drills with NCD coatings result in higher thrust forces than the ones with MCD coatings. This difference between the coatings is more evident when considering higher cutting speeds of 100 m/min and 130 m/min, presented in Figure 62b and Figure 62c, respectively. The reason behind this is most likely the difference in CER between the coatings, as it was already exposed. Another interesting observation, noticed for the higher cutting speed of 130 m/min, is that the conventional twist drills lead to lower thrust forces than their double-point angle counterparts with the same coating, but different cutting parameters combinations lead to distinct observations in this regard.

4.2.1.3. Peak Drilling Torque

The peak drilling torque was obtained similarly to the peak thrust force. An ANOVA considering all variables and drilling tools was also conducted, and its results are presented in Table 19.

Table 19 - ANOVA of peak drilling torque.

Source of variation	Sum of Squares	DF	Mean Squares	F-value	P-value	Contribution
Geometry	0.0039	1	2.4091	42.428	0.000	2.00%
Coating	0.0006	1	0.0039	6.396	0.014	0.30%
f	0.1690	2	0.0006	921.420	0.000	86.96%
V_c	0.0019	2	0.0845	10.326	0.000	0.97%
Geometry * Coating	0.0001	1	0.0009	0.831	0.365	0.04%
Geometry * f	0.0012	2	0.0001	6.605	0.002	0.62%
Geometry * V_c	0.0003	2	0.0006	1.756	0.180	0.17%
Coating * f	0.0001	2	0.0002	0.670	0.515	0.06%
Coating * V_c	0.0014	2	0.0001	7.489	0.001	0.71%
f * V_c	0.0022	4	0.0007	6.129	0.000	1.16%
Geometry * Coating * f	0.0016	2	0.0006	8.858	0.000	0.84%
Geometry * Coating * V_c	0.0032	2	0.0008	17.256	0.000	1.63%
Geometry * f * V_c	0.0006	4	0.0016	1.727	0.153	0.33%
Coating * f * V_c	0.0003	4	0.0002	0.717	0.583	0.14%
Geometry * Coating * f * V_c	0.0013	4	0.0001	3.633	0.009	0.69%
Error	0.0066	72	0.0003	-	-	3.40%
Total	0.1944	107	-	-	-	-

It is possible to conclude that the feed is by far the most influential parameter towards the drilling torque. The cutting speed as well as drill geometry and coating have negligible influence. This analysis follows the same trend as the one performed by Feito et al. [36]. In Figure 64 and Figure 65 the peak drilling torque for each drilling tool at a certain feed and cutting speed are presented.

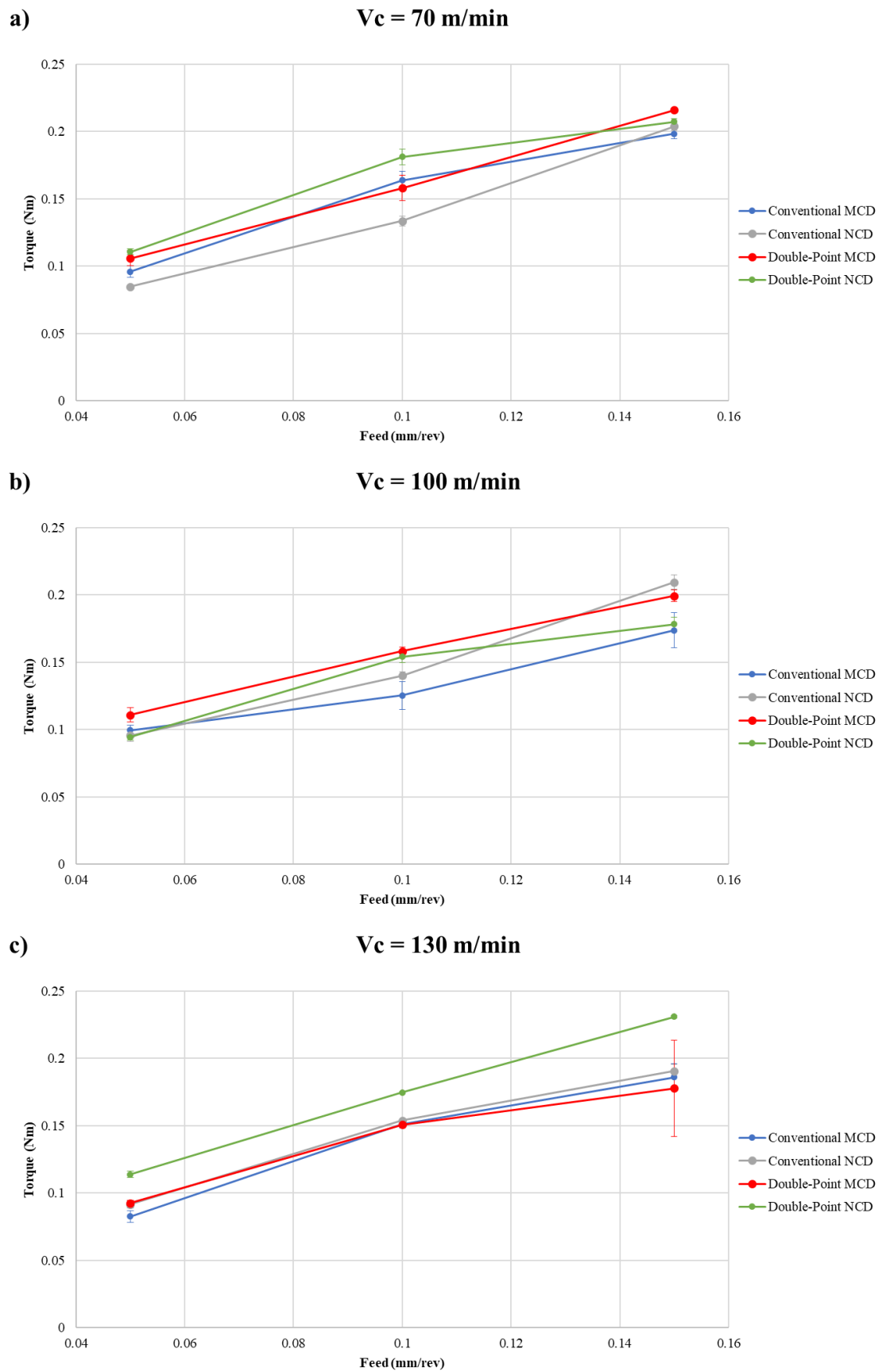


Figure 64 - Peak drilling torque for each drill at a certain cutting speed for various feeds: (a) Cutting speed of 70 m/min; (b) Cutting speed of 100 m/min; (c) Cutting speed of 130 m/min.

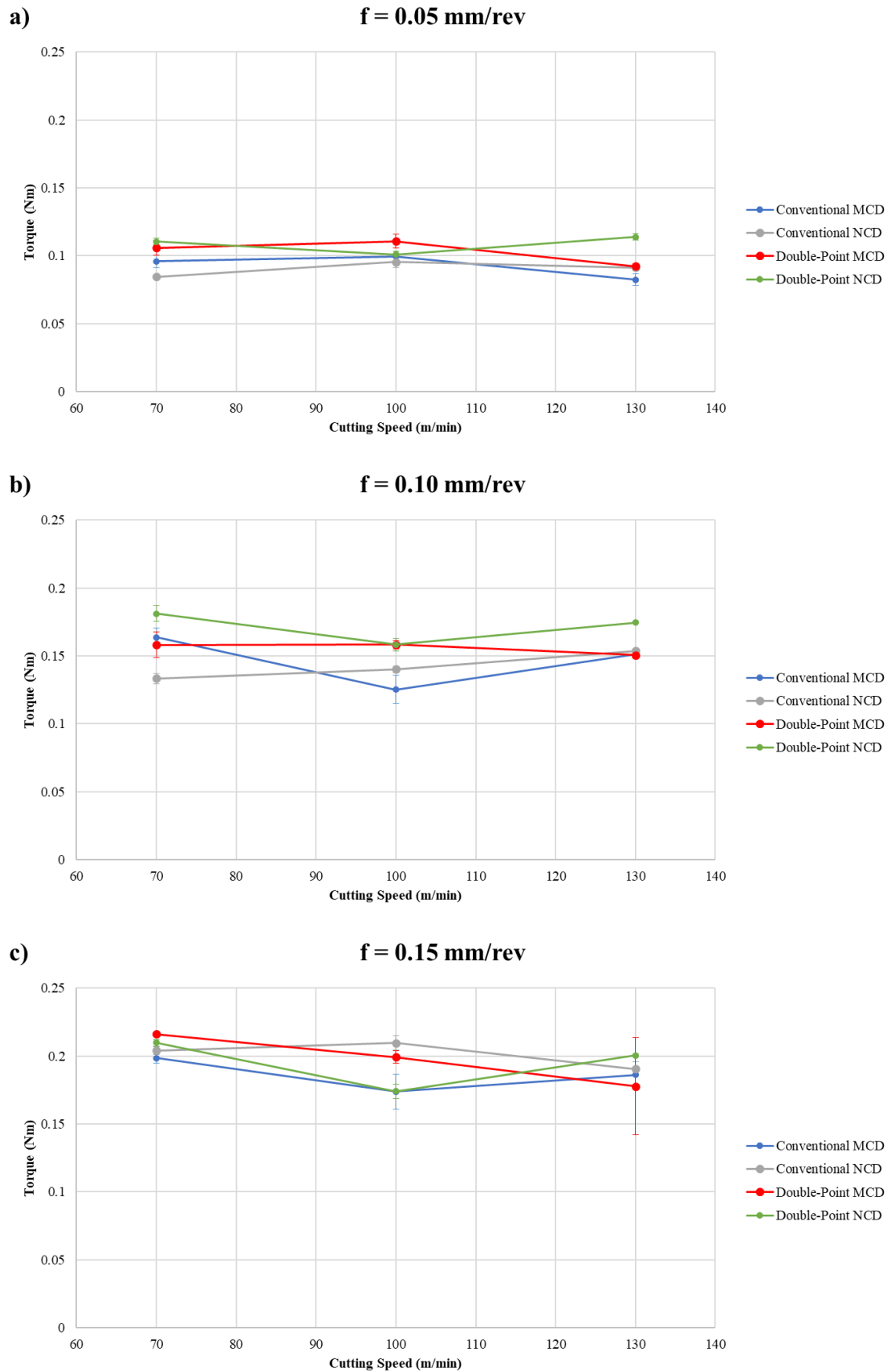


Figure 65 - Peak drilling torque for each drill at a certain feed for various cutting speeds: (a) feed of 0.05 mm/rev; (b) feed of 0.10 mm/rev; (c) feed of 0.15 mm/rev.

As it can be seen through Figure 64, the higher the feed, the higher the drilling torque for every cutting speed. As Ahmad [15] claimed, this is related with the increase in uncut chip

thickness at higher feeds. Figure 65 reinforces the observations from the ANOVA: the cutting speed has only a very slight influence on the drilling torque, particularly when compared to the influence of the feed, with the presented trendlines being almost parallel with the horizontal axis.

4.2.2. Drilled Hole Analysis

4.2.2.1. Delamination

Upon inspection of most of the obtained X-ray delamination images, it was observed that they presented little to no delamination, as illustrated by Figure 66a, and that it appeared uniformly around the hole. Some holes seemed to present severe delamination, like the one shown in Figure 66b. However, upon further inspection, it was concluded that the lines observed were, most likely, resultant from the manufacturing process of the plates, as they were also appearing on zones of the plate that did not have any drilled holes, like the plate's borders. The concentration of these lines around the hole was probably due to the hole presenting an entry point for the contrast liquid used in the X-ray. With this consideration, it should be mentioned that when calculating the delamination factors, these lines were disregarded so that the computed factors are not overestimated.

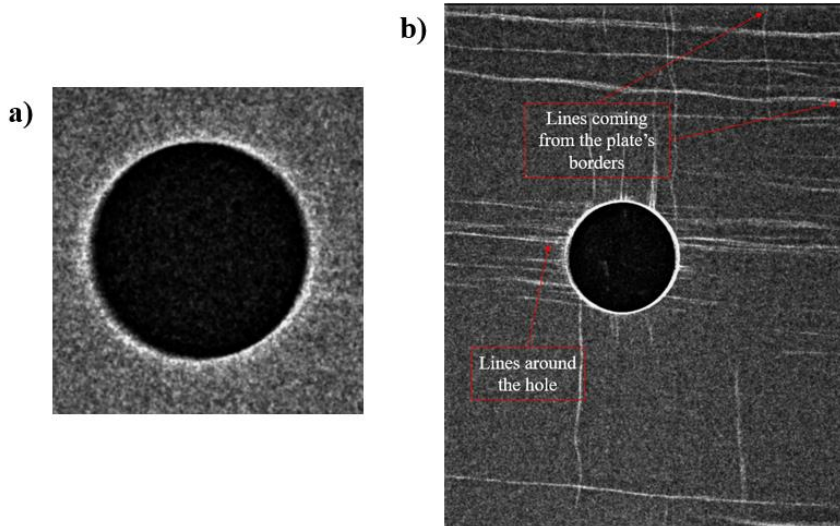


Figure 66 - X-ray images: (a) delaminated hole, performed with a double-point angle twist drill with a MCD coating at 0.15 mm/rev and 100 m/min; (b) highlight of possible plate defects, around a hole drilled with a conventional twist drill with a NCD coating at 0.05 mm/rev and 70 m/min.

The present delamination assessment will focus on the F_{da} value, for which an ANOVA is presented in Table 20.

Table 20 - ANOVA analysis for delamination using F_{da} criterion.

Source of variation	Sum of Squares	DF	Mean Squares	F-value	P-value	Contribution
Geometry	0.005	1	0.005	8.219	0.008	9.22%
Coating	0.000	1	0.000	0.166	0.687	0.19%
V_c	0.013	2	0.007	10.438	0.000	23.42%
f	0.020	2	0.010	15.437	0.000	34.64%
Error	0.019	29	0.001	-	-	32.53%
Total	0.057	35	-	-	-	-

From these results it is possible to understand that the cutting parameters have a more significant influence than the drilling tool. Within the cutting parameters, it was found that the

feed had a more significant effect than the cutting speed. As for the drilling tool, the geometry proved to be of more importance than the coating, which has a p-value above the critical value of 0.05, thereby being considered statistically insignificant. The high influence of the feed into the delamination factor is both in accordance with its high influence in the peak thrust force and with several literature results [32, 36, 38, 78, 116, 117]. It should be mentioned that in this analysis, the error has a significant influence on the results when compared with the other sources of variation. In Figure 67 and Figure 68, the graphs correspondent to the delamination factor for each drill at various cutting speed and feed levels are presented.

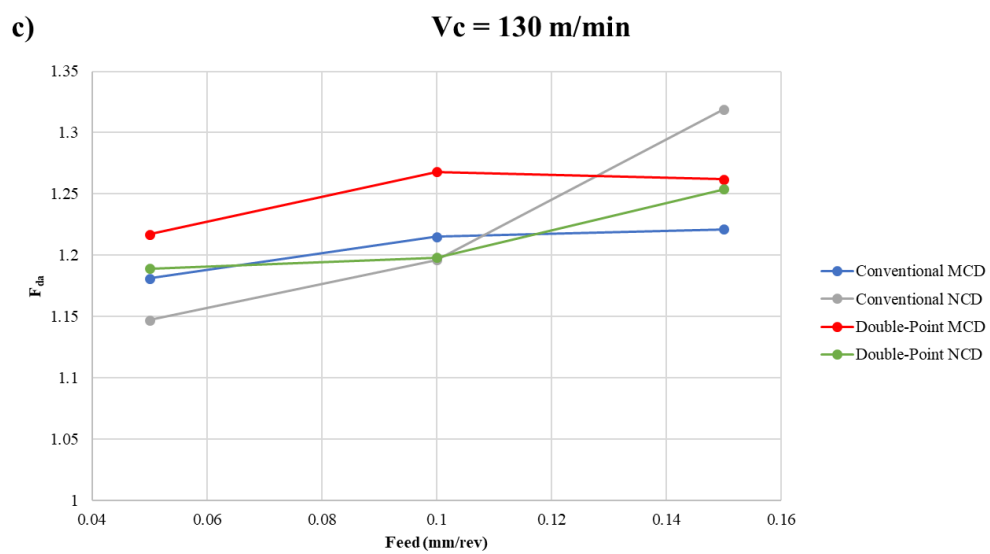
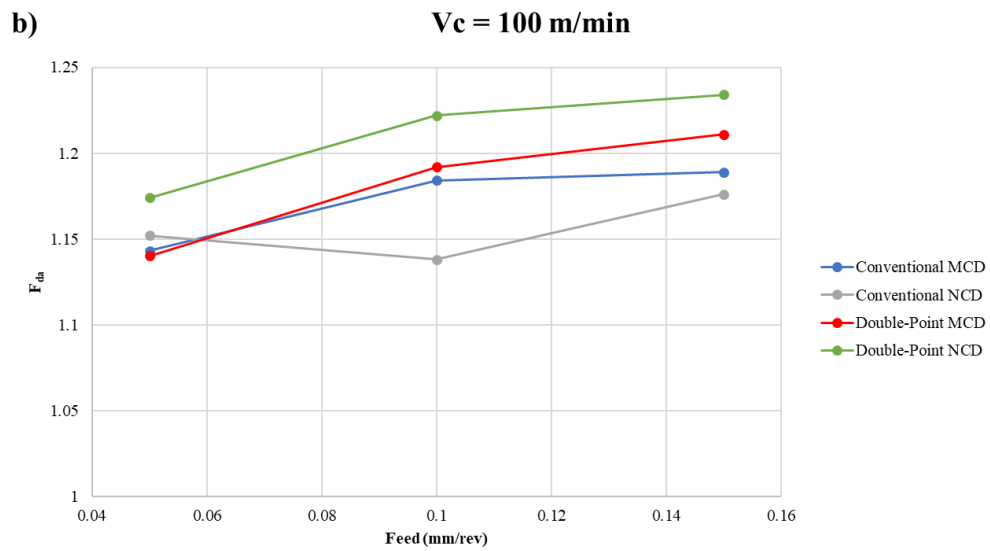
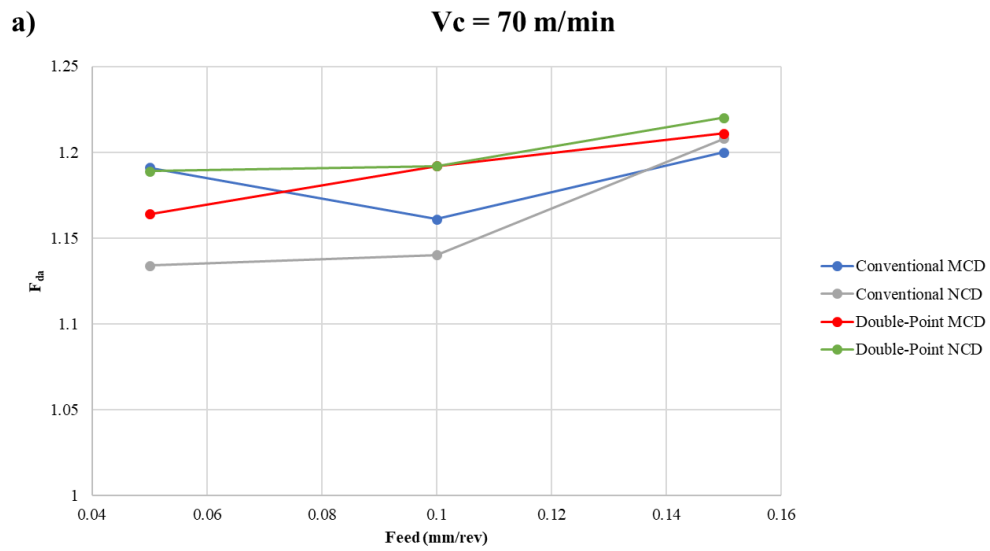


Figure 67 - Adjusted delamination factors for each combination of drill geometry and coating at various feed values: (a) cutting speed of 70 m/min; (b) cutting speed of 100 m/min; (c) cutting speed of 130 m/min.

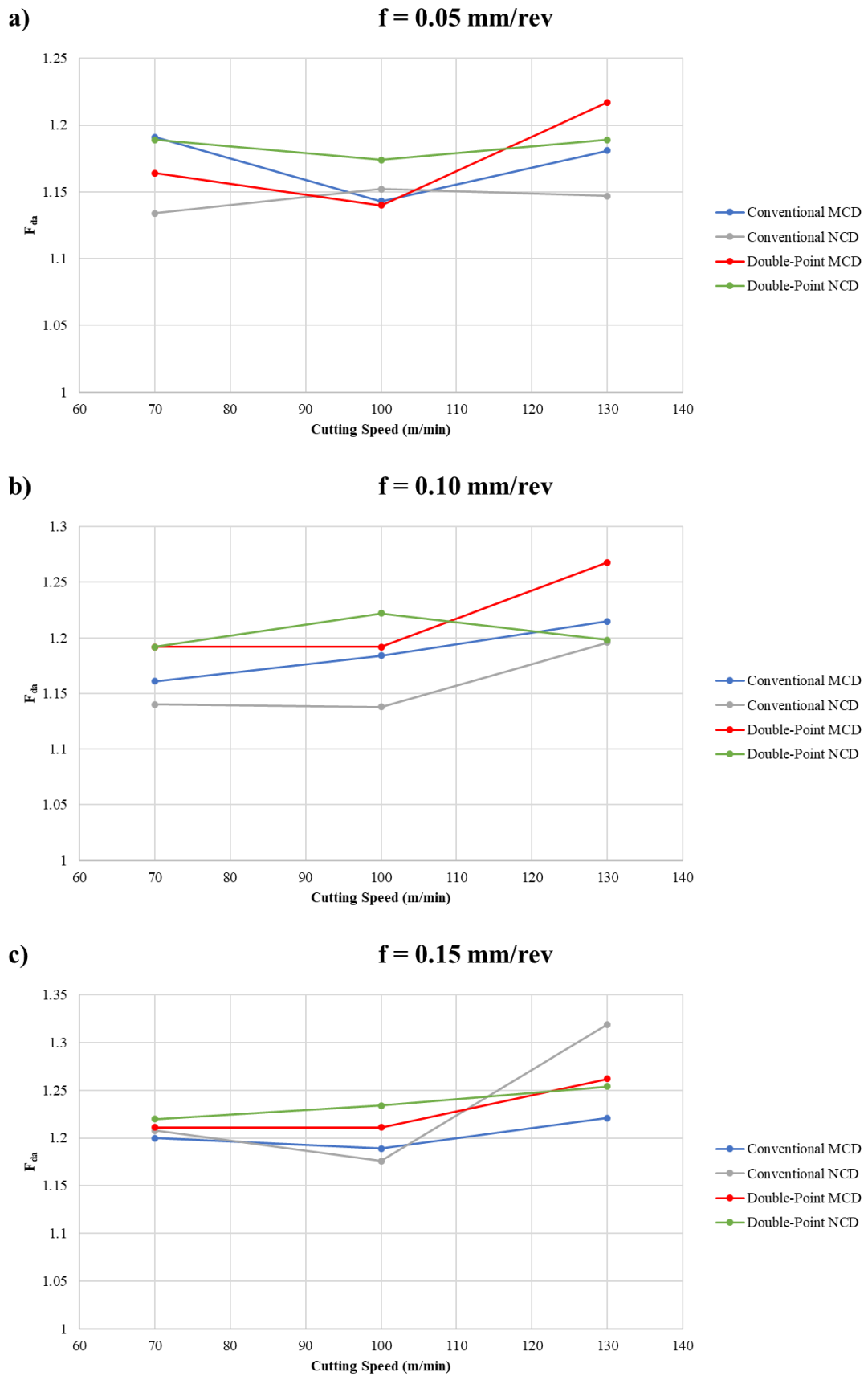


Figure 68 - Adjusted delamination factors for each combination of drill geometry and coating at various cutting speed values: (a) feed of 0.05 mm/rev; (b) feed of 0.10 mm/rev; (c) feed of 0.15 mm/rev.

From looking at the various results in Figure 67, it seems that regardless of the drilling tool, the higher the feed, the higher the delamination factor, which follows the trend of the

peak thrust force. This was to be expected, as several authors have reported similar observations [25, 32, 38, 116]. In terms of cutting speed, considering the higher tested feed values (Figure 68b and c), it seems that the higher its value is, the higher the delamination factor will be. Regarding the tool's geometry, there is a general tendency for the double-point angle twist drills to produce more delamination than the conventional twist drills, particularly for the higher feeds (Figure 68b and c). The F_{da} values are mostly within the range of 1.10 and 1.30, which are close to the ones obtained by Ge et al. [117] for a CFRTP material, even though the authors used a carbon fiber reinforced PEKK at lower cutting speed of 50 m/min. As for thermoset materials, Sorretino et al. [131] tested a carbon/epoxy twill material, and for the same feeds, the authors found F_{da} values between 1.1 to 1.5, with an increasing trend in delamination with the feed. It should, however, be mentioned that the cutting speeds were 47 m/min, 251 m/min and 377 m/min, corresponding to a larger range than of the speeds tested in the current study. Hou et al. [132] drilled a unidirectional carbon fiber reinforced epoxy laminate at feeds between 0.01 and 0.15 mm/rev and cutting speeds between 19 and 94 m/min, obtaining F_{da} values above 1.5. Even though the delamination can be affected by several factors apart from the matrix material, the difference between literature values (mostly available for thermoset matrices) and the obtained results seems to indicate that, when considering CFRP, the thermoplastic matrices are less prone to delamination than thermosets.

4.2.2.2. Burr

The analysis of the uncut fibers was performed to each of the 108 drilled holes through a qualitative method. It should be mentioned that, through the handling of the plates after drilling, such as, for example, moving the plate inside and removing it from the clamping system, some of the uncut fibers could have been ripped from the plate. Furthermore, as it will be exposed in 4.2.4, there seems to be some inconsistency regarding the uncut fibers, where sometimes holes made with the same drill and cutting parameters resulted in different quality levels. In the present analysis, only the uncut fibers that maintained attached to the plate after handling were considered, with the pictures taken in the same circumstances for each of the holes. In Figure 69, five hole images are presented, which served as a reference for the 5 quality levels related with burr damage.

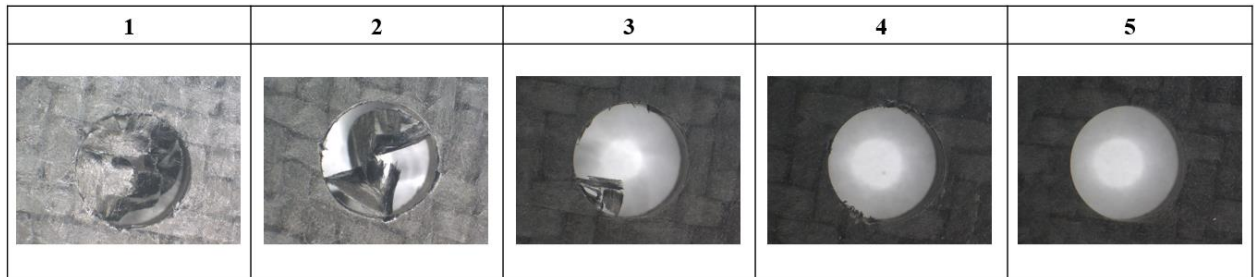


Figure 69 - Qualitative scale used for the characterization of the uncut fiber damage.

With the qualitative scale, it was then possible to characterize the quality of each hole. In Figure 70 and Figure 71, each point is correspondent to the average quality level of the 3 holes performed with the same drill at the same cutting parameters.

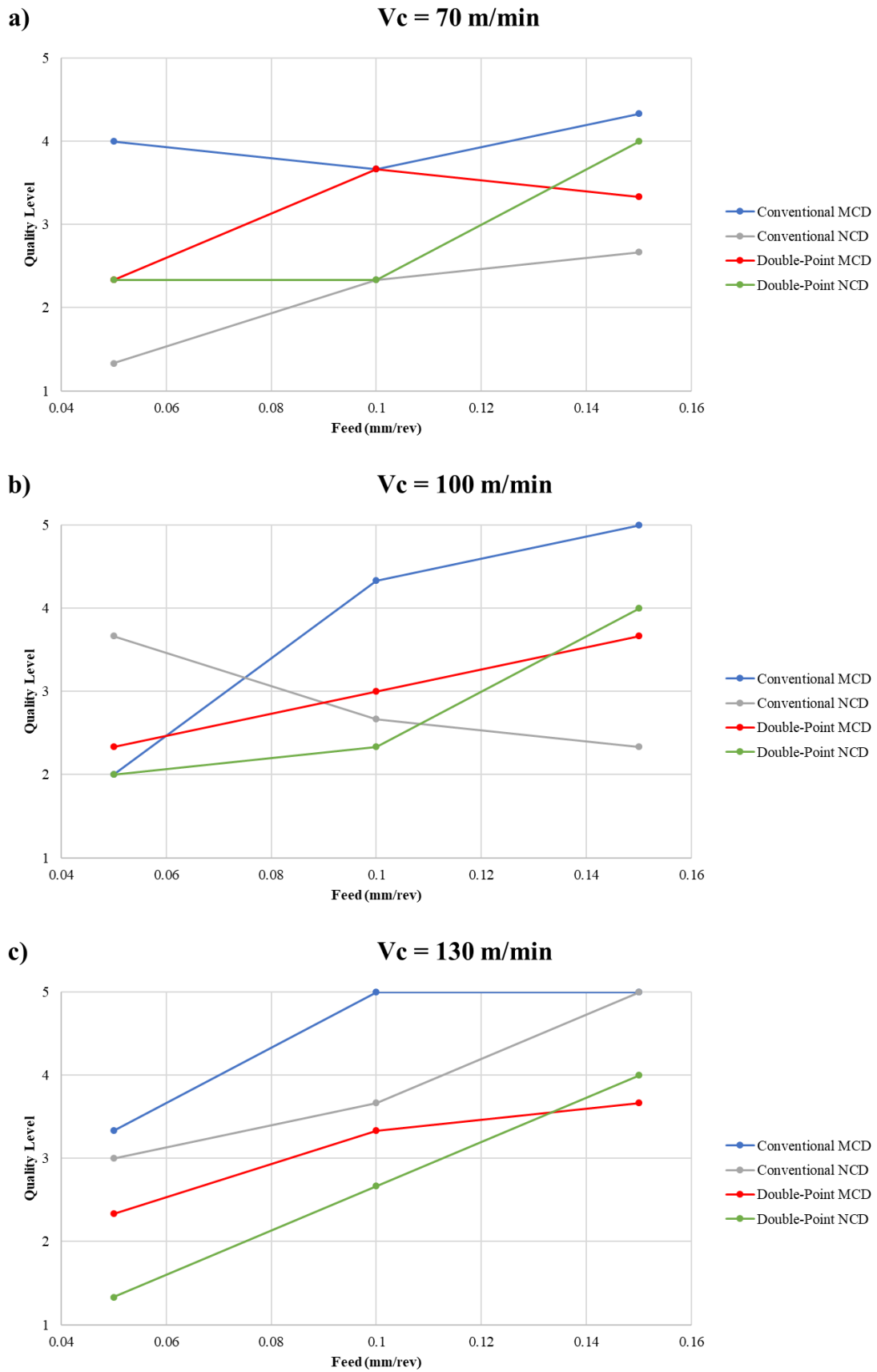


Figure 70 - Average hole quality level for each combination of drill geometry and coating at various feed values: (a) cutting speed of 70 m/min; (b) cutting speed of 100 m/min; (c) cutting speed of 130 m/min.

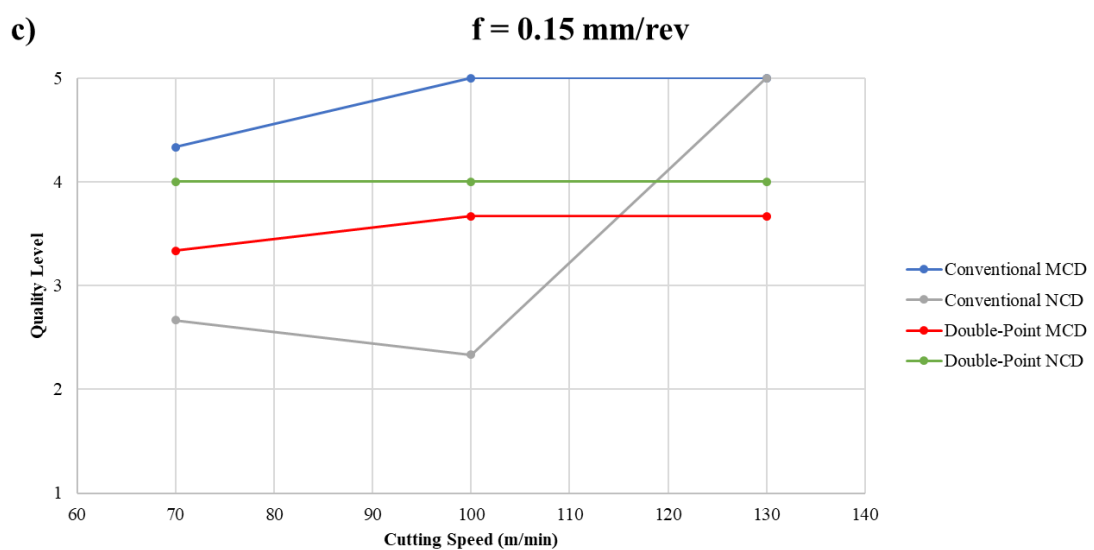
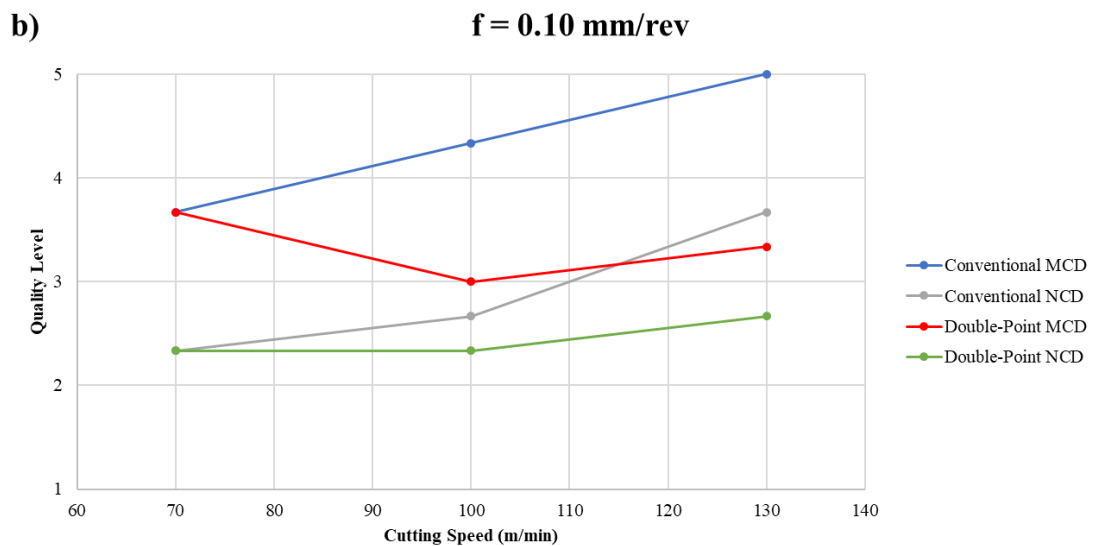
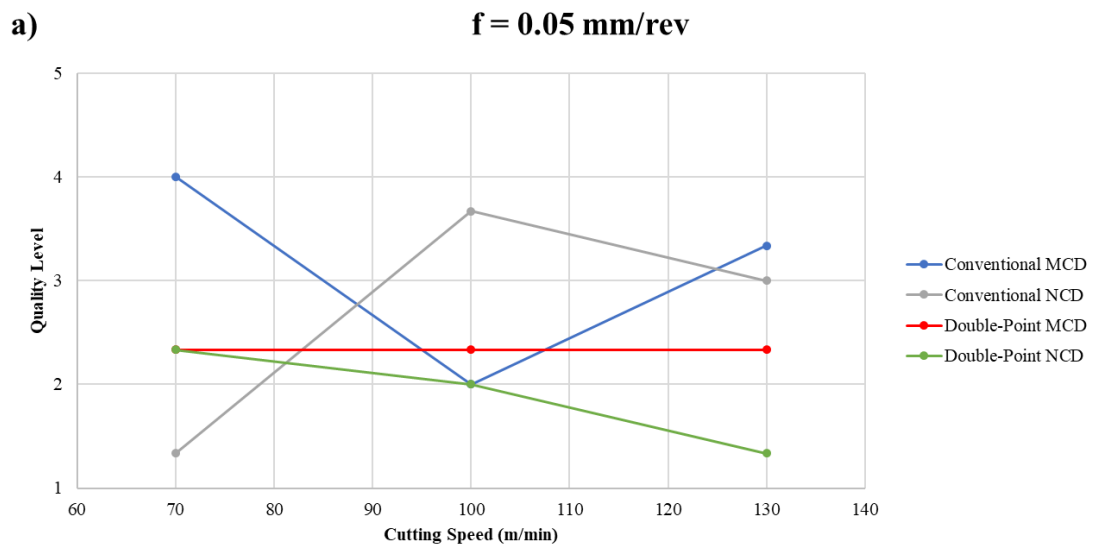


Figure 71 - Average hole quality level for each combination of drill geometry and coating at various cutting speed values: (a) feed of 0.05 mm/rev; (b) feed of 0.10 mm/rev; (c) feed of 0.15 mm/rev.

Through observation of Figure 70, it seems that higher feed benefits the hole's quality. In Figure 71 it is also apparent that, particularly for the higher feeds (Figure 71b and c),

higher cutting speeds are also beneficial. The cutting speed seems to have a lesser effect on burr formation than the feed. Regardless of the drilling tool, the best quality holes were obtained for a feed of 0.15 mm/rev coupled with the cutting speeds of 130 m/min or 100 m/min, except for the conventional twist drill with a NCD coating. An example of a higher quality hole obtained at these cutting parameters is presented on Figure 72a. With these operative conditions, the conventional twist drills with both coatings performed better than their double-point angle counterparts. In contrast, the lowest feed value of 0.05 mm/rev usually led to the worse quality holes, regardless of the cutting speed. An example of a hole obtained for this feed is presented on Figure 72b. Comparing the tools through Figure 71, it seems that there is a slight tendency for tools with a MCD coating to perform better than their counterparts with the same geometry and a NCD coating, which can probably be attributed to larger CER of the NCD coated tools, as it was mentioned in subsection 3.3.1.1. This fact was more evident for the higher cutting speeds presented in Figure 70b and c. Such observation was confirmed by Poór et al. [66] and Wang et al. [30], with the explanation most likely being that with higher CER, the chip removal mechanism becomes bending dominated, leading to more burr.

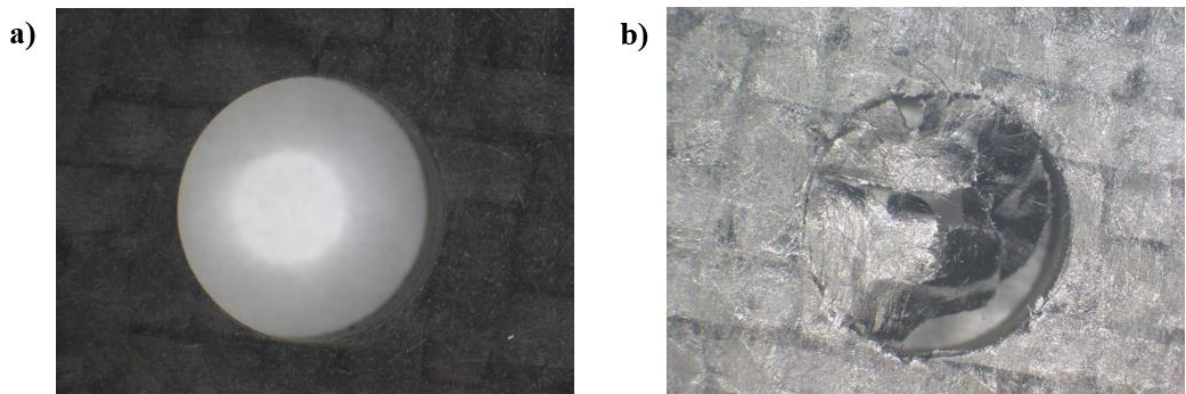


Figure 72 - Holes with distinct quality levels: (a) higher quality hole, drilled with a conventional twist drill with a MCD coating at 0.15 mm/rev and 130 m/min; (b) lower quality hole, obtained with a conventional twist drill with a NCD coating at 0.05 mm/rev and 70 m/min.

For the double-point angle twist drills there was a shape of uncut fiber that was not observed for the conventional twist drills. It consisted in segments of uncut fibers that were firmly attached to the hole, as it is highlighted in Figure 73, making them difficult to remove. This kind of damage did not seem to be correlated with the parameters, appearing randomly.

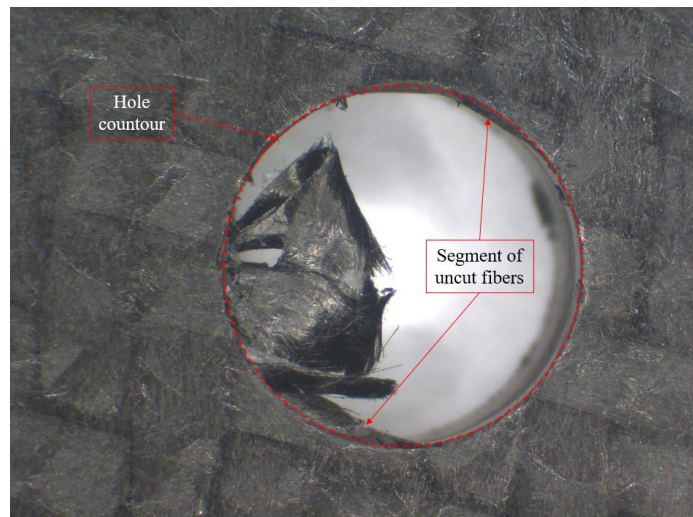


Figure 73 - Example of a hole with a large segment of uncut fibers, drilled with a double-point angle twist drill with a nanocrystalline diamond coating at 0.05 mm/rev and 100 m/min.

With the presented results, it seems that higher feeds lead to less burr, which does not follow the same tendency pointed out by most of the consulted literature [38, 66, 67], that claim that the burr reduces with a decrease in feed. One possible explanation for the difference might be associated with the drilling temperatures. When using lower feeds, the temperature tends to be higher (due to increasing the amount of time where the tool and workpiece are in contact [120]), resulting in a more challenging fiber cutting process, possibly leading to excessive burr.

4.2.2.3.Hole's Surface Roughness

Surface roughness was measured for the holes with the highest (feed of 0.15 mm/rev and cutting speed of 130 m/min) and lowest (feed of 0.05 mm/rev and cutting speed of 70 m/min) sets of parameters. The obtained results for Ra and Rz are presented in Figure 74 and Figure 75, respectively.

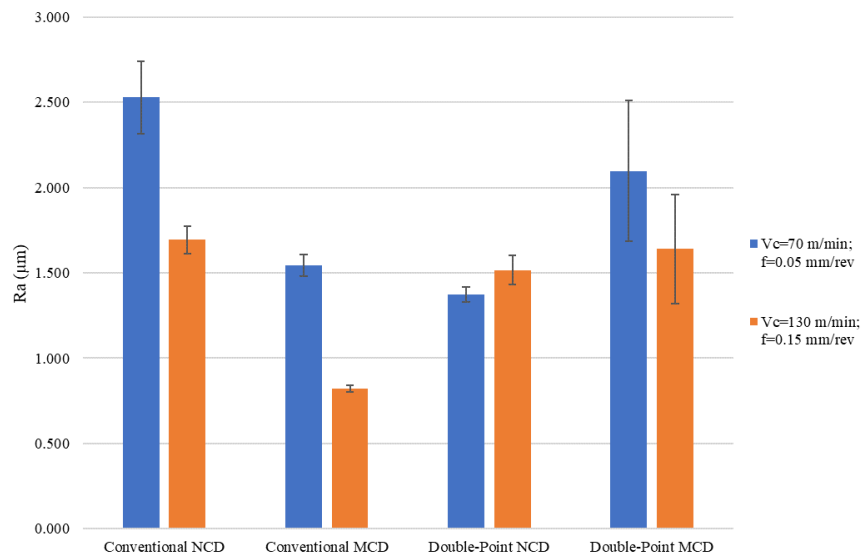


Figure 74 - Ra for the hole's surface of each drilling tool at 70 m/min and 0.05 mm/rev and at 130 m/min and 0.15 mm/rev.

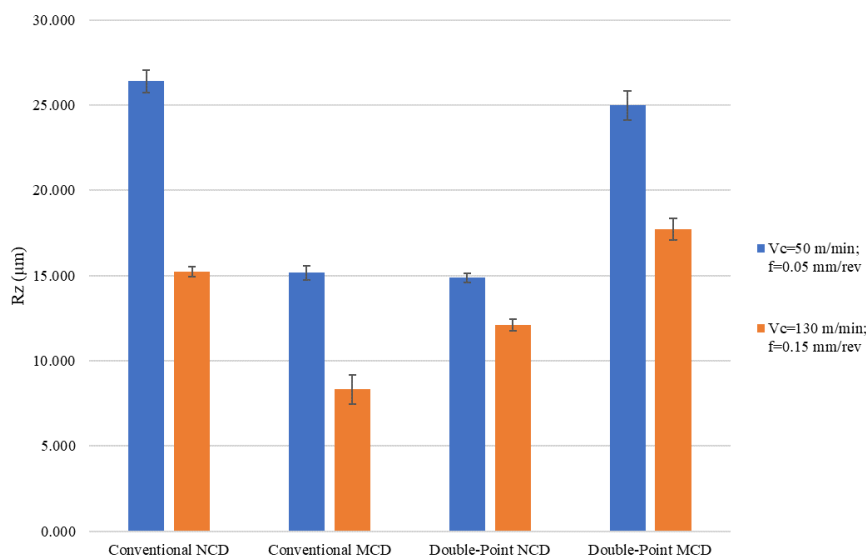


Figure 75 - Rz for the hole's surface of each drilling tool at 70 m/min and 0.05 mm/rev and at 130 m/min and 0.15 mm/rev.

From the obtained Ra results, it seems that, in general, using the set of highest parameters is beneficial regardless of the drilling tool used. The Rz results also follow the

same tendency. In both cases, the conventional twist drill with the MCD coating was the one with the best performance, particularly when used with the set of the highest parameters. Some authors point out that the best quality results are obtained through reducing the feed and increasing the cutting speed [72, 74], but because in this case a full parametric analysis was not performed, that could not be verified. However, if that tendency was to be verified in this analysis, it would seem that the higher cutting speeds had a more prominent beneficial effect than the higher feeds had a detrimental effect. Considering the literature values, there is some variability, with Konneh et al. [133] obtaining between 4.63 and 0.91 μm and Shahabaz et al. [74] reached between 1.930 and 0.785 μm , both at similar feeds but lower cutting speeds. Even though these values are close to the 2.5 to 0.8 μm range that was obtained, they were measured on holes drilled with different cutting parameters using different drilling tools and a carbon/epoxy material. The ratios between the values of Rz and Ra were also calculated, with the obtained results presented in the graph on Figure 76.

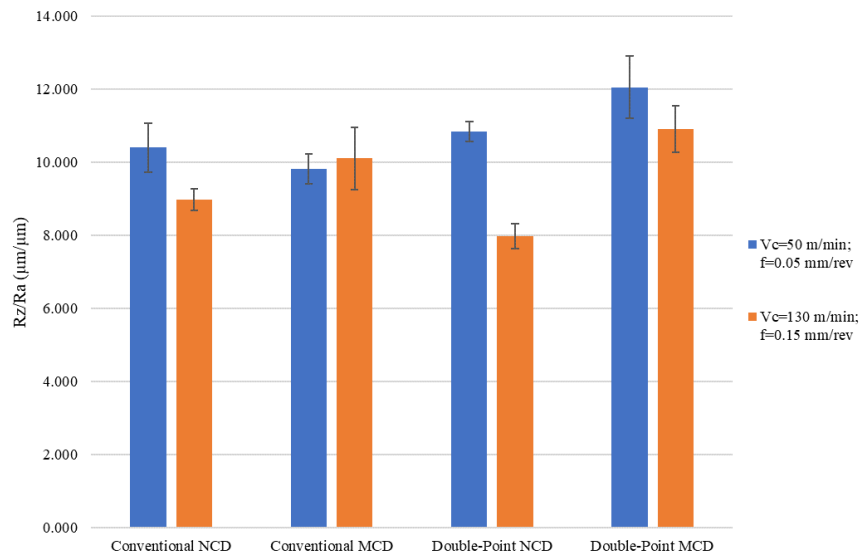


Figure 76 - Rz/Ra ratio for the hole's surface of each drilling tool at 70 m/min and 0.05 mm/rev and at 130 m/min and 0.15 mm/rev.

A higher ratio value usually means that the hole's surface has more defects, like uncut fibers or even delamination. In general, the tendency of obtaining a better-quality hole for the set of higher parameters that was observed for Ra and Rz is maintained for the ratio between these parameters.

4.2.2.4. Hole Accuracy

Regarding hole accuracy, data for all the 108 drilled holes was obtained. The average for every 3 holes drilled with the same parameters was calculated along with the respective standard deviation. In the graphs from Figure 77 and Figure 78 the values on the vertical axis are the hole's diameter deviation from the nominal value in μm , which in this case was 6 mm.

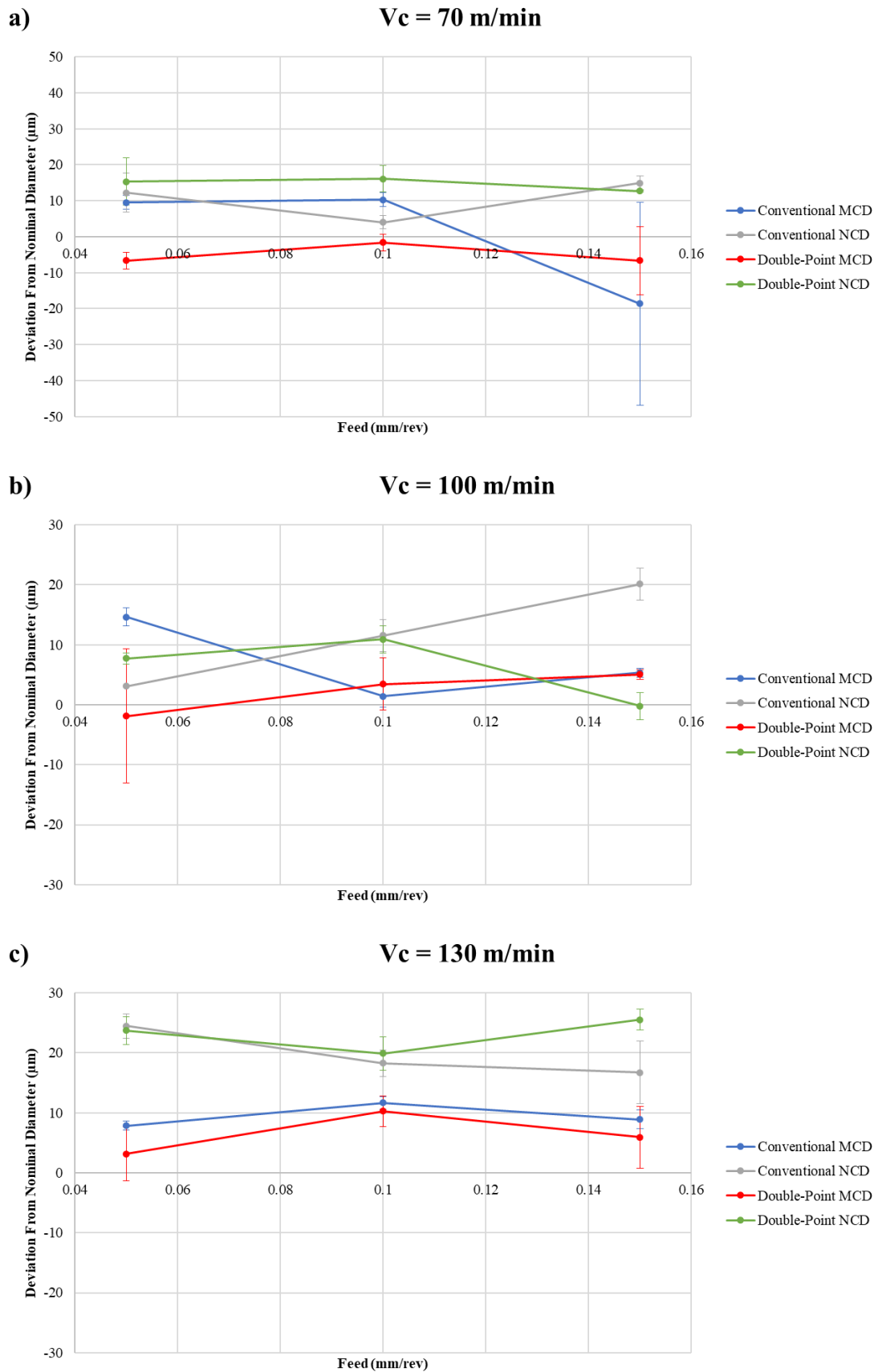


Figure 77 - Deviation from nominal diameter for each drill at a certain cutting speed for various feeds: (a) Cutting speed of 70 m/min; (b) Cutting speed of 100 m/min; (c) Cutting speed of 130 m/min.

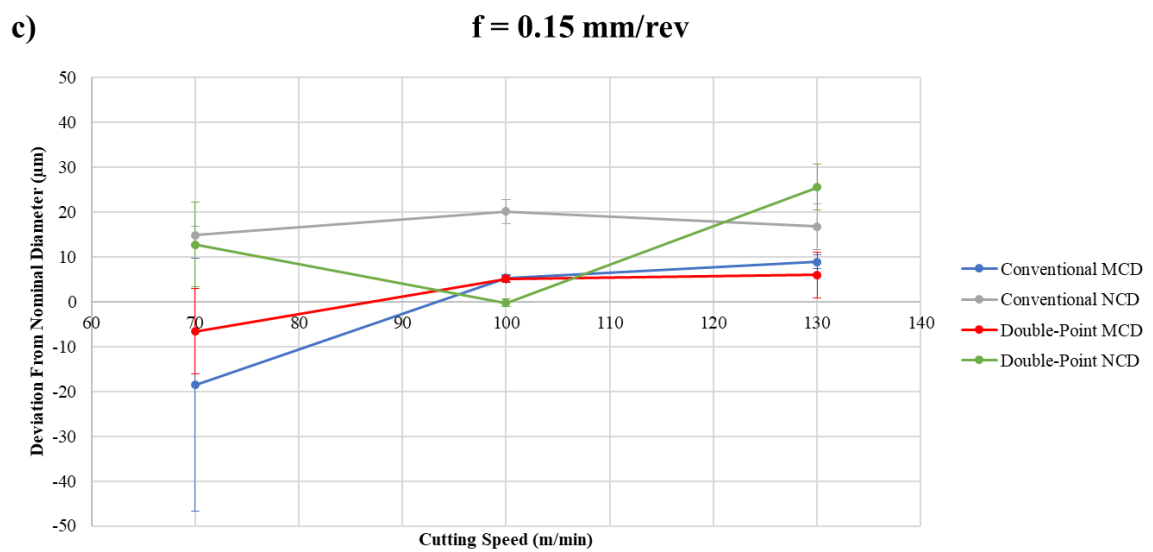
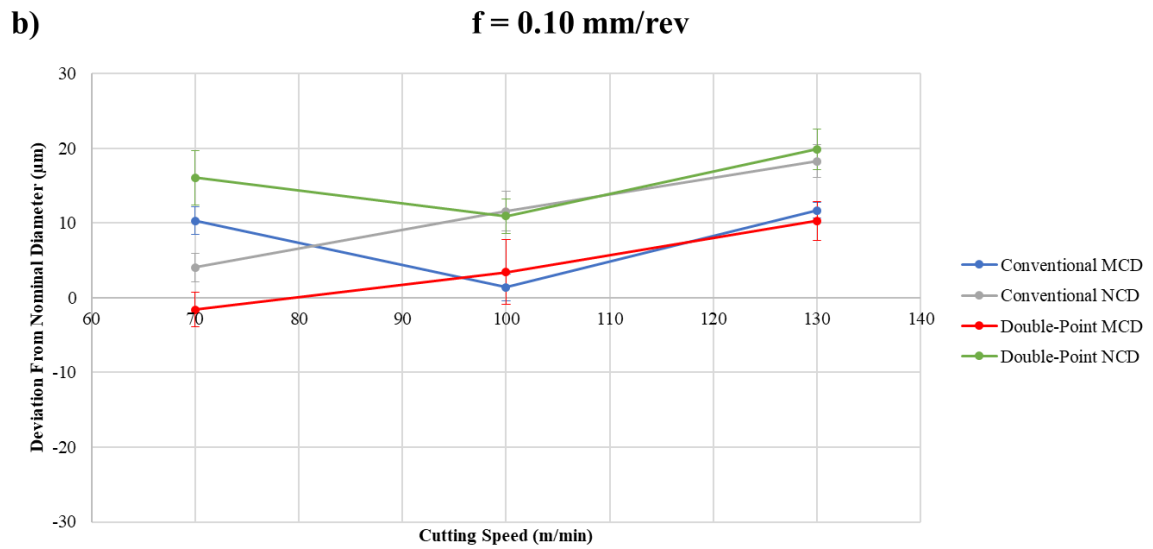
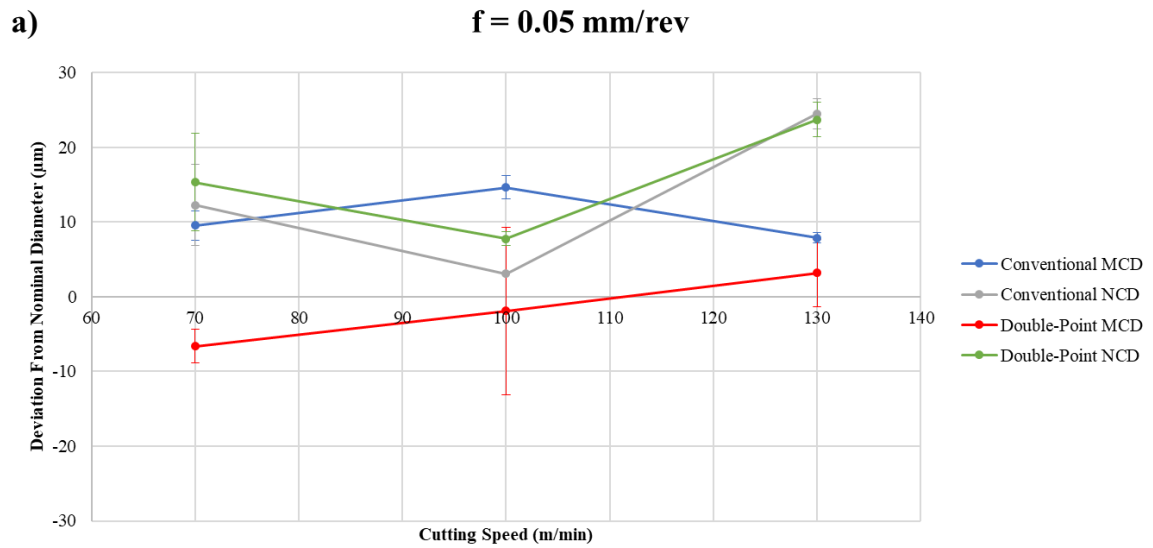


Figure 78 - Deviation from nominal diameter for each drill at a certain feed for various cutting speeds: (a) feed of 0.05 mm/rev; (b) feed of 0.10 mm/rev; (c) feed of 0.15 mm/rev.

Firstly, it should be mentioned that both Figure 77a and Figure 78c, the scale on the vertical axis is set to different values than the remaining graphs, due to the point

correspondent to the conventional MCD drill at a cutting speed of 70 m/min and a feed of 0.15 mm/rev presenting a very high standard deviation, which is most likely due to an outlier result. By observing the three graphs in Figure 78, it seems like higher values of cutting speed led to a higher positive deviation from the nominal diameter, meaning that the drilled holes tend to be larger than nominal. This observation is in accordance with Xu et al.'s [120] work, and is thought to be related with the more severe tool vibration at the higher cutting speeds. On all graphs of Figure 77 and Figure 78 (particularly in Figure 77c), it seems that the tools with a NCD coating tend to produce holes with a higher diameter than their counterparts with a MCD coating. This can be due to the thickness of each coating. As it was mentioned on subsection 3.3.1.1, the drills with a NCD coating present a higher CER than their MCD counterparts. Considering that the drill's diameter is controlled before applying the diamond coating to a tolerance of h6 (lower limit of -0.008 mm and upper limit of 0 mm), the observed difference may be explained by the NCD coating being thicker, which thereby results in drilling tools that will probably have a higher diameter. Therefore, the holes produced with these kinds of drills tend to have a higher diameter which, by the presented observations in Figure 77c, seems to be more pronounced for higher cutting speeds.

According to the standard ISO 2768, which concerns general dimensional tolerances, a hole with a nominal diameter of 6 mm should not deviate more than 50 μm from the nominal value for it to be considered under a fine tolerance, which corresponds to the tightest tolerance level. From the observed results, it can be noticed that regardless of the drilling tool or the cutting parameters, this tolerance requirement is met [134].

4.2.3. Chips

During the drilling process, the chips tended to cause tool clogging, as it is highlighted through Figure 79, regardless of the drilling tool and cutting parameters. This observation follows what several other authors found [116, 117, 119], with some pointing out that this could influence the drilling thrust force. To reduce its interference in the obtained results, the chips were removed from the cutting tool between the drilling of each hole.

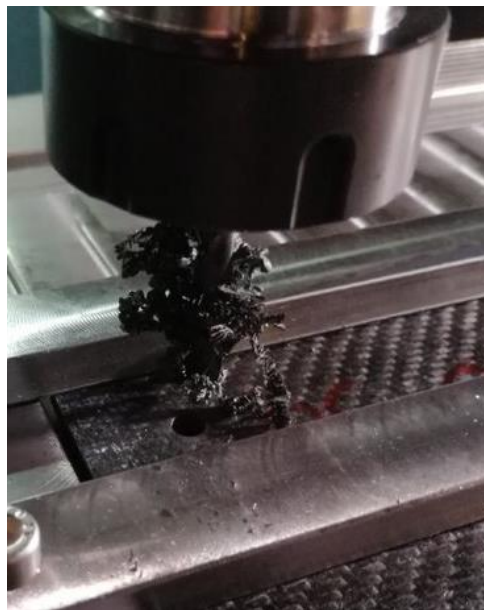


Figure 79 - Clogged drilling tool after drilling.

On Figure 80, three different chips are presented, correspondent to a conventional twist drill with a NCD coating, at a cutting speed of 70 m/min for various feeds.

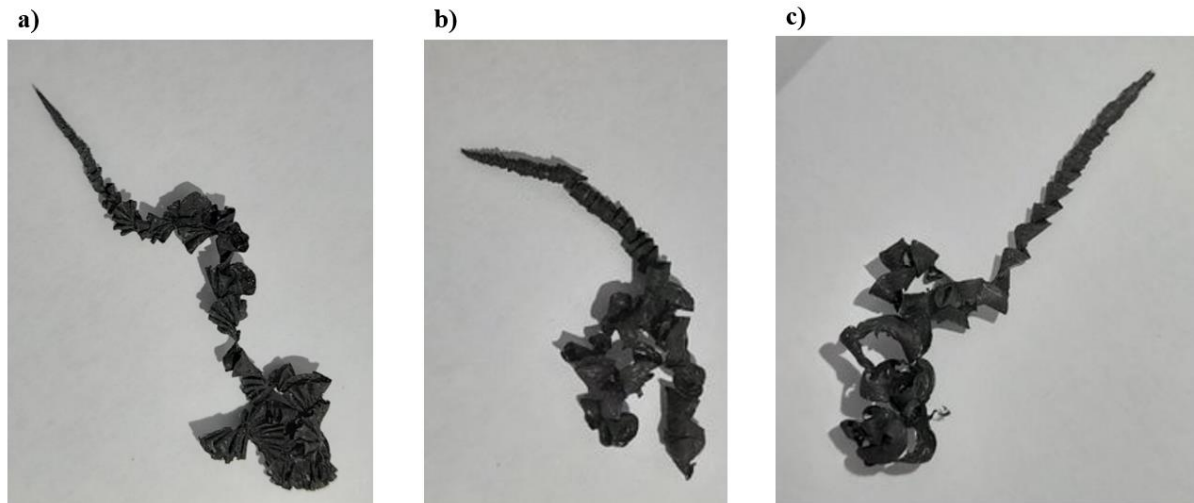


Figure 80 - Chips obtained from holes drilled with a conventional twist drill with a NCD coating at a cutting speed of 70 m/min: (a) feed of 0.05 mm/rev; (b) feed of 0.10 mm/rev; (c) feed of 0.15 mm/rev.

Firstly, it should be referred that in every case the obtained chips were continuous and ribbon-like. This observation has also been confirmed by literature [116, 117, 119, 121], as several authors refer that thermoplastic matrices possess enough ductility to withstand high plastic deformation through the machining process. Even though it was not measured, it is also likely that the glass transition temperature of the PA12 matrix material was surpassed, promoting material softening and increasing its ductility, which allows the chip to deform plastically instead of fracturing, as some authors also pointed out [117, 121]. Besides the feed, neither the drilling tool (either through the geometry or the coating), nor the cutting speed seemed to produce a significant effect on the chip's morphology.

Another interesting observation is the difference in the chips' shape as the feed increases, which becomes particularly evident when chips obtained for 0.05 mm/rev (Figure 80a) are compared to the ones of 0.10 mm/rev and 0.15 mm/rev (Figure 80b and c, respectively), and is highlighted on Figure 81a and Figure 81b, where the first ones are more curly and folded than the second ones. These observations are coherent with the ones of Ge et al. [117], and is thought to be associated with the difference in chip thickness at different feed values.

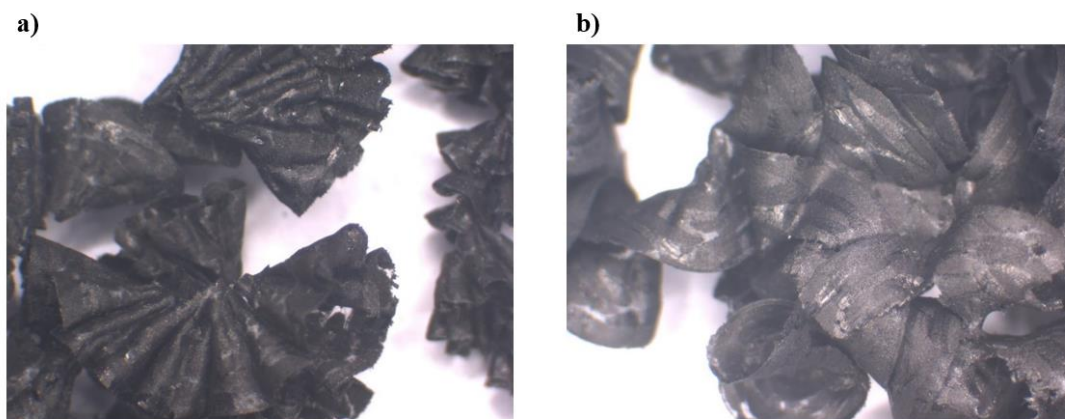


Figure 81 - Chips obtained from holes drilled with a conventional twist drill with a NCD coating at higher magnification for a cutting speed of 70 m/min: (a) feed of 0.05 mm/rev; (b) feed of 0.10 mm/rev.

To access this, chip images were obtained through SEM, and their respective thickness was measured. In Figure 82a, b and c it is possible to observe chips obtained at feeds of 0.05, 0.10 and 0.15 mm/rev, respectively. Using the ImageJ software, a total of 10 measurements

were performed along the thickness of each chip, with the obtained average values present in Table 21.

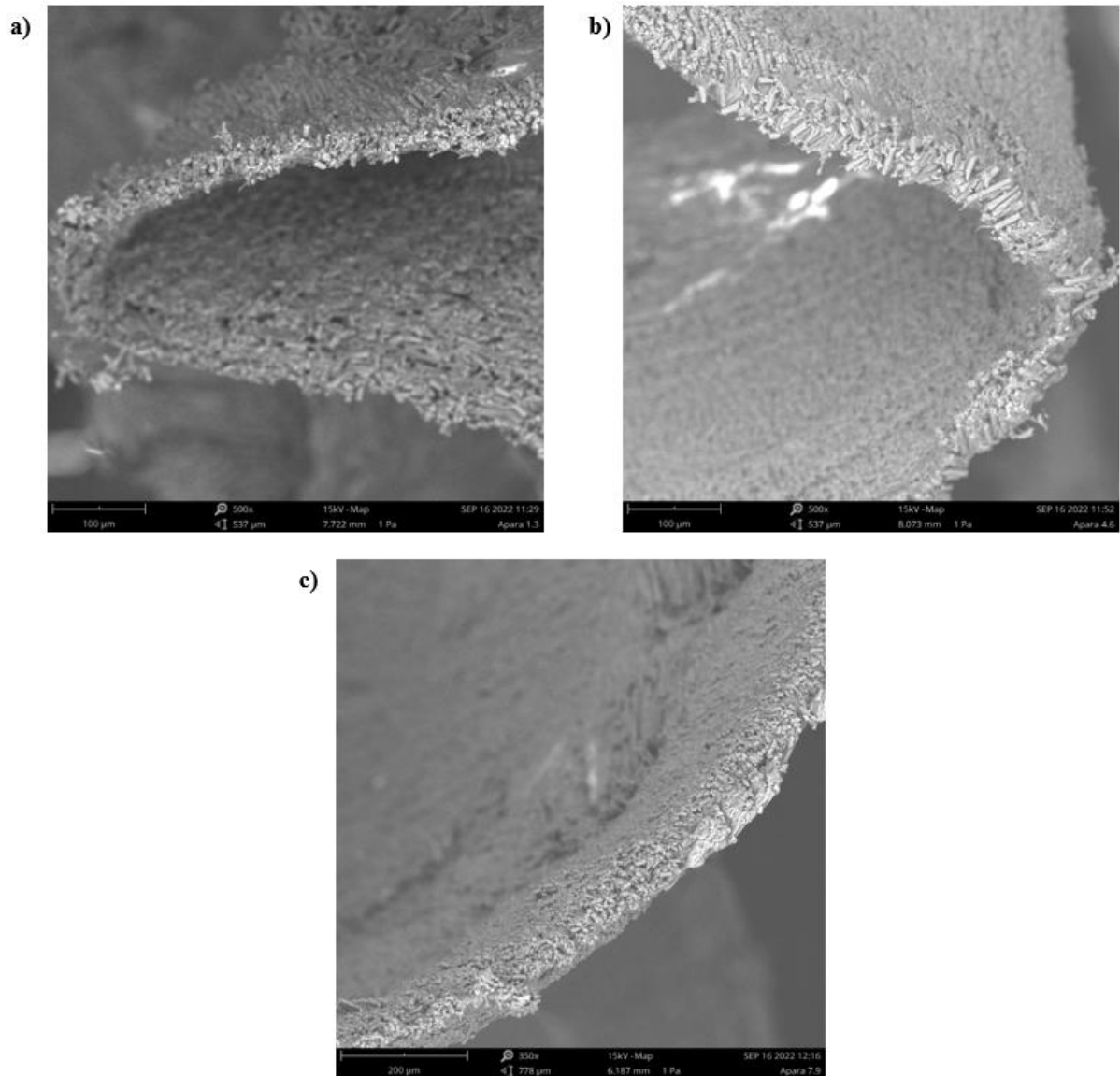


Figure 82 - SEM images of chips obtained using a conventional twist drill with a NCD coating, at a cutting speed of 70 m/min: (a) feed of 0.05 mm/rev; (b) feed of 0.10 mm/rev; (c) feed of 0.15 mm/rev.

Table 21 - Average measured thickness for chips at various feeds.

Feed (mm/rev)	Average Measured Thickness (μm)	Standard Deviation (μm)
0.05	32.92	2.89
0.10	53.34	4.20
0.15	81.59	8.93

The presented values indicate that with an increase in feed there is a resultant increase in the average chip thickness, which follows the observations presented by Ge et al. [117], and is related to the increase in the uncut chip thickness at higher feed values. The thinner chips resultant from lower feeds tend to be more curly due to higher susceptibility to being deformed, when compared with thicker chips (obtained at higher feeds).

4.2.4. Wear Analysis

For the drilling tool's wear assessment, it was decided to use a conventional twist drill with a MCD coating at a feed of 0.10 mm/rev and a cutting speed of 130 m/min. This drill was found to produce better quality holes both regarding burr in subsection 4.2.2.2, and lower surface roughness values in subsection 4.2.2.3. It also resulted in slightly lower peak thrust forces (subsection 4.2.1.2) and the delamination factors were similar to the remaining drilling tools (subsection 4.2.2.1). In subsection 4.2.2.4 it was also found that the MCD coated drills produced more accurate holes, particularly at higher feed and cutting speed combinations.

By comparing the virgin drilling tool (Figure 83a and b) to the drilling tool after 650 holes are drilled (which corresponds to a drilling distance of 2600 mm, presented in Figure 83c and d), it is attainable that neither the tool's flank nor the rake face presented any signs of wear.

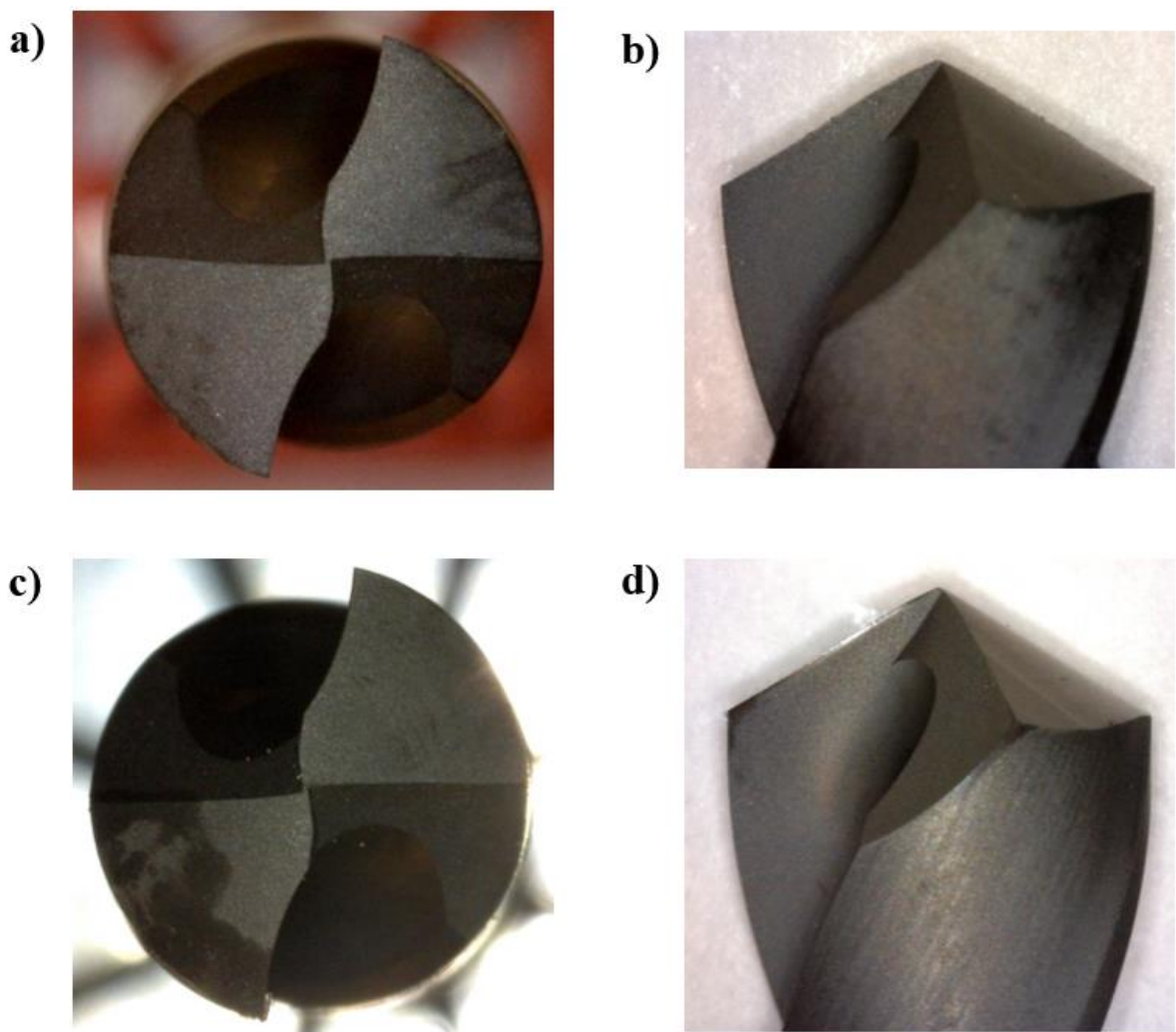


Figure 83 - Drill pictures from wear campaign: (a) flank face of new drilling tool; (b) rake face of new drilling tool; (c) flank face of drill after 650 drilled holes; (d) rake face of drill after 650 drilled holes.

After 650 drilled holes the drilling tool was submitted to an SEM analysis. Looking firstly at the flank face, it presented with a coating delamination and chipping at the corner that was not observed before, as it is visible through the highlight in Figure 84b of the area delimited by the red rectangle on Figure 84a. The appearance of these defects is thought to be evidenced by the cleaning process through ultrasonic bath before the drill's observation, that most likely led to the resurgence of defects in an already fragile zone of the drilling tool.

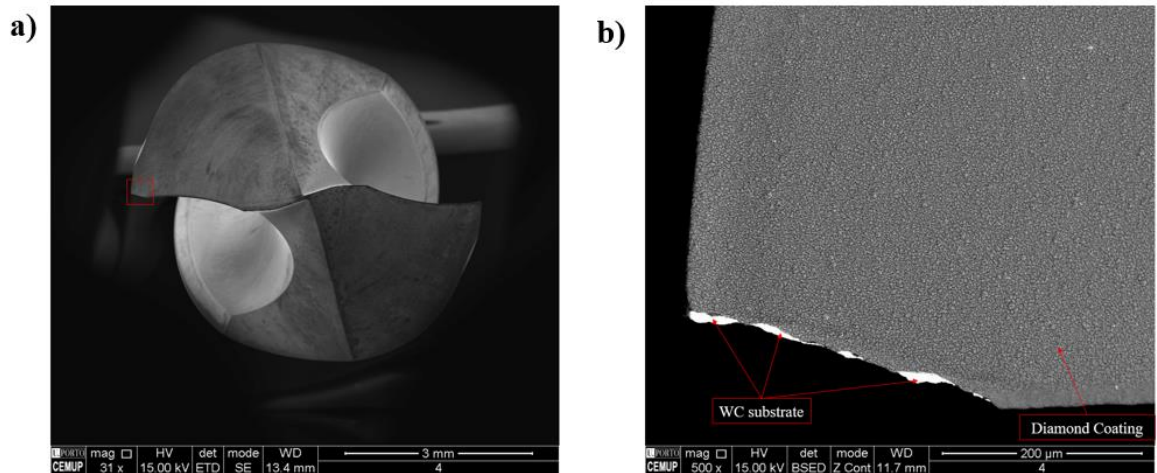


Figure 84 - SEM of drill's flank face after 650 drilled holes: (a) at lower magnification; (b) corner detail.

Looking at the drill's rake faces, the coating delamination is visible on both. This delamination was smaller in one of the drill's corners, presented in Figure 85a and observed through higher magnification in Figure 85b, in comparison to the other corner, presented in Figure 85c and highlighted in Figure 85d. These observations mean that, most likely, for these testing conditions, the drill's corners are the most prone to coating delamination.

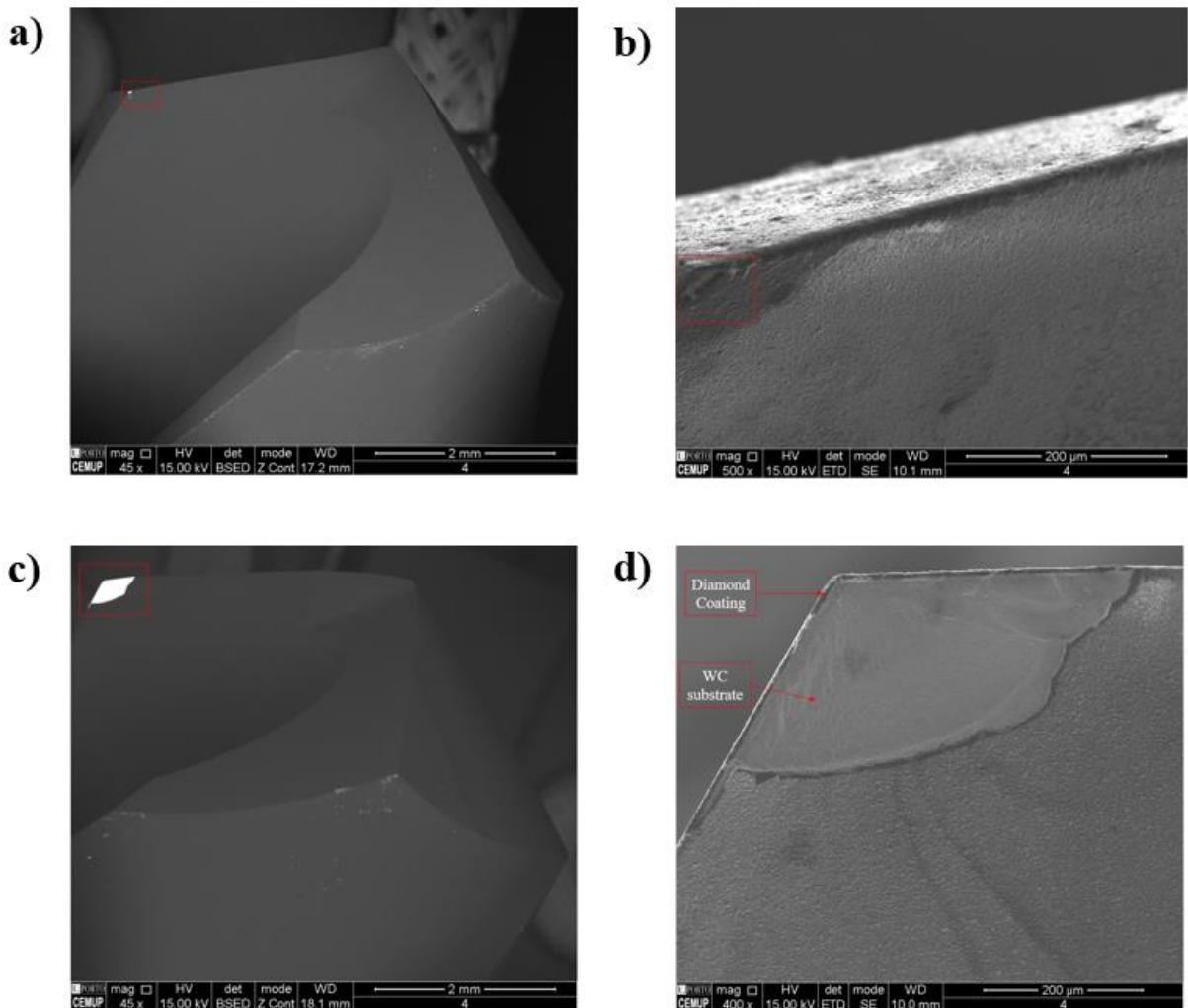


Figure 85 - SEM of drill's rake faces after 650 drilled holes: (a) rake face with the smaller coating delamination; (b) rake face with highlight around the smaller coating delamination; (c) rake face with the larger coating delamination; (d) rake face with the larger coating delamination at higher magnification.

Figure 86b shows that after 650 holes the drilling tool suffered some abrasion in its flank face near to the cutting edge. Using the ImageJ software, it was possible to obtain an average abrasion length of $17.32\text{ }\mu\text{m}$. It should also be mentioned that even though the drill after 3 holes (12 mm of drilling distance) was almost new, it already suffered some chipping at its corner as it can be seen in Figure 86a.

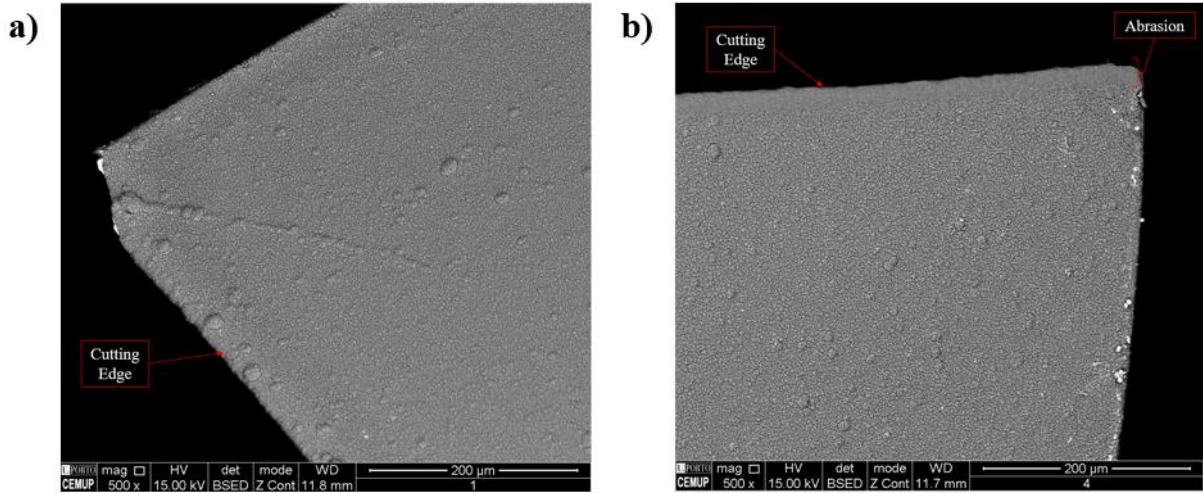


Figure 86 - SEM of drill's flank faces: (a) drill after 3 drilled holes (12 mm of drilling distance); (b) drill after 650 drilled holes, with a highlight on surface abrasion.

Comparing Figure 87a and b, correspondent to the cutting edge of a relatively new drill (performed 3 holes) and a used drill (performed 650 holes), respectively, it can be noticed that the newest drill has a rougher cutting edge, whereas the new drill appears to be smoother, most likely due to abrasion from the workpiece material. Even though it was not measured, the used drilling tool seems to have a higher CER.

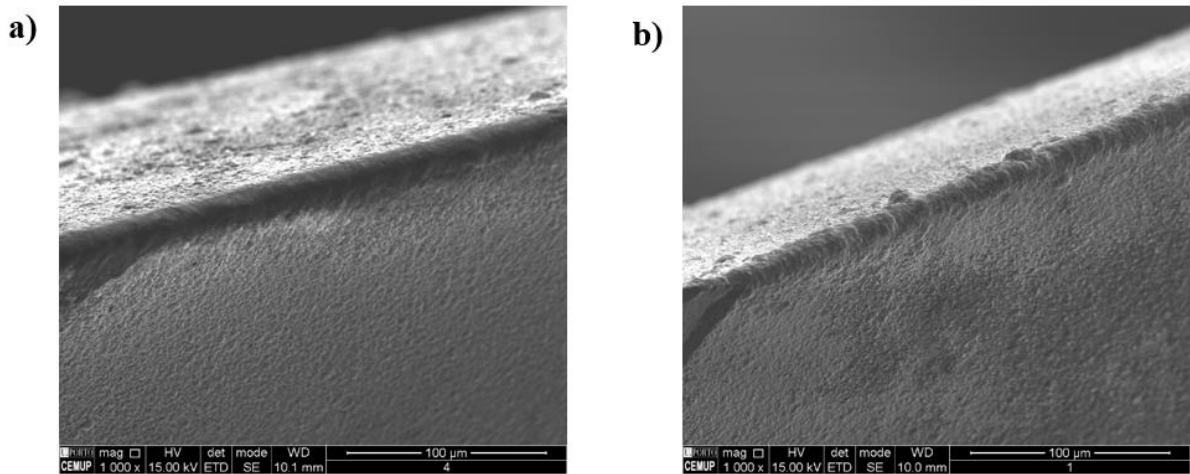


Figure 87 - SEM image of drill's cutting edge: (a) after 3 holes; (b) after 650 holes.

Throughout the next drilled holes, the smaller coating delamination grew along the cutting edge for an increasing number of performed holes highlighted in Figure 88 and Figure 89. Using the measurement tool of the DinoLite 2.0 software, it was found that the length of the coating delamination along the cutting edge went from around 0.257 mm after 750 holes (3000 mm of drilled distance), to 0.347 mm after 850 holes (3400 mm of drilled distance) and 0.489 mm after 950 holes (3800 mm of drilled distance).

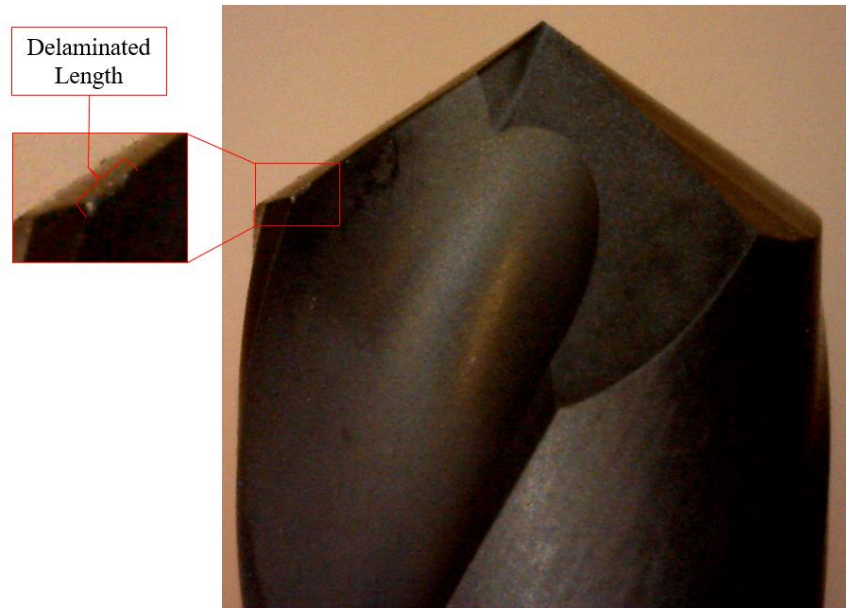


Figure 88 - Rake face of drilling tool after 750 drilled holes, with highlight on coating delamination.



Figure 89 - Rake face of drilling tool after 950 drilled holes, with highlight on coating delamination.

The larger delamination also presented a similar pattern, with the length of the delamination along the cutting edge increasing with the amount of drilled holes, which is perceivable through the observation of Figure 90a and b. In this case, the delaminated length measured along the cutting edge increased from 0.429 mm after 750 drilled holes, to 0.530 mm after 850 drilled holes and 0.595 mm at 950 drilled holes. Regarding the flank face, no relevant variations were observed.

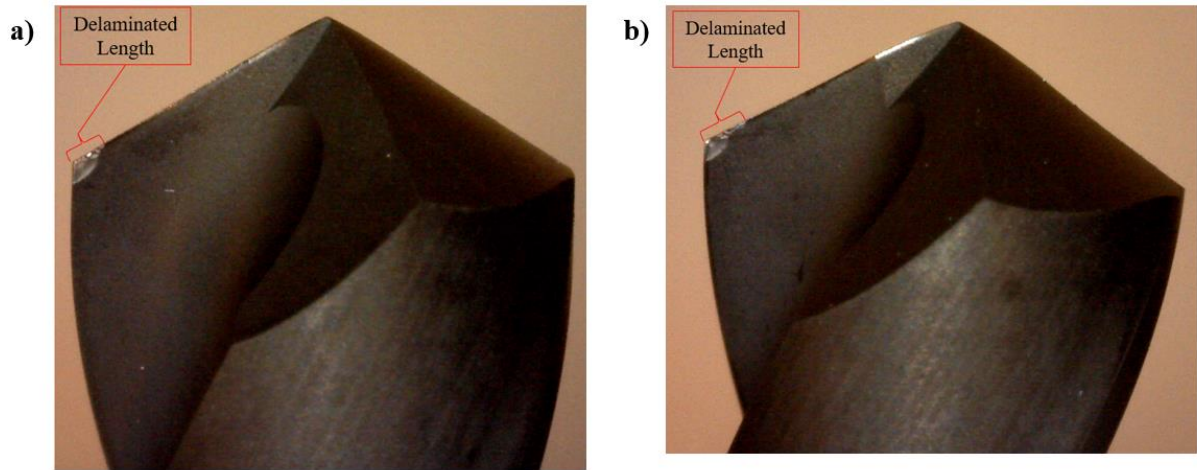


Figure 90 - Rake face of drilling tool with highlight on coating delamination: (a) 750 drilled holes (3000 mm of drilled distance); (b) after 950 drilled holes (3800 mm of drilled distance).

The present observations are in line with the ones from Fernández-Pérez et al. [93], in that the cause of tool failure is coating delamination, highlighting the importance of coating adhesion to the substrate in terms of increasing the tool's life. The study conducted by Kuo et al. [135] also supports the obtained results, with both coating delamination and slight chipping being found at the drill's corner. Like in the present work, the authors also observed abrasion near the cutting edge and cutting edge rounding.

Throughout the wear analysis, even though the hole's quality was not controlled, it was perceivable that there was some inconsistency regarding burr, as it is represented through Figure 91, where the drilling was performed in a line with the direction of the red arrow, and then began at another line following the white arrow. This inconsistency seems to indicate that the burr is dependent upon factors other than the drilling tool's geometry and coating or the cutting parameters. The fact the holes with burr as the ones highlighted appear before holes without any burr most likely means that tool wear is not the factor behind the observed inconsistency.

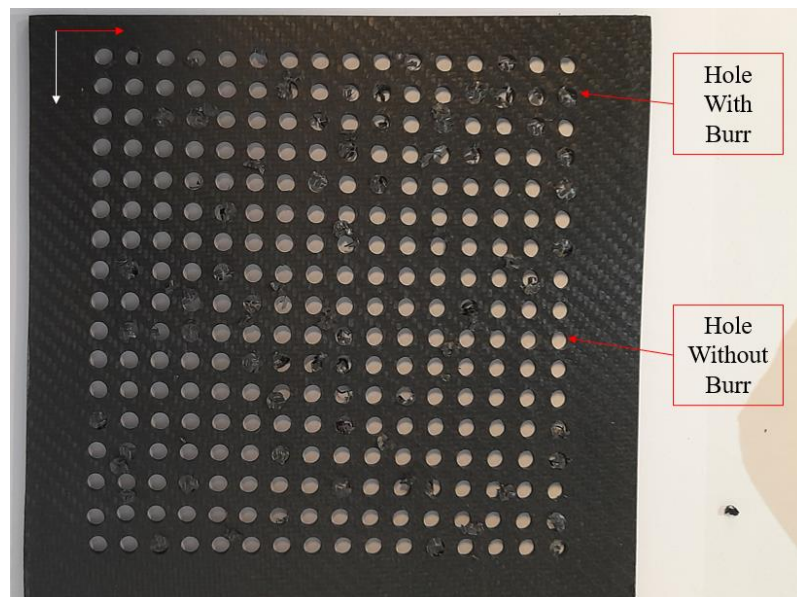


Figure 91 - Plate used in drilling wear assessment.

Chapter 5

Finite Element Analysis (FEA)

This chapter addresses the development of a FEA model of the CFRTP material used in experimental testing. It starts with an explanation of the steps followed towards the development of the tensile, compressive and shear testing models. Afterwards, the results of each model are exposed and compared to the experimental results obtained in subsection 4.1.

5.1.FEA Model Creation

The FEA model was developed using the ABAQUS software. The purpose of the created model was to establish the basis of material characterization for the present CFRTP, further enabling more advanced numerical models including delamination assessment and hole drilling simulation. To understand the validity of the identified material properties, the characterization tests were numerically simulated. Thereby, a total of three models were created: a tensile test model, a compressive test model and a shear test model. The same material model was employed for all three numerically portrayed loading conditions.

The geometry of each part followed the nominal dimensions considered in the manufacturing of the testing specimens, with a partition in the area correspondent to the respective gage section, as it is presented in Figure 92a, b and c for the tensile, compressive and shear tests, respectively. All the parts were 3D planar shells. The usage of shell elements was imposed by Hashin's criterion for fiber reinforced composite material's damage, as this built-in criteria must be used with these elements [136]. All the equations for this model were exposed in subsection 2.8, more specifically equations 2.1 to 2.4, that correspond to the several material failure modes.

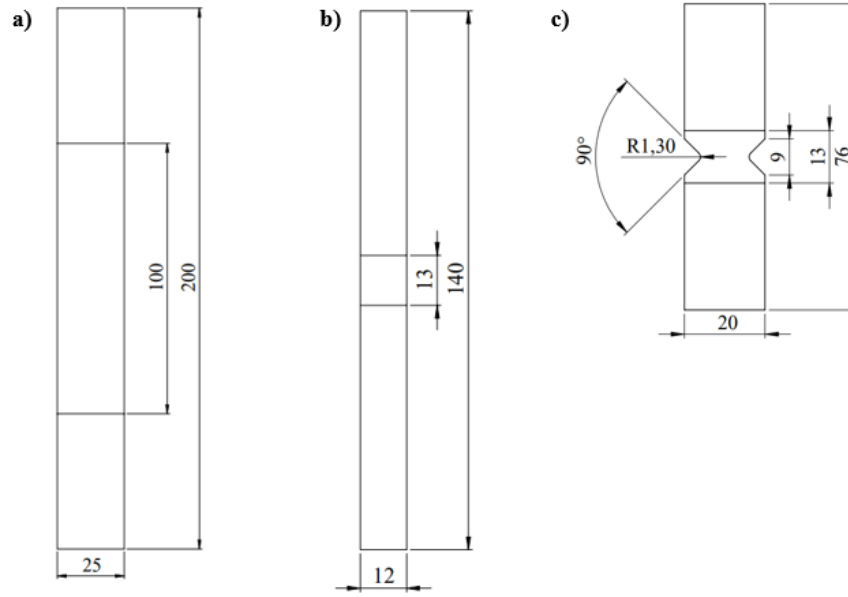


Figure 92 - Nominal specimen's dimensions used for FEA models: (a) tensile; (b) compressive; (c) shear.

Regarding the material's mechanical properties, both elastic properties and ones related to Hashin's criterion are required, and are presented in Table 22. It should be mentioned that, even though ABAQUS distinguishes the properties along longitudinal (1) and transverse (2) directions, the considered material does not present different mechanical behavior according to those directions. For the elastic constants, E_1 and E_2 were determined in subsection 4.1, with the used value being the average of the tensile modulus obtained for the 3 specimens, as well as ν_{12} and G_{12} . G_{13} and G_{23} result from an estimation based upon the properties exposed by Varas et al. [137], where a difference of around 500 MPa was found between G_{12} and G_{13} or G_{23} . In this case, as G_{12} was considered at 3.579 GPa, G_{13} and G_{23} were roughly estimated at 3 GPa. Regarding the properties required by Hashin's criterion, all the ultimate strengths were introduced based upon the results in subsection 4.1, where σ_1^{tu} and σ_2^{tu} are applied using the strength value correspondent to the only specimen with a valid fracture, and σ_1^{cu} , σ_2^{cu} and τ_{12}^u result from the average of the value obtained for the 3 specimens. Within the damage evolution suboption of Hashin's criteria, there is a requirement for both the intralaminar/translaminar tensile and compressive fracture energies, which are represented by G_{1C}^t or G_{2C}^t and G_{1C}^c or G_{2C}^c , respectively. G_{1C}^t and G_{2C}^t result from the average of the values obtained by Haldar et al. [138] and Blanco et al. [139], while G_{1C}^c and G_{2C}^c result from the value determined by Dalli et al. [140]. As for the viscosity coefficient, which has the purpose of overcoming convergence difficulties, it was set to 0.0001 [141-143].

Table 22 - Mechanical properties used for FEA model [138-142].

Property	Value
$E_1; E_2$	56.827 GPa
$\sigma_1^{tu}; \sigma_2^{tu}$	520.79 MPa
ν_{12}	0.1085
$\sigma_1^{cu}; \sigma_2^{cu}$	248.35 MPa
G_{12}	3.579 GPa
$G_{13}; G_{23}$	3 GPa [137]
τ_{12}^u	76.97 MPa
$G_{1C}^t; G_{2C}^t$	54.75 kJ/m ² [138, 139]
$G_{1C}^c; G_{2C}^c$	32.50 kJ/m ² [140]
Viscosity coefficient	0.0001 [141-143]

To attribute the modelled material to the part, the composite layup feature was used. As in accordance with the real specifications exposed in subsection 3.1, the material was modelled as 16-ply conventional shell, with each ply being at a 0° orientation and having 0.25 mm in thickness, resulting in a total material thickness of 4 mm. In Appendix J, images related to the material's properties and composite layup definition within the ABAQUS software are presented. A static general step was considered, with a time period of 1 and NLGEOM turned off. As for the boundary conditions, a similar rationale was applied for every test. On one of the ends of the specimen was restricted through an ENCASTRE boundary condition, restricting the movement and rotation along all axis, simulating the end of the specimen that is fixed immobile to the machine. On the other end, a displacement boundary condition was applied dependent on the simulated test, restricting movements across all axis except for the vertical axis. In tensile testing (Figure 93a), a positive displacement of 2 mm was applied, on compressive testing (Figure 93b) it was a negative displacement of 0.2 mm, and on shear testing it was a negative displacement of 0.45 mm (Figure 93c). In this end of the specimen, the boundary condition was applied to a reference point, that was coupled to the respective surface of the specimen through a coupling constraint.

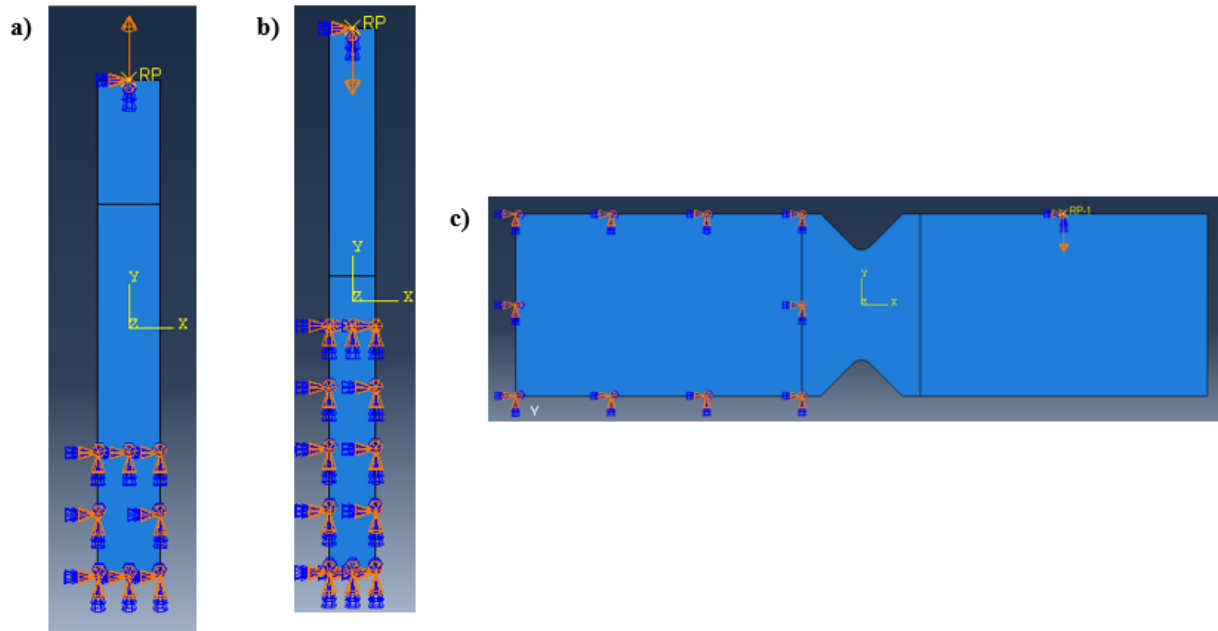


Figure 93 - Applied boundary conditions on FEA models: (a) tensile; (b) compressive); (c) shear.

In the mesh module, the seed size was set as considering the limitation of the ABAQUS student edition, that restricts the maximum number of nodes to 1000. In the shear specimen, the mesh controls were set to structured, so that the resultant mesh was not deformed due to the shape of the V-notches. The meshed specimens are presented in Figure 94a, b and c for the tensile, compressive and shear specimens, respectively. In every model, the element type was set to S4R with element deletion turned on.

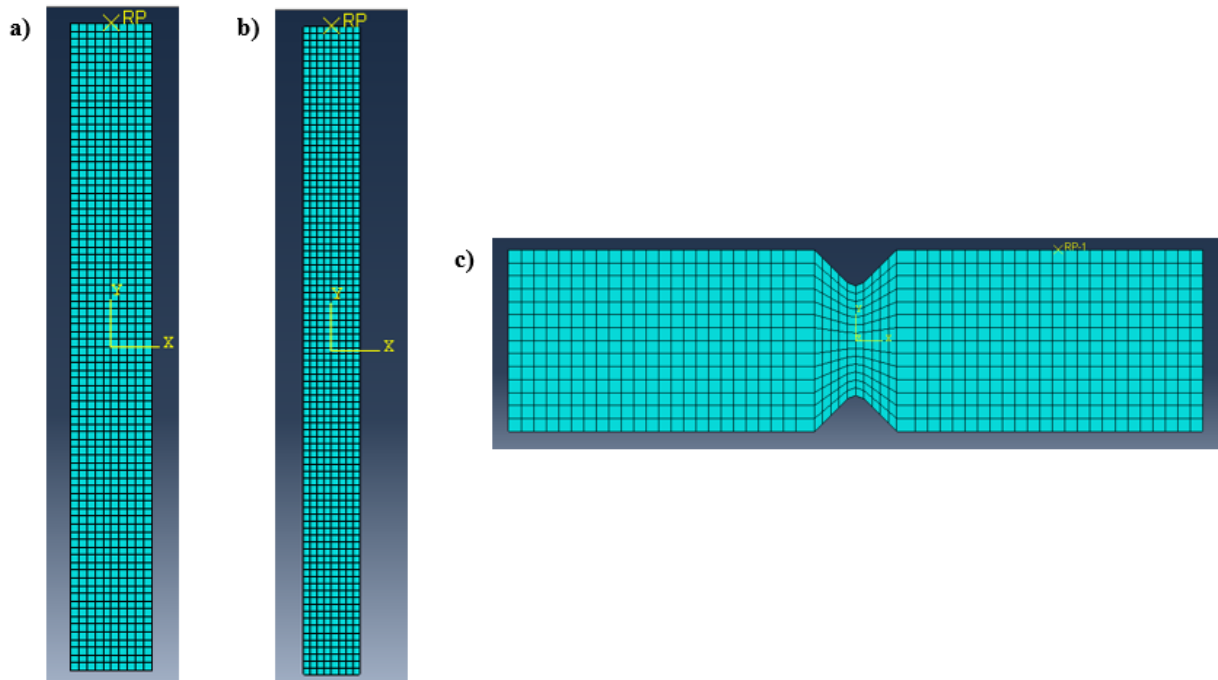


Figure 94 - ABAQUS specimen's after meshing: (a) tensile; (b) compressive; (c) shear.

5.2.FEA and Experimental Results Comparison

In order to assess the validity and accuracy of the obtained results through the FEA models, a comparison with the experimental results will be performed. In the case of both tensile and compressive tests, this will be achieved through comparing the force-displacement curves, while in the shear tests will be through the force-strain curves.

5.2.1.Tensile Results

The presented experimental curves result from the synchronization of the force data obtained from the testing machine's load cell and the displacement data that is resultant from DIC. This displacement data is obtained through multiplying the obtained strain along the vertical axis and the specimen's gage length. The FEA model's force-displacement curves were obtained through the outputs of force and displacement at the reference point. It should be mentioned that the presented force-displacement curves consider the test until specimen's fracture at peak force. In Figure 95, the experimental and FEA model's tensile force-displacement curves are represented. The only experimental force-displacement curve is the one correspondent to specimen 1 because, as it was seen in subsection 4.1.1, it was the only specimen that produced a valid fracture. In Table 23, both peak force and displacement at peak force values are represented, along with a calculation of the error between the experimental and the FEA model's results.

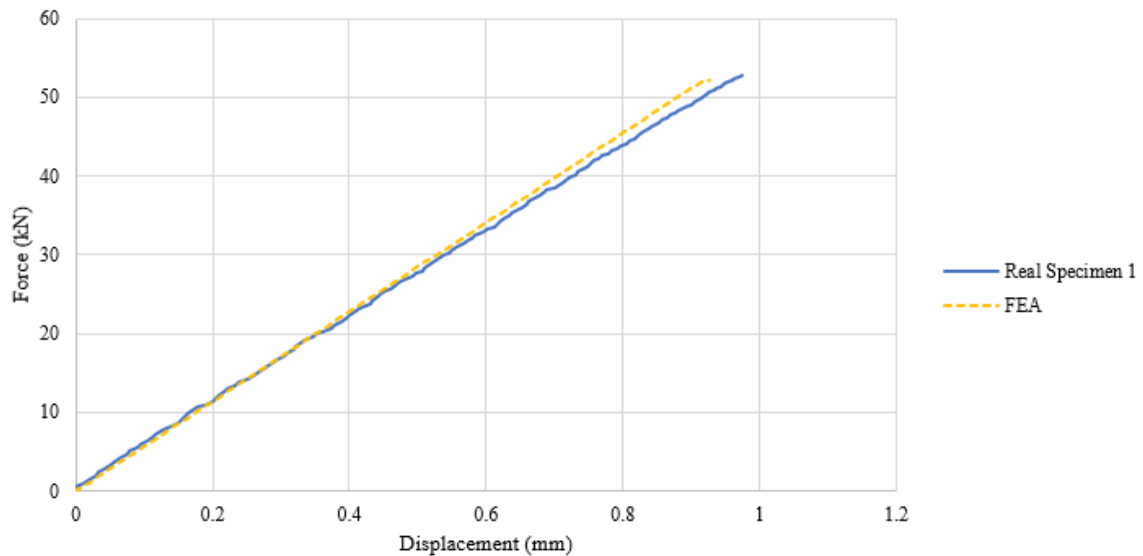


Figure 95 - Comparison between tensile force-displacement curves for experimental specimen and FEA model.

Table 23 - Experimental and FEA model tensile results for peak force and displacement at peak force.

Parameter	Experimental	FEA	Error
Peak force (kN)	52.693	52.104	1.12 %
Displacement at peak force (mm)	0.974	0.927	4.76 %

The results show a good correlation between experimental and numerical results. The peak force and displacement at peak force error values show that the FEA results are within 1.12 % and 4.76 % of the experimental values. The slight deviations for larger displacements (above 0.6 mm) may be attributed to the dimensions used in the FEA models being the nominal dimensions, while the dimensions of the experimental specimens do not ever meet the nominal values, as it can be seen from the several specimen dimensions controlled in

Appendix A. Apart from that, the materials used to produce experimental specimens are always subject to imperfections and defects that compromise their homogeneity, which was not considered when making the material's FEA model.

5.2.2.Compressive Results

For compressive testing, a similar procedure for obtaining both the experimental and FEA model's tensile force-displacement curves exposed in subsection 5.2.1 was followed. In Figure 96 the force-displacement curves for the 3 compressive specimens are presented, along with the curves correspondent to the two FEA models. One of these models considers the properties that were mentioned in Table 22, and the other considers the average of the compressive modulus determined for the 3 testing specimens in subsection 4.1.2 of 51.241 GPa, instead of the tensile Young's modulus value. In Table 24 and Table 25, the peak force and displacement at peak force values are compared to both FEA models. It should be mentioned that in both tables, average values of both the peak force and displacement are considered for the experimental results, instead of the values obtained for each individual testing specimen.

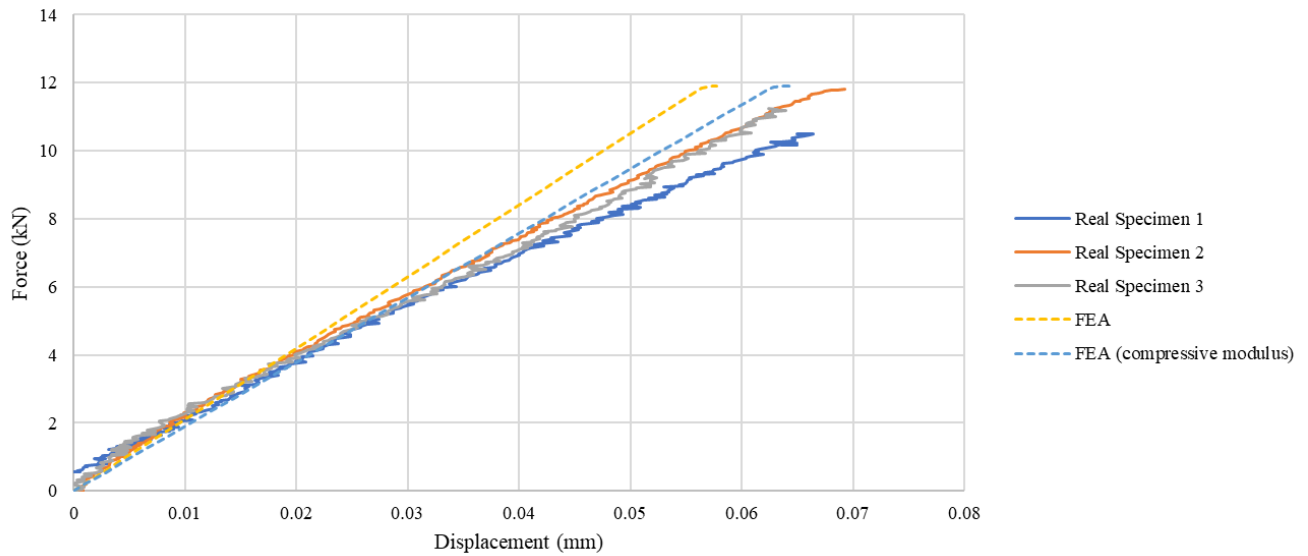


Figure 96 - Comparison between compressive force-displacement curves for experimental specimens and FEA models.

Table 24 - Experimental and FEA model compressive results for peak force and displacement at peak force.

Parameter	Experimental average	FEA	Error
Peak force (kN)	11.177	11.911	6.57 %
Displacement at peak force (mm)	0.0665	0.0577	13.19 %

Table 25 - Experimental and FEA model (considering compressive modulus) compressive results for peak force and displacement at peak force.

Parameter	Experimental average	FEA (compressive modulus)	Error
Peak force (kN)	11.177	11.905	6.52 %
Displacement at peak force (mm)	0.0665	0.0644	3.22 %

From the presented results there is an agreement between both FEA models curves and the experimental curves, with the curve obtained from using the material's compressive modulus seemingly being more representative of the experimental specimens. As for the peak force values, changing the modulus had a negligible impact upon the measured value, because the peak force is mainly dependent upon the material's strength. However, there was an increase in accuracy for the displacement at the peak force value, from an error of 13.19 % to 3.22 %. The increased accuracy when considering the compressive instead of elastic modulus when modelling the material's properties is mainly associated with software assuming that the elastic modulus is equal to the compressive modulus, which as it can be seen from the results obtained in subsection 4.1.2, is not the case with the present material. Thereby, it can be assumed that, most likely, if when modelling the material the compressive modulus could be defined separately to the elastic modulus, an increase in the FEA model's accuracy would be observed. The observed deviations between the experimental and FEA models' curves can partly be attributed to the same reasons that were exposed in subsection 5.2.2, such as the difference between the dimensions of the real specimens and the nominal dimensions and the assumption of a homogenous defect-free material.

5.2.3. Shear Results

For the comparison of the shear results the force-strain curves were used. In the case of the experimental curves, they were obtaining through synchronizing the force data from the machine's load cell with the DIC data, as it was explained in further detail in subsection 3.2.3. Regarding the FEA model, the force data was obtained as a direct output at the reference point, while the strain data was calculated through simulating virtual strain gages. This was achieved by obtaining the coordinates data from the points A, B, C and D highlighted in Figure 97, where one of the virtual strain gages is A-B and the other is C-D. Their initial distance (l_0) and distance at the various step increments (l) was calculated. With equation 5.1, it was then possible to obtain the strain (ε) along the two directions A-B and C-D, and through equation 3.5 in section 3.2.3, the shear strain was obtained in a similar manner to the experimental test.

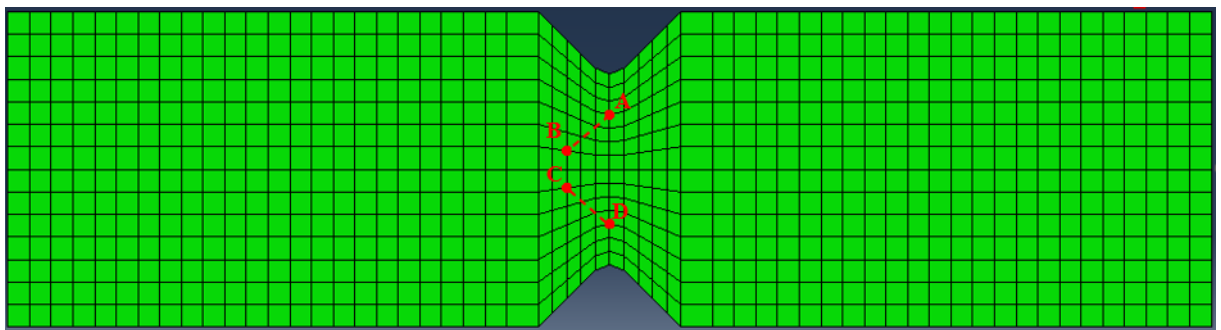


Figure 97 - Points used in the calculation of shear strain of shear FEA model.

$$\varepsilon = \frac{l - l_0}{l} \quad (5.1)$$

With the FEA model' strain data calculated, it was possible to reach the force-strain curve, which is represented along with the experimental curves of the three specimens in Figure 98.

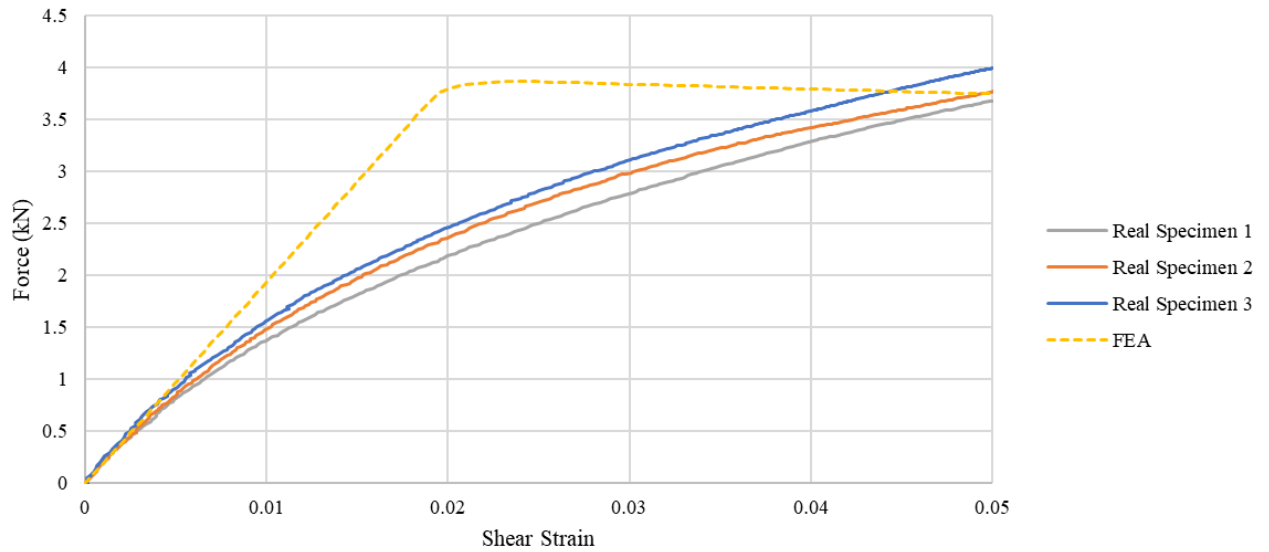


Figure 98 - Comparison between shear force-strain curves for experimental specimens and FEA models.

A mismatch between the FEA model and the experimental curves is observed. Whereas the FEA model presents a linear tendency up to peak force, the experimental curves exhibit the non-linear behavior of the material when loaded in shear. This difference could partly be attributed to the severe specimen deformation that was observed in subsection 4.1.3, resulting in the testing specimen not being under a pure shear loading condition, particularly at higher strains. It is also possible that material failure (stiffness degradation) has occurred as the specimen was deformed, leading to a non-linear behaviour of shear stiffness. Through Figure 99, which represents the comparison of the force-strain curves below a shear strain of 0.01, it can be noticed that the material seems to be under a pure shear loading condition particularly until around a shear strain of 0.05, but as the strains increase, this condition is no longer verified.

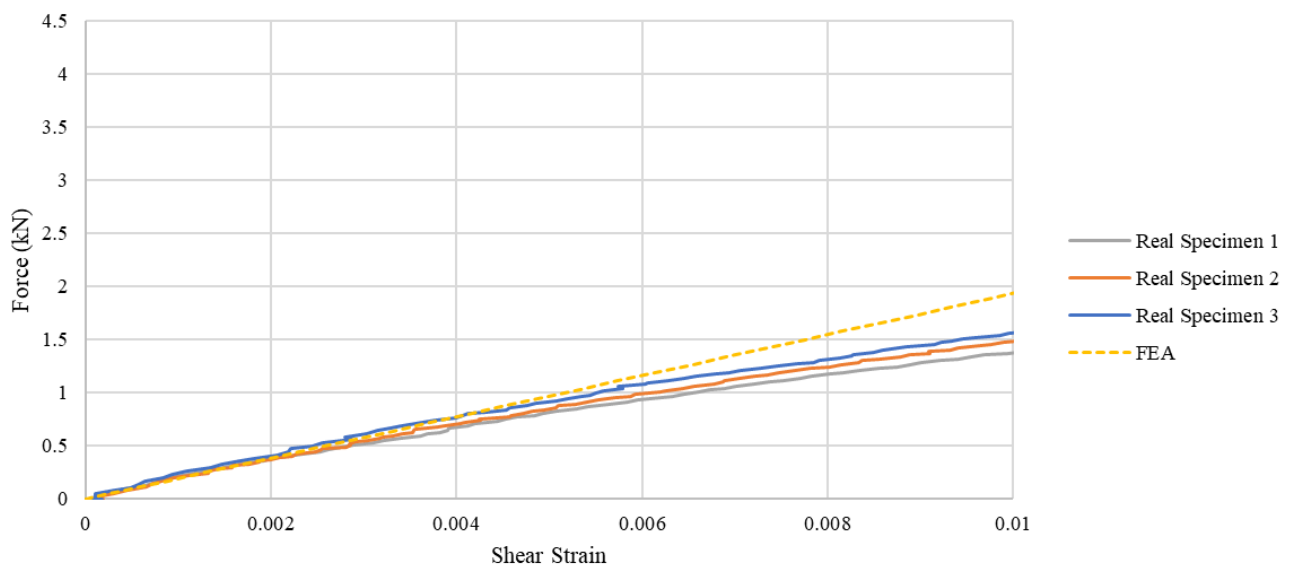


Figure 99 - Comparison between experimental and FEA model' shear force-strain curves for shear strain values below 0.01.

Table 26 Exhibits the peak force value for the FEA model and the average peak force value for the three testing specimens.

Table 26 - Peak force values for FEA model and experimental shear tests.

Parameter	Experimental average	FEA	Error
Peak force (kN)	3.819	3.870	1.329 %

From the presented results it is attainable the peak force value for the FEA model and experimental results are within 1.329 % of each other. However, these values are obtained for very distinct shear strains. With these observations, it can be concluded that the presented FEA model does not accurately depict the shear behavior of the tested material for shear strain values above 0.005, as it presents a linear shear response until fracture while the experimental specimen exhibits a non-linear shear response. A possible solution would be using a non-linear shear behavior model, as several authors have done [144-146], as it would more accurately depict the material' shear behavior.

Chapter 6

Conclusions and Future Works

6.1. Conclusions

In the present master's dissertation, the drilling of a CFRTP material using WC-Co CVD diamond coated tools was experimentally investigated. Throughout this analysis, the influence of the drill tool, through its geometry and coating, and the drilling parameters, feed and cutting speed, was studied. Comparisons using the cutting loads (thrust force and drilling torque), and hole quality parameters, such as delamination, burr, hole accuracy and hole's surface quality, were conducted. A chip analysis was also performed to better understand the drilling process in these materials, as well as wear analysis to access the wear patterns within the tested CVD diamond coated tools. The CFRTP material's tensile, compressive and shear properties were also determined, so that an FEA model of the material could be developed for further delamination assessment and drilling simulation analysis. The main conclusions from the developed work will be presented as follows.

Regarding the cutting loads, it was found that the feed was the most influential cutting parameter towards peak thrust force and peak drilling torque, with an increase in feed leading to an increase in both peak loads. The drill's coating was also more influential than the geometry towards the peak thrust force, with the MCD coated tools producing lower peak thrust forces than their NCD counterparts, particularly at higher cutting speeds, which is most likely attributed to lower CER of the MCD coated tools.

As for the hole quality indicators, it was found that an increase in feed led to an increase in delamination damage, as it increased the peak thrust force. The burr results indicated that higher hole quality was obtained for higher feeds, particularly when combined with higher cutting speeds. The conventional twist drill geometry usually resulted in holes with less burr than the double-point angle twist drill geometry, while the MCD coated tools generally performed better than their NCD coated counterparts. More accurate holes were produced with MCD coated tools, particularly at higher feeds and cutting speeds, where the NCD tools produced holes that always had a larger diameter than nominal. For example, at 130 m/min, the NCD tools presented deviations of between 15 to 25 μm and the MCD tools between 3 and 12 μm . This difference is thought to be associated with the NCD coating being thicker, having a higher likelihood of producing higher diameter drills, that consequently drill higher diameter holes. Higher cutting speeds seemed to result in larger diameter holes with higher deviations from the nominal diameter, which is probably related with more severe machine vibration within these conditions. The conventional twist drill geometry with a MCD

coating at a feed of 0.10 mm/rev and a cutting speed between 100 and 130 mm/rev seems to be the most performative solution when all damage types are considered, as it minimizes burr, while not presenting a severe delamination factor increase versus other cutting parameters and tools, and producing holes whose diameter is within $\pm 50\text{ }\mu\text{m}$ of the nominal value.

Throughout the drilling experiments, the continuous ribbon-like chips produced tended to clog the drill. These types of chips are probably associated with the ductile nature of the PA12 resin coupled with the likely exceedance of its glass transition temperature during the drilling process. At lower feed values, the chips exhibited a curlier morphology in comparison to the more continuous at higher feeds, which is associated with the lower chip thickness at the lower feeds.

During wear analysis of a conventional twist drill with a MCD coating, the drill exhibited no signs of wear until its preparation for SEM analysis after 650 drilled holes. During this analysis, the presence of abrasion on the flank face near the cutting edge was noted, as well as coating delamination, whose appearance might have been expedited through the drill's preparation for the SEM analysis. It is possible that workpiece material may have been adhered to the tool's edge, hindering the observation of delamination, which was exposed after ultrasonic bath. On the 300 holes drilled beyond the 650 holes, the coating delamination exhibited a tendency to grow along the direction of the cutting edge.

The material's tensile and compressive properties were successfully determined. In the shear tests, severe specimen deformation was observed, compromising the pure shear loading condition and consequently the accuracy of the determined properties. The produced FEA models using Hashin's criterion for damage accurately depicted the material's behavior under tensile and compressive loads, but not under shear loadings, where further testing is required. Regardless, the produced material model could serve as a base for a future drilling process FEA model.

6.2.Future Works

After the development of this work, several other aspects related to this theme could be approached to both enhance the obtained results and expand the knowledge regarding the studied theme. Some suggestions could be the following:

- A drilling experimental analysis using thermal sensing equipment would provide a deeper insight into the drilling process, as several authors have found it to explain several aspects when drilling CFRT materials [116, 117, 120];
- Using higher feeds than 0.15 mm/rev and cutting speeds higher than 130 m/min, as both burr formation and hole's surface roughness improved with more aggressive parameters, while delamination and hole's accuracy did not deteriorate severely;
- Extending wear analysis to all tested drilling tools to verify if the tendencies observed in a MCD coated tool would be the same as in an NCD coated tool, if the tool's geometry influence is significant and the impact of varying either of the cutting parameters;
- Analyzing the impacts of tool wear towards hole quality indicators, such as delamination, burr and hole accuracy;
- Using a thicker plate to produce specimens for the DCB fracture testing procedure, as it would increase the beam's bending stiffness and possibly allow the fracture energy to be determined, so that the critical thrust force values could be estimated;
- Applying a different shear testing procedure, such as the one described in ASTM D 7078 (that has been claimed to produce better results in materials with the same architecture as the one tested [147]), to compare with the obtained results and access the accuracy of the determined shear properties;

- Using a non-linear shear behavior model in the FEA of shear testing, such as several authors have done [144-146], as it would probably more accurately represent the material's shear behavior;
- Conduct drilling numerical simulations using the FEA model developed for the material.

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Appendix A: Specimen's Dimensions

The tensile, compressive and shear specimen's dimensions are presented in Table 27, Table 28 and Table 29, respectively.

Table 27 - Tensile specimen's dimensions.

Specimen	Width (mm)				Thickness (mm)			
	1	2	3	Average	1	2	3	Average
1	24.437	24.424	24.414	24.425	4.149	4.128	4.151	4.143
2	24.734	24.745	24.729	24.736	4.129	4.123	4.133	4.128
3	24.436	24.421	24.470	24.442	4.147	4.134	4.144	4.142

Table 28 - Compressive specimen's dimensions.

Specimen	Width (mm)				Thickness (mm)				Length (mm)
	1	2	3	Average	1	2	3	Average	
1	10.642	10.653	10.610	10.635	4.156	4.139	4.148	4.148	139.88
2	10.959	10.981	11.017	10.986	4.142	4.138	4.122	4.134	140.44
3	11.037	11.021	1.025	11.028	4.135	4.119	4.123	4.126	139.39

Table 29 - Shear specimen's dimensions.

Specimen	Width (mm)	Thickness (mm)
1	12.188	4.081
2	12.082	4.101
3	12.055	4.110

Appendix B: Fracture Testing

B.1. Testing Methodology

To determine the material's pure mode I fracture energy, a DCB tests were conducted. This testing procedure used a specific fracture testing equipment, represented Figure 100. The values of force and displacement were recorded. In specimen, the thickness of both beams ($2h$), the width (b) and the pre-crack length (a_0) pictured on Figure 101 were measured using a pachymeter to an accuracy of 0.05 mm.

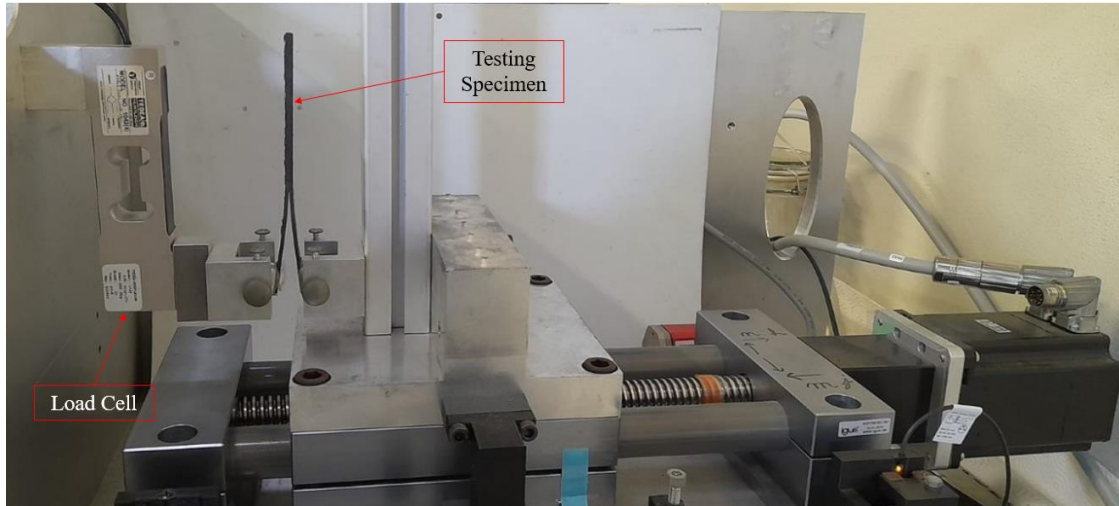


Figure 100 - DCB testing setup.

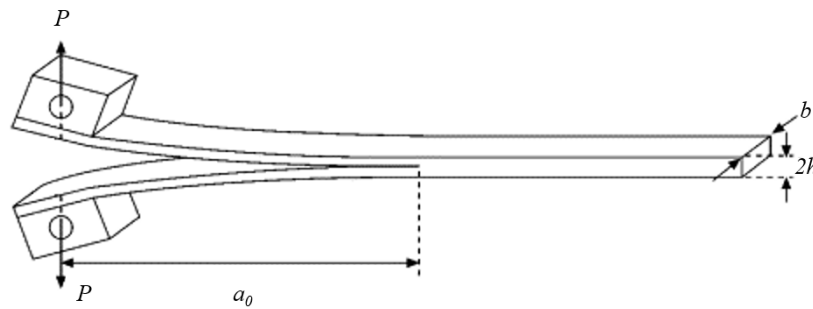


Figure 101 - Schematic of DCB testing with relevant dimensions (adapted from [148]).

These testing specimens were manufactured from the composite plate with the pre-crack. They were firstly cut from the plate, using the same sawing machine from Wendt GmbH that was used to manufacture the specimens for the tensile, compressive and shear tests. After cutting the specimens from the plate, aluminum cubes with holes of 6 mm in diameter were adhesively bound to the cut specimen using Araldite 2051 (whose spec sheet is present on H.1). Before applying the adhesive, the surfaces of both the aluminum cubes and the composite specimen were sanded and cleaned with acetone. A Teflon sheet was also placed inside the pre-crack, to prevent the adhesive from gluing both specimen's beams together. After the adhesive was applied, the specimens were left to cure for at least 30 minutes at room temperature.

As described in more detail in B.2 all specimens produced invalid test results. However, the procedure and rationale for each specimen will be presented next.

The first DCB specimens to be produced were specimens 1 and 2, presented in Figure 102a and b, respectively. The only differences between them were the pre-crack length, with specimen 1 having a longer pre-crack than specimen 2.

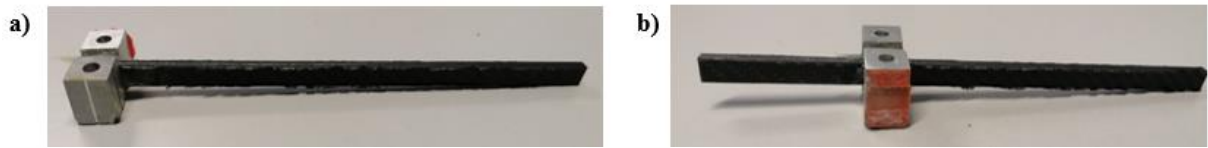


Figure 102 - First DCB testing specimens: (a) specimen 1; (b) specimen 2.

As both specimen 1 and 2 failed in testing, the pre-crack length of specimen 3 was reduced to improve the beam's flexural rigidity. Due to the failure in testing of specimen 3, a razor blade was inserted into the pre-crack of specimen 4 before the DCB blocks were glued to the specimen to try and facilitate the crack's propagation, as it is represented in Figure 103.



Figure 103 - Usage of a razor blade to facilitate crack propagation before testing DCB specimen 4.

Because specimen 4 also produced invalid results, the width was increased from for specimen 5, present in Figure 104, to increase its flexural rigidity. The razor blade was also used for this specimen.



Figure 104 - DCB testing specimen 5.

As specimen 5 also failed during the DCB test, the fracture test was switched to a CT. As the thickness of the composite plate did not allow for the production of specimens according ASTM D 5045 testing standard, a similar procedure to the one presented by Wang et al. [149] was followed, in which a piece of the composite plate is cut to shape and then glued onto two steel holder blocks to make up the remaining parts of the testing specimen, as it is schematized on Figure 105. The holder blocks' technical drawing is presented in I.1.

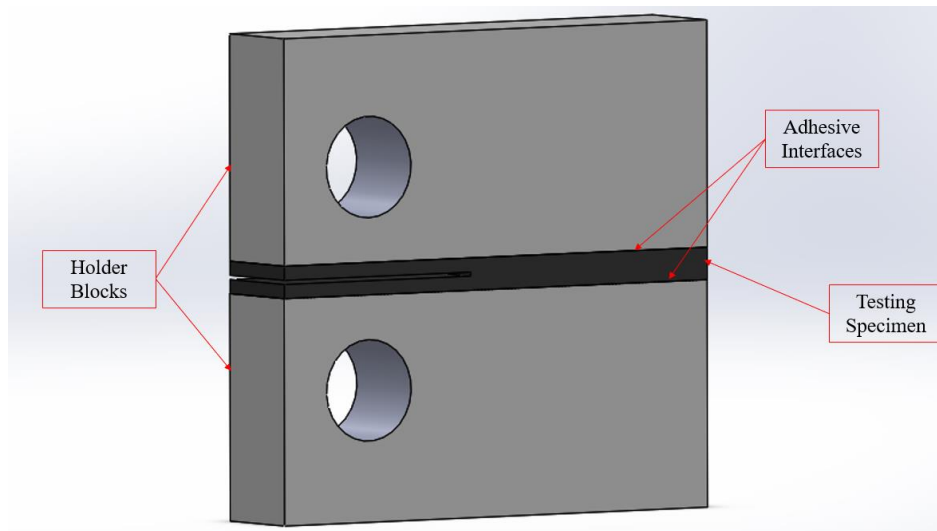


Figure 105 - CT testing specimen.

A total of 8 testing specimens were produced, and all presented invalid test results in the same matter, as reported in B.2. The procedure and rationale for each specimen will, regardless, be exposed next.

The dimensions of the specimens were in part determined by the blocks, with a length of 62.5 mm and a width of 10 mm, and the original plates, having a thickness of 4 mm. The pre crack length was set to 27.5 mm. The testing was performed in an MTS 810 machine, at a speed of 0.5 mm/min using a load cell that was acquiring force and displacement data at a rate of 15 Hz, represented on Figure 106a. A total of three specimens were firstly produced, numbered 1 to 3. The adhesive used was Araldite 2051. Both the blocks and the specimen were firstly sanded and then cleaned with acetone. A small layer of adhesive was applied to the specimen and the block, after which they were glued together, and left to cure for at least 24 hours. A Teflon sheet was placed in the pre-crack, similarly to before. The final specimens are presented in Figure 106b.

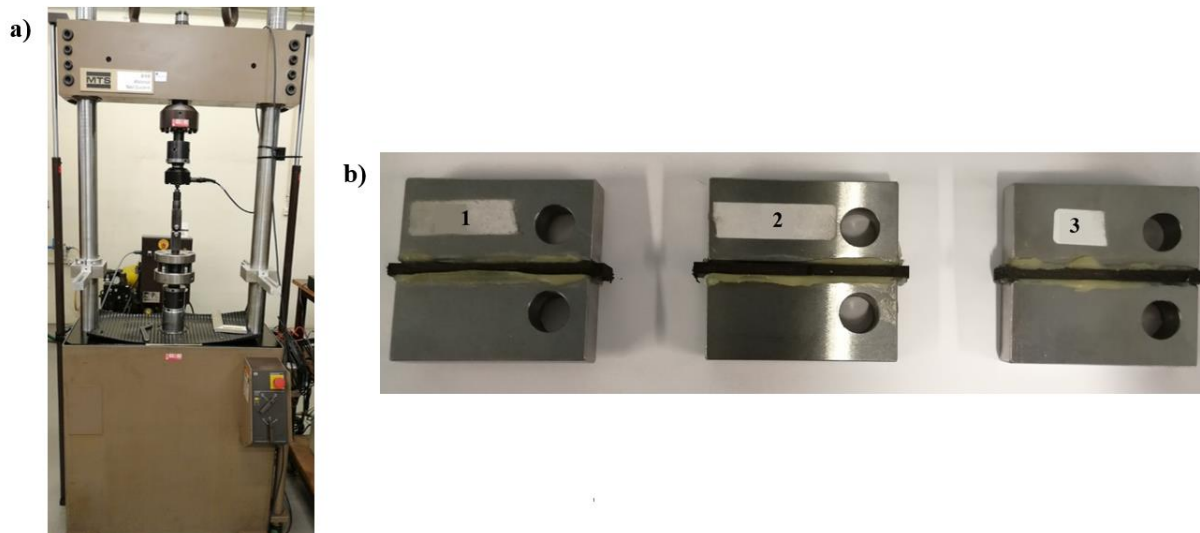


Figure 106 - CT testing: (a) MTS 810 machine; (b) specimens 1 to 3.

Following the first specimens' testing failure, a plasma surface treatment was applied in the preparation of the next specimens, using an arcojet plasmagenerator PG 051, presented in Figure 107a. This treatment increases the surface tension of the composite, thereby

increasing the adhesive's adhesion to the surface. Three specimens were produced and are presented in Figure 107b, numbered 4 to 6.

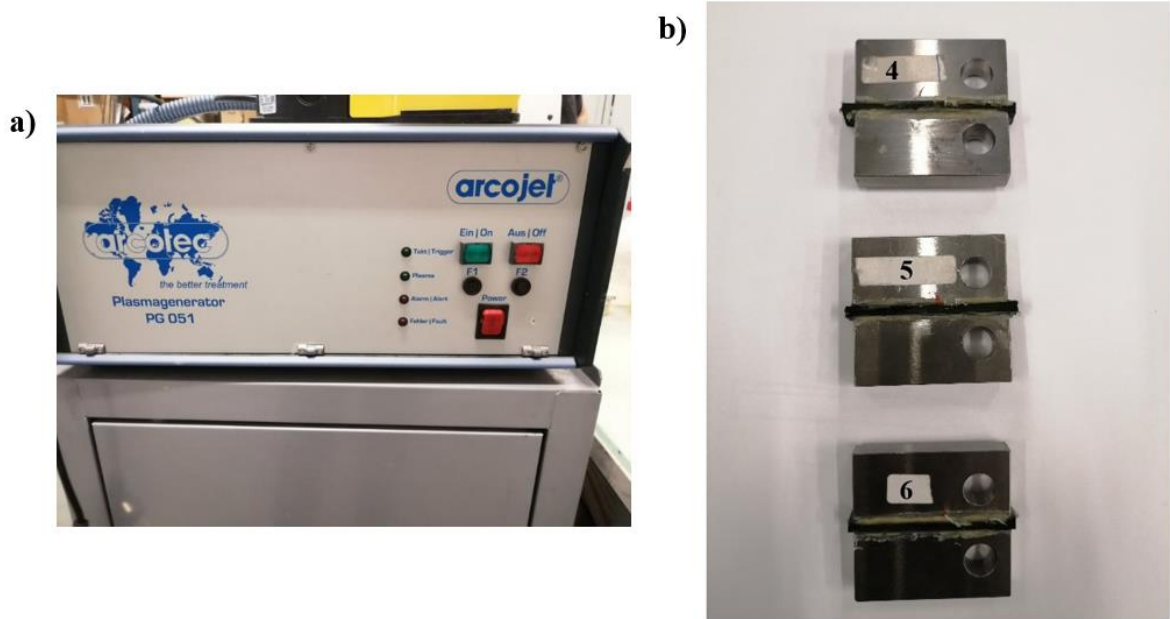


Figure 107 - CT testing: (a) arcojet plasmagenerator PG 051; (b) specimens 4 to 6.

Two final specimens were produced using different adhesives, namely an Araldite 420 A/B (presented in H.2) and an Araldite AV138M-1 (presented in H.3), numbered 7 and 8, respectively. The holder blocks were sandblasted instead of being sanded, but rest of the procedure remained unaltered. On Figure 108, the two produced testing specimens are presented.

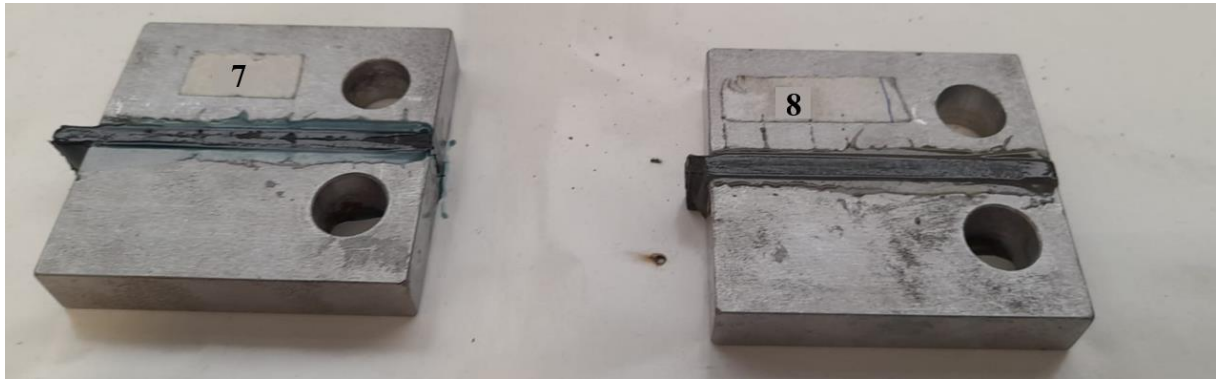


Figure 108 - CT testing specimens 7 to 8.

A final DCB test was attempted following the failure of the last CT specimens. For this, two long steel blocks were produced (whose technical drawing is presented in I.2), covering almost the entire length of the 200 mm composite specimen, with the intent of increasing the beam's flexural rigidity. The procedure followed was the same as for the first CT tests, meaning both the blocks and specimen's surfaces were sanded, after which a layer of Araldite 2051 was applied to both. The tests were conducted in the same machine as the CT tests, as it is presented in Figure 109.

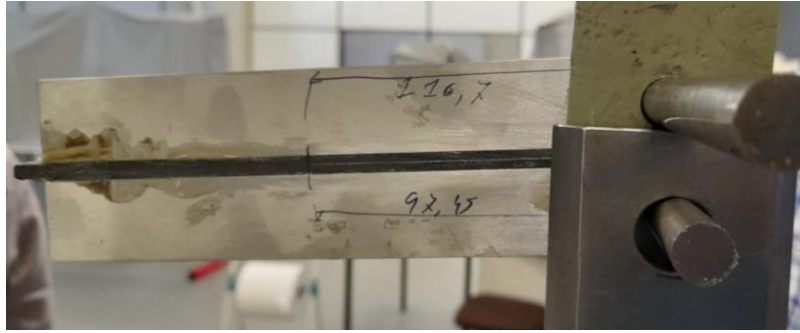


Figure 109 - Final DCB test specimen.

B.2.Results

The first tests conducted for the determination of the fracture energy were the DCB tests. A total of 5 specimens were used in this first tests, numbered from 1 to 5 in Table 30, where their dimensions are specified.

Table 30 - First DCB tests specimen dimensions.

Specimen	b (mm)	$2h$ (mm)	a_0 (mm)
1	9.30	4.20	104.00
2	9.30	4.30	46.10
3	9.10	4.30	14.70
4	9.30	4.30	61.50
5	19.70	4.30	28.00

The first specimens that were produced and tested were 1 and 2. These were identical in every aspect except in the pre-crack length. During testing of specimen 1, the beams started to flex as the displacement was increasing, as it is visible in Figure 110a, until one of the specimens beams broke at the crack's front before the crack started to propagate, as it is presented on Figure 110b, and thereby the test was deemed as invalid. Specimen's 2 testing also produced invalid results due to the same issue.

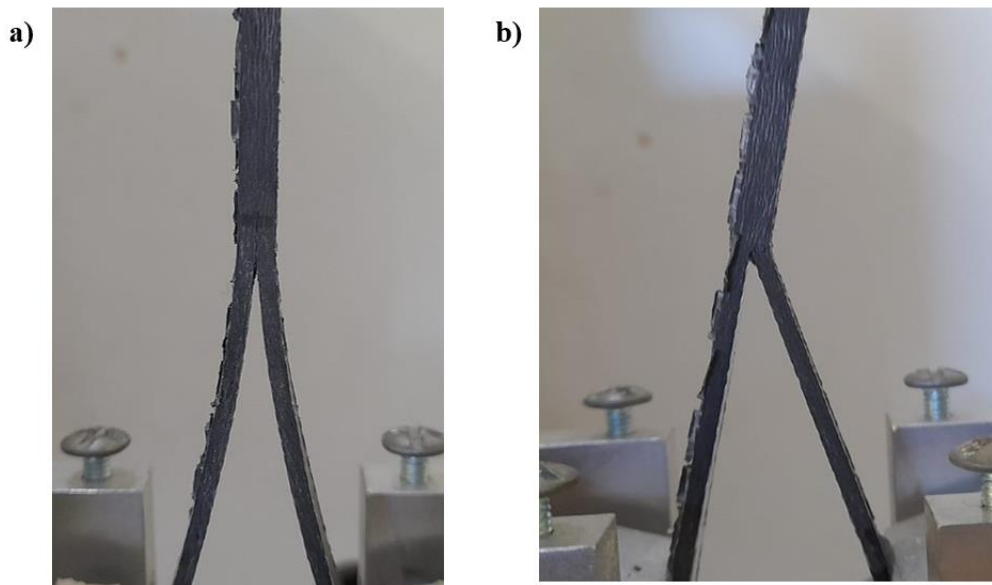


Figure 110 - DCB specimens during testing: (a) test specimen with flexing beam; (b) test specimen with broken beam.

All the following attempts in specimens 3 to 5 to increase the beam's flexural rigidity proved ineffective, as the specimens failed in the same matter. Figure 111 represents specimen 5 after failure.



Figure 111 - DCB specimen 5 fractured after being removed from the testing machine.

Regarding CT testing, the difference between the specimens is highlighted through Table 31. All 8 specimens presented a similar issue, which is presented in Figure 112, where the specimen separated between the composite and the adhesive interface. Because of this, all specimens from 1 to 8 had invalid test results. It should be mentioned, however, that the peak force at which the adhesive interface failure occurred was higher in specimens 4 to 6 when compared to specimens 1 to 3, which proves that the plasma surface treatment did improve the adhesion to the composite material. In specimens 7 and 8, the peak force was also higher than in specimens 4 to 6, proving that the adhesives provided a better performance, but not enough for the crack to propagate.

Table 31 - CT testing specimens.

Specimen	Adhesive	Plasma Surface Treatment
1 to 3	Araldite 2051	No
4 to 6	Araldite 2051	Yes
7	Araldite 420 A/B	Yes
8	Araldite AV138M-1	Yes



Figure 112 - CT specimen 5 after failure.

A final DCB test was made. As it can be seen in Figure 113, during the testing of the specimen the same issue that was seen during CT testing was already starting to occur when there was not any crack propagation. In Figure 114, it is possible to see that the test failed due to the fracture between the composite and the adhesive interface occurring before the crack's propagation. With this, it was not possible to obtain the material's fracture energy.

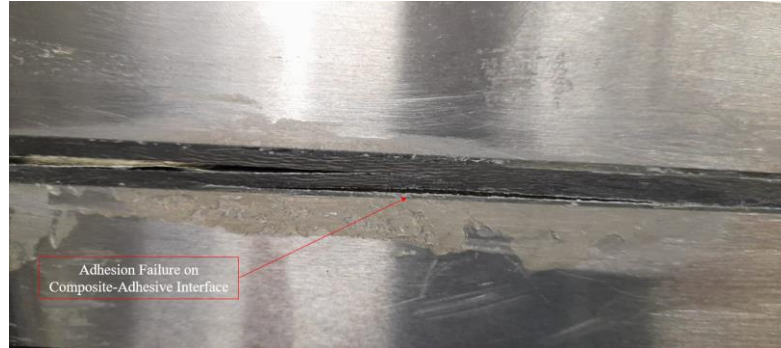


Figure 113 - Final DCB test with adhesion failure in the composite-adhesive interface.



Figure 114 - Final DCB specimen after failure.

[illegible]

Appendix D: Parametric Research

Author	Cutting Speed (m/min)	Feed (mm/rev)
Marques [129]	80, 100, 120	0.03, 0.05, 0.07
Feito et al. [36]	25, 50, 100	0.05, 0.10, 0.15
Sugita et al. [88]	150.7	0.03
Yu et al. [14]	15.08, 28.3, 56.55	0.025, 0.05, 0.1
Raj and Karunamoorthy [150]	60	0.05-0.15
Heisel et al. [151]	42.7	0.05, 0.10, 0.15, 0.20, 0.30, 0.40
	21, 43, 64, 85, 128, 171, 214, 256, 299, 342, 385, 427, 470, 513	0.1, 0.15, 0.2
Qiu et al. [152, 153]	28.274, 65.97, 103.67	0.01, 0.02, 0.03, 0.04, 0.05
Geier et al. [28]	50, 64.6, 100, 135.4, 150	0.07, 0.1037, 0.185, 0.2663, 0.3
Shyha et al. [80]	15, 45	0.1, 0.2
Fu et al. [154]	150	0.05
Wang et al. [155]	94.25	0.02, 0.06
Ramirez et al. [156]	100	0.05
Wang et al. [157]	75.4, 113.1, 150.8, 188.5	0.01, 0.02, 0.03, 0.04
Caggiano et al. [158]	53.86, 119.7, 149.62	0.11, 0.15, 0.20
Kuo et al. [135]	50, 75	0.05, 0.1, 0.15
Ameur et al. [85]	56.55, 113.1, 169.65	0.005, 0.01, 0.02, 0.03, 0.04, 0.05
Feito et al. [46]	25, 50, 100	0.05, 0.1, 0.15
Iliescu et al. [159]	56.55, 113.10, 169.65, 226.19	0.05, 0.1, 0.15
Xia et al. [160]	60	0.025, 0.05
Henerichs et al. [161]	90, 150	0.06, 0.10
Geier et al. [19]	50, 65, 100, 135, 150	0.035, 0.043, 0.064, 0.078, 0.093
Al-Wandi et al. [86]	62.83, 125.66, 188.5, 251.33	0.05, 0.10, 0.125

Eneyew et al. [162]	29.924, 59.85, 89.77, 119.7	0.064, 0.128, 0.192, 0.256, 0.320
Merino-Pérez et al. [163]	50, 100, 150, 200	0.05
Hrechuk et al. [95]	100	0.02

Appendix E: Drilling Campaign Parameter Combinations

Drill Geometry	Drill Coating	Feed (mm/rev)	Cutting Speed (m/min)
Conventional	MCD	0.05	70
			100
			130
		0.10	70
			100
			130
		0.15	70
			100
			130
		0.05	70
			100
			130
Conventional	NCD	0.10	70
			100
			130
		0.15	70
			100
			130
		0.05	70
			100
			130
		0.10	70
			100
			130
Double-Point	MCD	0.05	70
			100
			130
		0.10	70
			100
			130
		0.15	70
			100
			130
		0.05	70
			100
			130

Double-Point	NCD	0.05	70
			100
			130
		0.10	70
			100
			130
		0.15	70
			100
			130

Appendix F: Loads Outlier Correction Campaign

The results peak thrust force for the full-factorial drilling campaign are represented in Figure 115 and Figure 116.

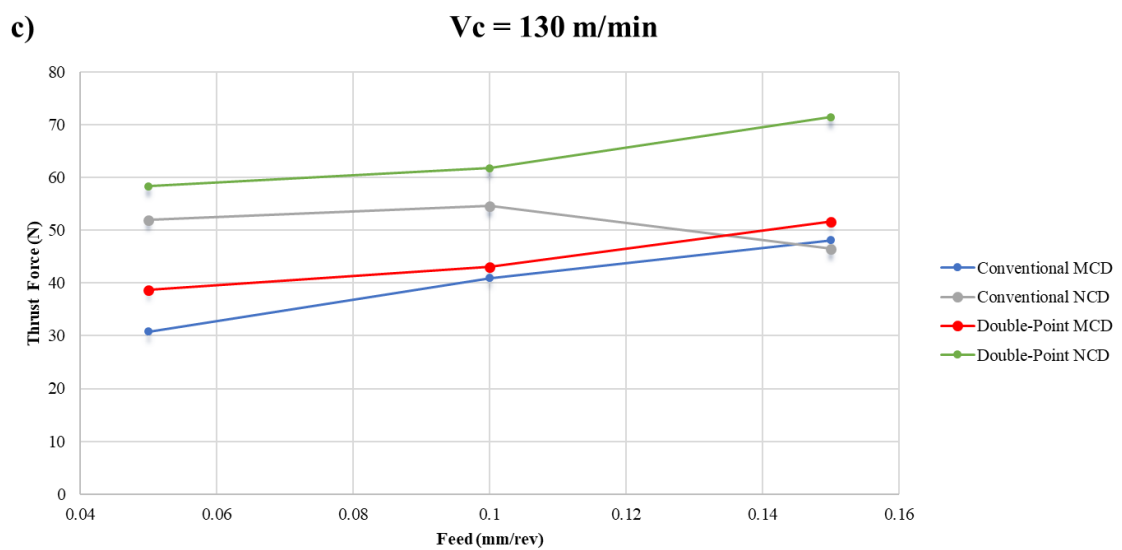
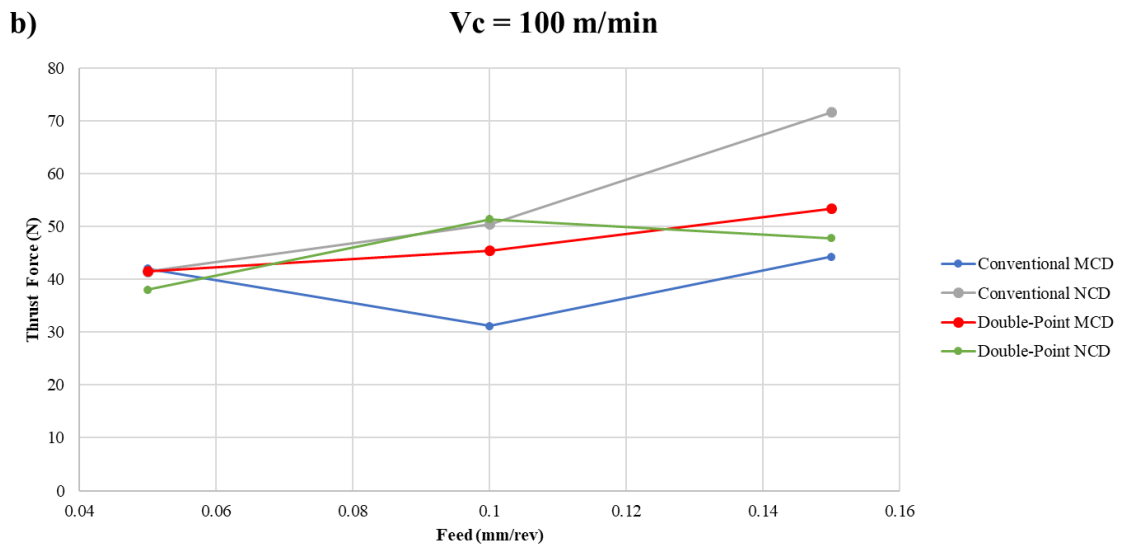
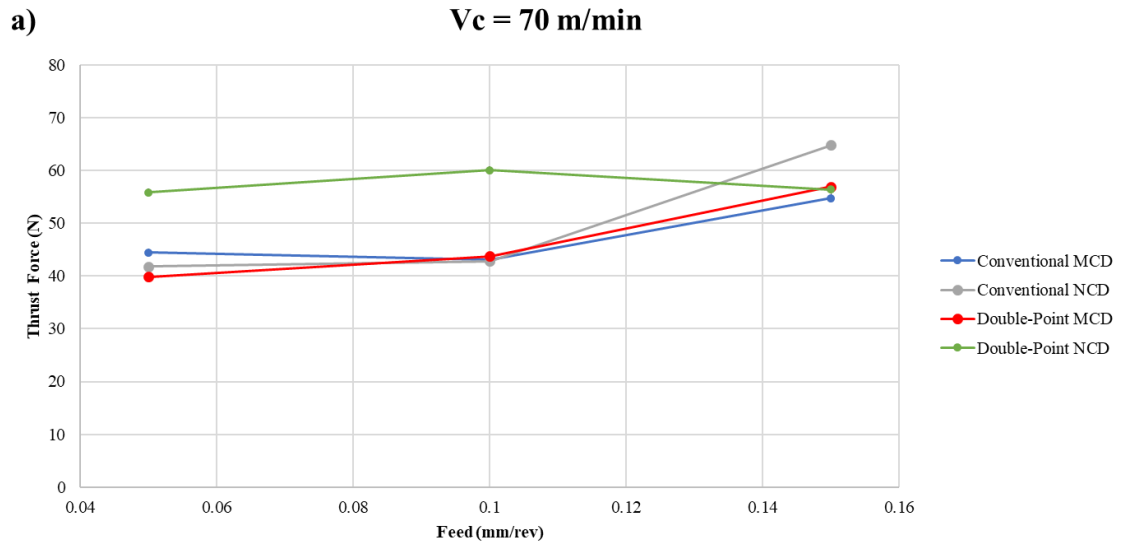


Figure 115 - Peak thrust force values at various feeds for certain cutting speed: (a) 70 m/min; (b) 100 m/min; (c) 130 m/min.

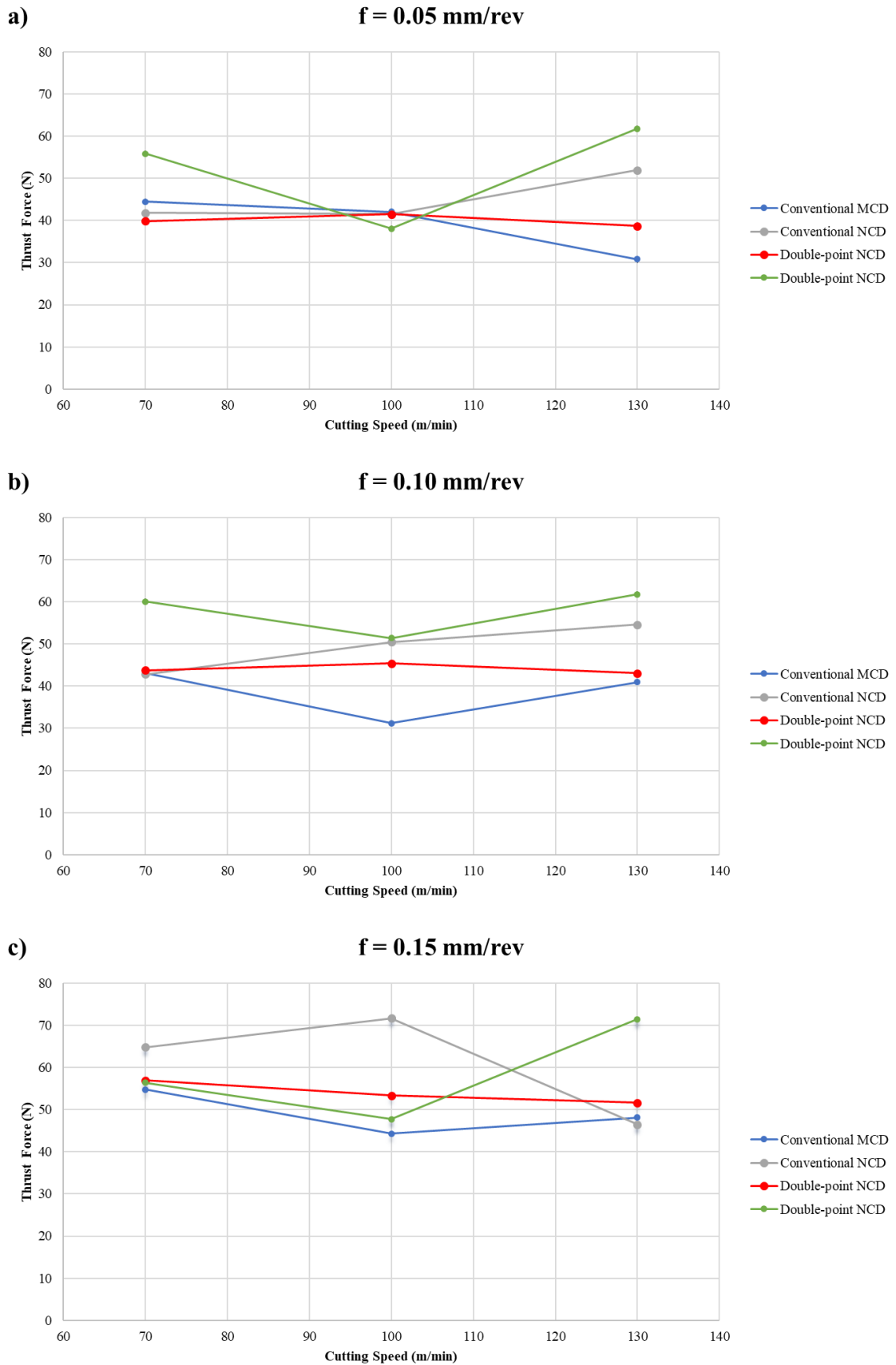


Figure 116 - Peak thrust force values at various cutting speeds for certain feeds: (a) 0.05 mm/rev; (b) 0.10 mm/rev; (c) 0.15 mm/rev.

From the observation of Figure 115, there is a clear tendency for the thrust force to increase with the feed, regardless of the cutting speed, and the tool's coating and geometry.

There are, however, some results that contradict this tendency, such as, for example, the one obtained for a conventional NCD tool at a feed of 0.15 mm/rev and a cutting speed of 130 m/min. The combinations of cutting tool and parameters where this contradiction was found were repeated, with all the selected sets of parameters presented on Table 32. The peak thrust force and peak drilling torque results presented within subsection 4.2.1 for the referred combinations are resultant from this drilling campaign.

Table 32 - Sets of drill bits and parameters for the peak thrust force outlier correction campaign.

Drill Geometry	Drill Coating	Feed (mm/rev)	Cutting Speed (m/min)
Conventional	NCD	0.15	130
Conventional	MCD	0.10	100
		0.05	100
		0.10	100
Double-Point	NCD		100
		0.15	130

Appendix G: Hole Cylindricity

The hole's cylindricity was obtained for every one of the 108 holes, with the average and respective standard deviation being calculated for the 3 holes drilled with the same parameters. The obtained results are presented in Figure 117 and Figure 118.

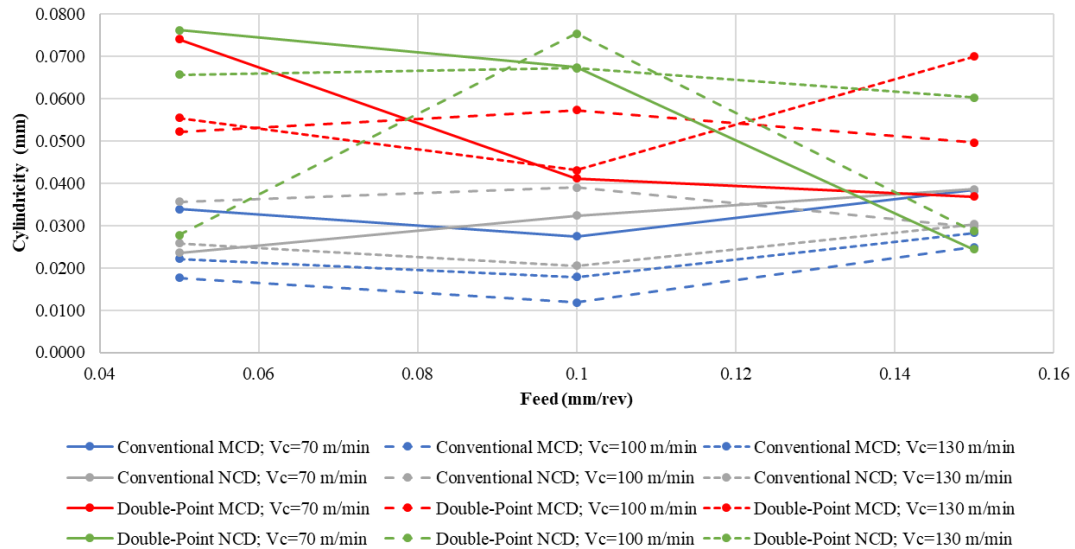


Figure 117 - Cylindricity for each drill at various feed for a certain cutting speed.

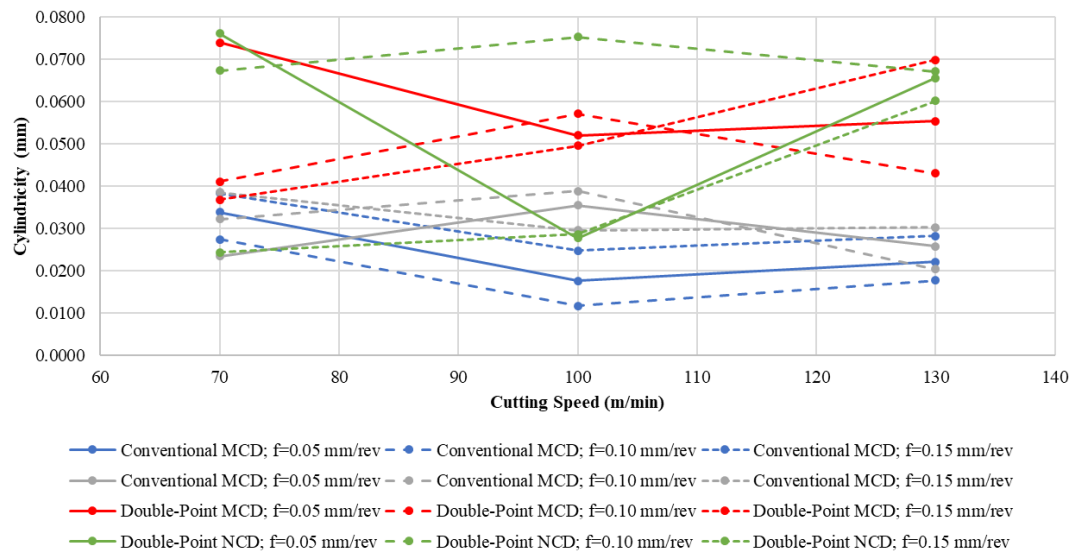


Figure 118- Cylindricity for each drill at various cutting speed for a certain feed.

The presented results exhibited coefficients of variation for each datapoint always above 20%, reaching 80% at the worst. The results are consequently associated to very high variability, impeding the extraction of any valid conclusion regarding the effects of the drilling tool and respective drilling parameters on the hole's cylindricity.

Appendix H: Adhesives Datasheets

H.1.Araldite 2051



Advanced Materials

Araldite® 2051

Structural Adhesives

TECHNICAL DATASHEET

Araldite® 2051

Two component toughened methacrylate adhesive system

Key properties

- Fast curing from 0°C to 40°C
- Suitable for service temperatures up to 120°C
- Application and curing under water
- Application and curing in humid areas
- Excellent ageing and weathering resistance
- Minimum pre-treatment needed

Description

Araldite® 2051 is a fast curing, two-part methacrylate adhesive designed to accelerate structural bonding operations while providing high toughness and resilience to vibrations, impact and dynamic loading.

Product data

Properties	Resin	Hardener	Mixed adhesive
Colour (visual)	Off white	Yellow / greenish	Pale yellow
Specific gravity	ca. 1	ca. 1	ca. 1
Viscosity at 25°C (Pa.s)	35 – 60	15 – 30	non-sagging
Pot life (20 gr. at 25°C)	-	-	4 - 6 minutes
Lap shear strength at 25°C (A501)*	-	-	> 20 MPa
Time to peak exotherm (20gr) (A159)*	-	-	8 – 14 minutes

* Specified data are on a regular basis analysed. Data which is described in this document as 'typical' is not analysed on a regular basis and is given for information purposes only. Data values are not guaranteed or warranted unless if specifically mentioned.

Processing

Pretreatment

The strength and durability of a bonded joint are dependent on proper pretreatment of the surfaces to be bonded, however the methacrylate adhesives can be used effectively with little surface preparation. Ideally joint surfaces should be cleaned with a good degreasing agent such as acetone, iso-propanol (for plastics) or other proprietary degreasing agents in order to remove all traces of oil, grease and dirt. Low grade alcohol, gasoline (petrol) or paint thinners should never be used. The strongest and most durable joints are obtained by either mechanically abrading or chemically etching ("pickling") the degreased surfaces.

Mix ratio	Parts by weight	Parts by volume
Resin	100	100
Hardener	100	100

Application of adhesive

This system is available in cartridges incorporating mixers and can be applied as ready to use adhesive with the aid of the tool recommended by Huntsman Advanced Materials.

The resin/hardener mix may be applied manually or robotically to the pretreated and dry joint surfaces. Huntsman's technical support group can assist the user in the selection of a suitable application method as well as suggest a variety of reputable companies that manufacture and service adhesive dispensing equipment.

A layer of adhesive 0.25 mm thick will normally impart the greatest lap shear strength to the joint. The joint components should be assembled and secured in a fixed position as soon as the adhesive has been applied.

Temperature of application: the recommended temperature application range is 0°C to 40°C. If the adhesive is used at a temperature above 40°C, because of the fast reaction, the viscosity will increase so rapidly that a good wetting of the substrate will be difficult to achieve. Therefore above 40°C, the performance of the bond may be limited. For temperature below 0°C, the Araldite® 2060 may be preferred.

Warning: the cure reaction can generate a high amount of heat, it is not recommended to mix large amounts of material at room temperature.

For more detailed explanations regarding surface preparation and pretreatment, adhesive joint design, and the dual cartridge dispensing system, visit www.aralditeadhesives.com.

Equipment maintenance

All tools should be cleaned with hot water and soap before adhesives residues have had time to cure. The removal of cured residues is a difficult and time-consuming operation.

If solvents such as MEK are used for cleaning, operatives should take the appropriate precautions and, in addition, avoid skin and eye contact.

Typical times to minimum shear strength on sandblasted aluminium / contact pressure

Temperature	°C	0°C	10°C	23°C	30°C	40°C
Cure time to reach	hours					
LSS > 1MPa	minutes	90	30	15	10	6
Cure time to reach	hours	2				
LSS > 10MPa	minutes		50	25	15	10

H.2.Araldite 420 A/B Datasheet



Advanced Materials

Araldite® 420 A/B

Structural Adhesives

ARALDITE® 420 A/B
Two component epoxy adhesive system

- Key properties**
- Very high lap shear and peel strength
 - Bonds a wide variety of materials (metal, composite and thermoplastics)
 - Good moisture resistance
 - Extremely tough and resilient adhesive

Description

ARALDITE® 420 A/B is a two-component room temperature curing, epoxy adhesive paste of high strength and toughness. Performances can be enhanced by post-curing at elevated temperature. It is suitable for bonding a wide variety of metals, ceramics and many other substrates in common use.

Product data

Property	Araldite 420 A	Araldite 420 B	Mixed Adhesive
Colour (visual) (A112)*	Yellow	Blue	Dark green
Specific gravity	1.2	1.0	approx. 1.1
Viscosity at 25 °C (A93) (Pa.s)*	100-300	0.6-1.4	35-45
Lap shear strength at 23 °C (A501)*	-	-	> 30 MPa
Pot Life (100 gm at 25 °C)	-	-	Ca. 60 min

* Specified data are on a regular basis analysed. Data which is described in this document as 'typical' is not analysed on a regular basis and is given for information purposes only. Data values are not guaranteed or warranted unless it specifically mentioned.

Processing

Pretreatment

The strength and durability of a bonded joint are dependent on proper treatment of the surfaces to be bonded. At the very least, joint surfaces should be cleaned with a good degreasing agent such as acetone, iso-propanol (for plastics) or other proprietary degreasing agents in order to remove all traces of oil, grease and dirt. Low grade alcohol, gasoline (petrol) or paint thinners should never be used. The strongest and most durable joints are obtained by either mechanically abrading or chemically etching ("pickling") the degreased surfaces. Abrading should be followed by a second degreasing treatment

Mix ratio	Parts by weight	Parts by volume
Araldite 420 A	100	100
Araldite 420 B	40	50

The resin and hardener should be blended until they form a homogeneous mix. Araldite® 420 A/B is available in cartridges incorporating mixers and can be applied as ready to use adhesive with the aid of the tool recommended by Huntsman Advanced Materials

Application of adhesive

The resin/hardener mix is applied with a spatula, to the pretreated and dry joint surfaces.

A layer of adhesive 0.05 to 0.10 mm thick will normally impart the greatest lap shear strength to the joint.

The joint components should be assembled and clamped as soon as the adhesive has been applied. An even contact pressure throughout the joint area will ensure optimum cure.

Mechanical processing

Specialist firms have developed metering, mixing and spreading equipment that enables the bulk processing of adhesive.

We will be pleased to advise customers on the choice of equipment for their particular needs.

Equipment maintenance

All tools should be cleaned with hot water and soap before adhesives residues have had time to cure. The removal of cured residues is a difficult and time-consuming operation.

If solvents such as acetone are used for cleaning, operatives should take the appropriate precautions and, in addition, avoid skin and eye contact.

Typical times to minimum shear strength

Temperature	°C	23	40	60	100
Cure time to reach	hours	7.5			
LSS > 1N/mm ²	minutes		100	35	< 5
Cure time to reach	hours	11	2	1	
LSS > 10N/mm ²	minutes				20

LSS = Lap shear strength.

H.3.Araldite AV138M-1 Datasheet

HUNTSMAN

Enriching lives through innovation

Advanced Materials

Araldite® AV 138M-1 / Hardener HV 998-1

Structural Adhesives

TECHNICAL DATASHEET

Araldite® AV 138M-1 / Hardener HV 998-1

Two component epoxy adhesive

Key properties

- Excellent chemical resistance
- Temperature resistant to 140°C
- Thixotropic, gap filling paste

Description

Araldite® AV 138M-1 / Hardener HV 998-1 is a two component, room temperature curing paste adhesive of high strength. When fully cured the adhesive will have excellent performance at elevated temperatures and has high chemical resistance. It is suitable for bonding a wide variety of metals, ceramics, glass, rubbers, rigid plastics and other materials, and is widely used in many industrial applications where resistance to aggressive or warm environments are required.

Typical product data

Property	Araldite® AV 138M-1	Hardener HV 998-1	Mix
Colour - visual (A112)*	beige	grey	grey
Specific gravity	ca. 1.7	ca. 1.6	ca. 1.7
Viscosity (Pa.s)	thixotropic	thixotropic	thixotropic
Lap shear strength at 25 °C (A501)*	-	-	> 11 MPa
Pot Life (100 g at 25°C)	-	-	30 min

* Specified data are on a regular basis analysed. Data which is described in this document as "typical" is not analysed on a regular basis and is given for information purposes only. Data values are not guaranteed or warranted unless if specifically mentioned.

Processing

Pretreatment

The strength and durability of a bonded joint are dependent on proper treatment of the surfaces to be bonded.

At the very least, joint surfaces should be cleaned with a good degreasing agent such as acetone or other proprietary degreasing agents in order to remove all traces of oil, grease and dirt.

Low grade alcohol, gasoline (petrol) or paint thinners should never be used.

The strongest and most durable joints are obtained by either mechanically abrading or chemically etching ("pickling") the degreased surfaces. Abrading should be followed by a second degreasing treatment

Mix ratio	Parts by weight	Parts by volume
Araldite® AV 138M-1	100	100
Hardener HV 998-1	40	40

Resin and hardener should be blended until they form a homogeneous mix.

Application of adhesive

The resin/hardener mix is applied with a spatula, to the pretreated and dry joint surfaces.

A layer of adhesive 0.05 to 0.10 mm thick will normally impart the greatest lap shear strength to the joint.

The joint components should be assembled and clamped as soon as the adhesive has been applied. An even contact pressure throughout the joint area will ensure optimum cure.

Mechanical processing

Specialist firms have developed metering, mixing and spreading equipment that enables the bulk processing of adhesive. We will be pleased to advise customers on the choice of equipment for their particular needs.

Equipment maintenance

All tools should be cleaned with hot water and soap before adhesives residues have had time to cure. The removal of cured residues is a difficult and time-consuming operation.

If solvents such as acetone are used for cleaning, operatives should take the appropriate precautions and, in addition, avoid skin and eye contact.

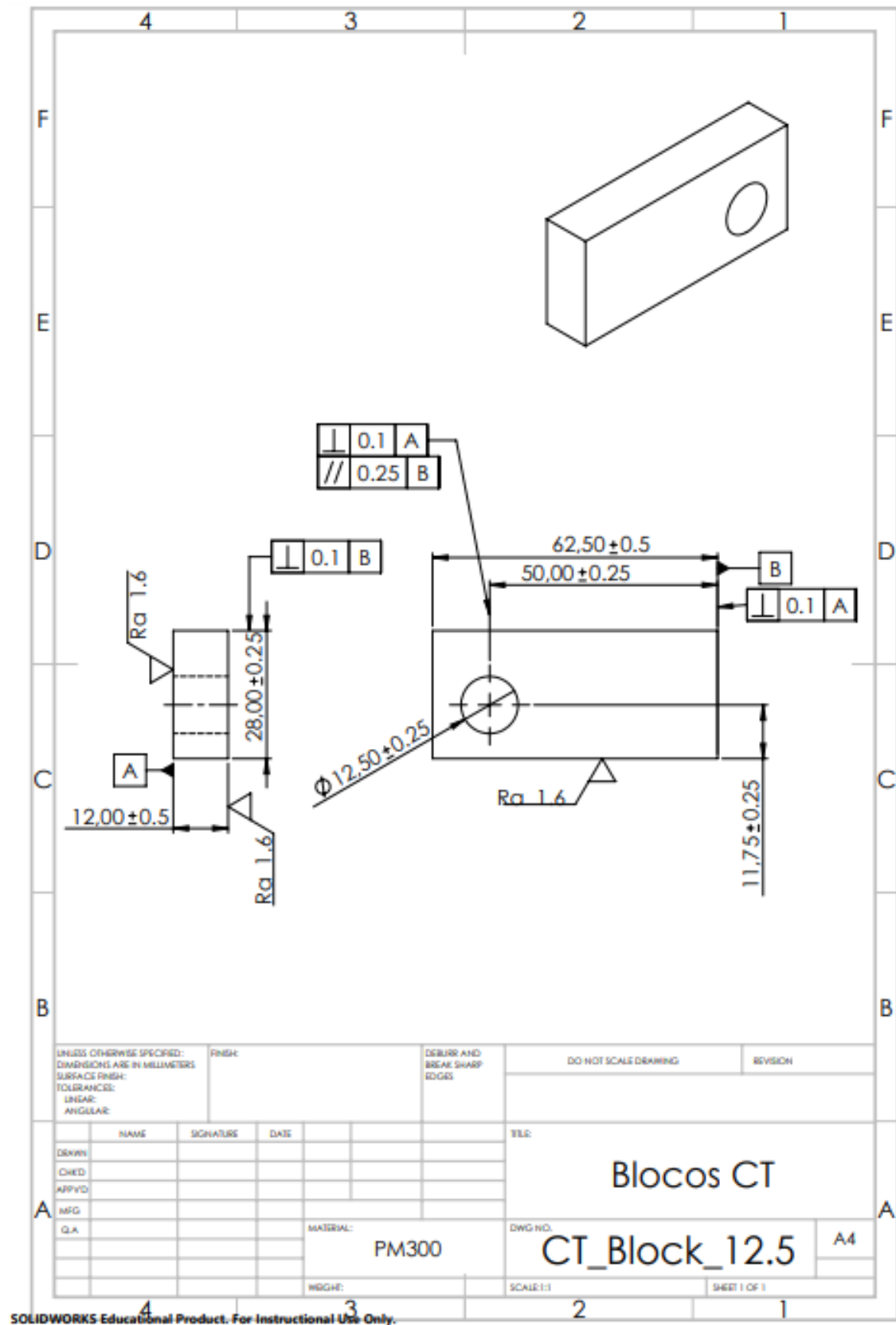
Typical times to minimum shear strength

Temperature	°C	10	15	23	40	60	100
Time to reach a LSS of 1 MPa	hours	14	8	4	-	-	-
	minutes	-	-	-	75	20	4
Time to reach a LSS of 10 MPa	hours	22	15	6	-	-	-
	minutes	-	-	-	120	30	5

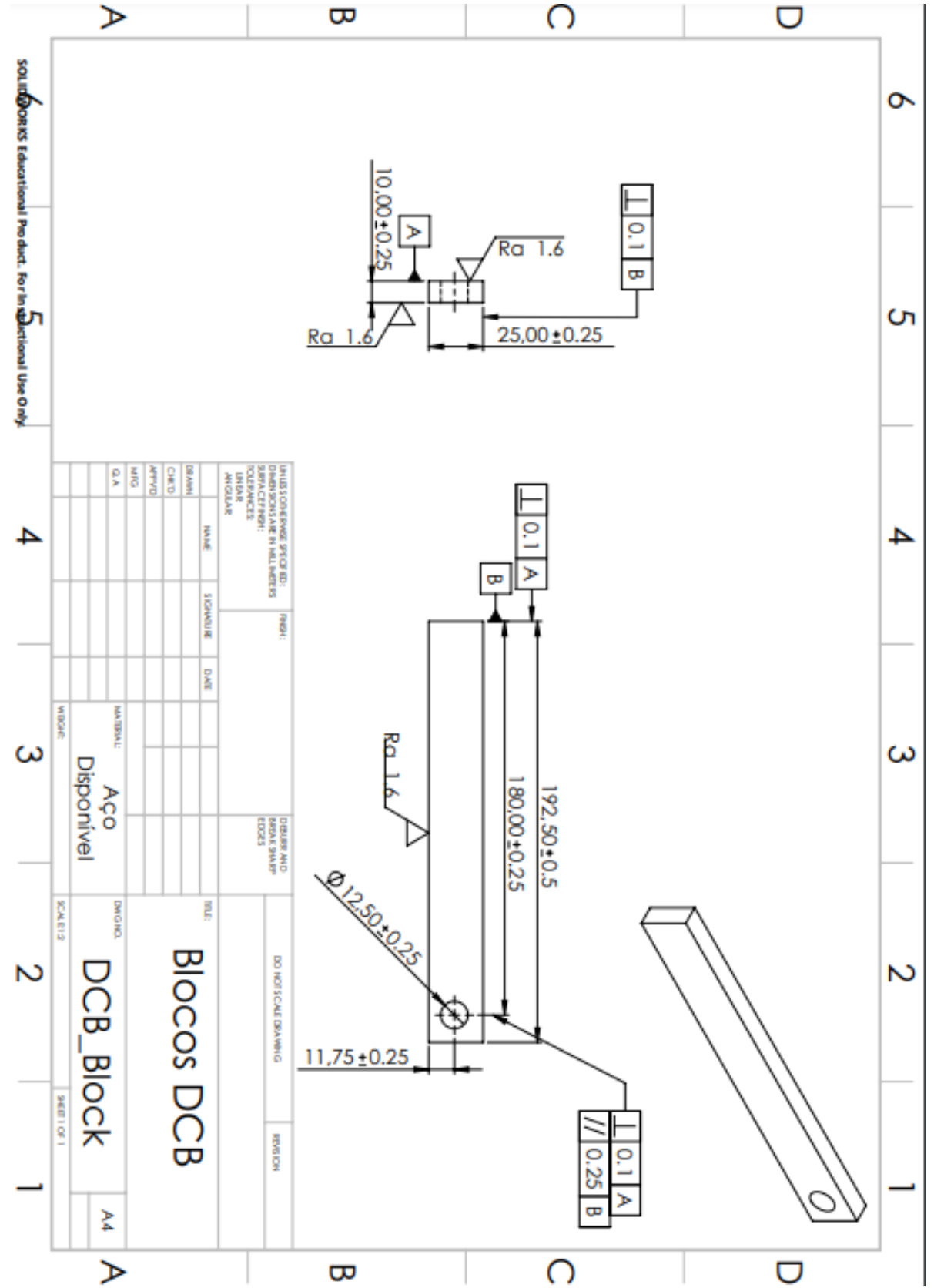
LSS = Lap shear strength.

Appendix I: Technical Drawings of Fracture Testing Blocks

I.1. Technical Drawing of Blocks Used for CT testing

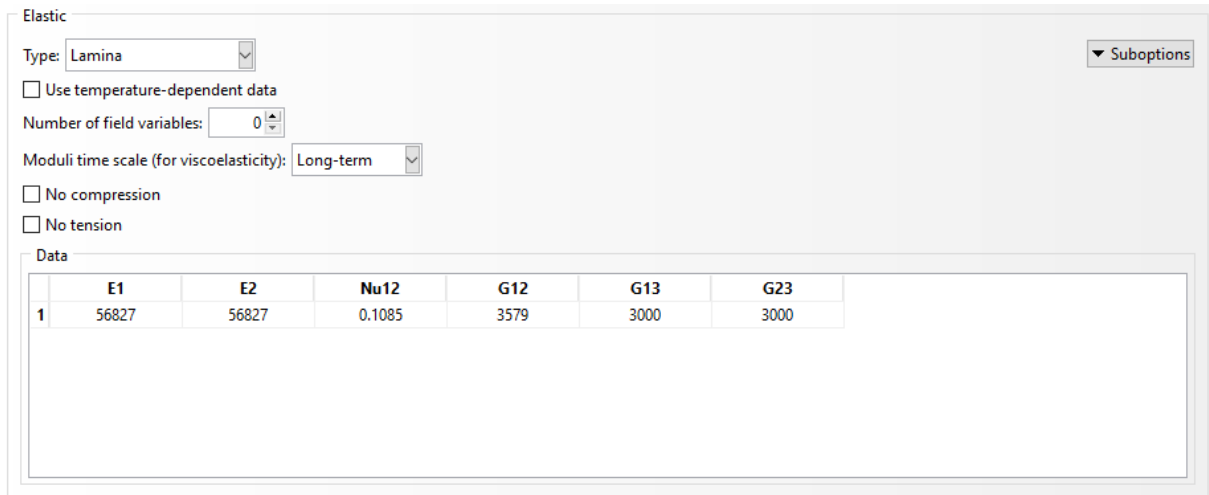


I.2. Technical Drawing of Blocks Used for final DCB test



Appendix J: ABAQUS Property Module

This appendix exposes the various properties and settings applied in the ABAQUS property module. Figure 119 is regarding the introduction of the elastic properties, while between Figure 120 and Figure 122 the Hashin's criterion properties are introduced, and in Figure 123 the composite layup settings are illustrated.



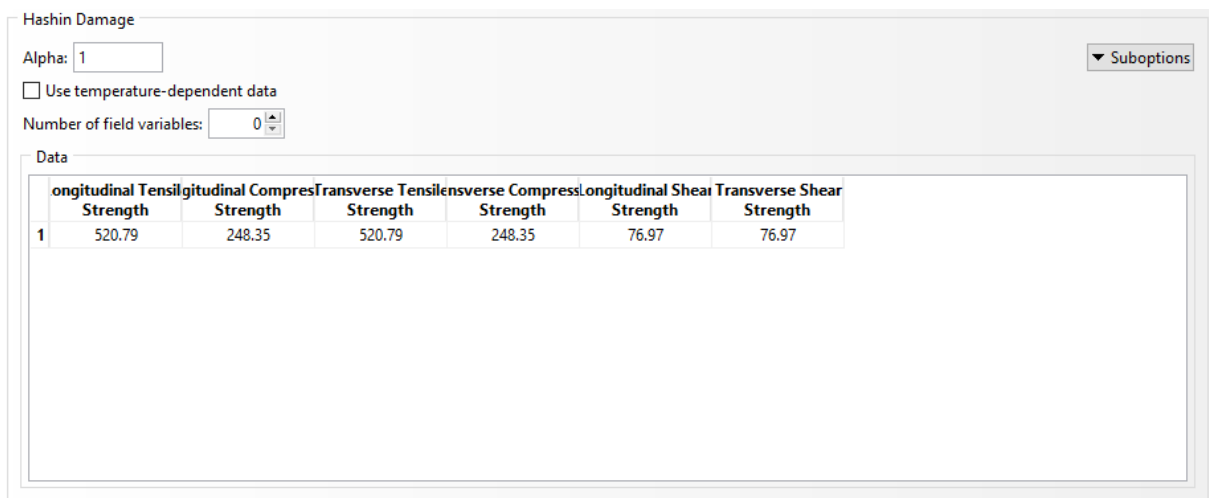
The 'Elastic' window in ABAQUS shows the following settings:

- Type: Lamina
- Use temperature-dependent data: ☐
- Number of field variables: 0
- Moduli time scale (for viscoelasticity): Long-term
- No compression: ☐
- No tension: ☐

The 'Data' table contains the following values:

	E1	E2	Nu12	G12	G13	G23
1	56827	56827	0.1085	3579	3000	3000

Figure 119 - ABAQUS menu with elastic properties inputs.



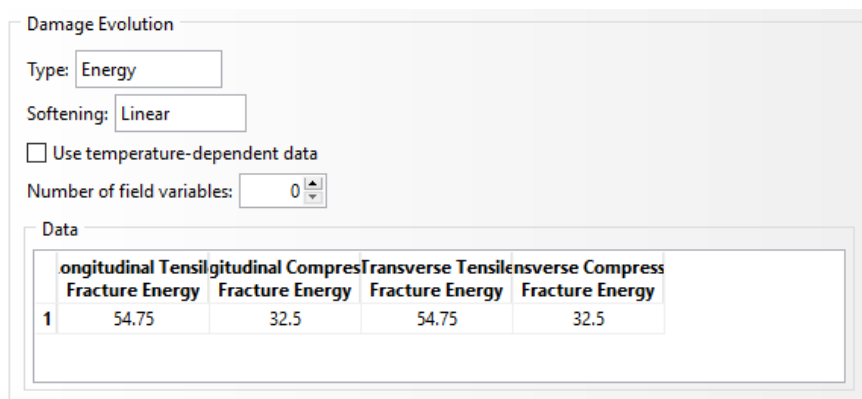
The 'Hashin Damage' window in ABAQUS shows the following settings:

- Alpha: 1
- Use temperature-dependent data: ☐
- Number of field variables: 0

The 'Data' table contains the following values:

	Longitudinal Tensile Strength	Longitudinal Compressive Strength	Transverse Tensile Strength	Transverse Compressive Strength	Longitudinal Shear Strength	Transverse Shear Strength
1	520.79	248.35	520.79	248.35	76.97	76.97

Figure 120 - ABAQUS Hashin damage menu properties inputs.



The 'Damage Evolution' window in ABAQUS shows the following settings:

- Type: Energy
- Softening: Linear
- Use temperature-dependent data: ☐
- Number of field variables: 0

The 'Data' table contains the following values:

	Longitudinal Fracture Energy	Longitudinal Compressive Fracture Energy	Transverse Fracture Energy	Transverse Compressive Fracture Energy
1	54.75	32.5	54.75	32.5

Figure 121 - ABAQUS damage evolution properties inputs.

Damage Stabilization

Viscosity coefficient in the longitudinal tensile direction: 0.0001

Viscosity coefficient in the longitudinal compressive direction: 0.0001

Viscosity coefficient in the transverse tensile direction: 0.0001

Viscosity coefficient in the transverse compressive direction: 0.0001

Figure 122 - ABAQUS damage stabilization properties inputs.

Element type: Conventional Shell Description: []

Layup Orientation

Definition: Part global 

Part coordinate system

Normal direction: ☐ Axis 1 ☐ Axis 2 ☒ Axis 3

Section integration: ☒ During analysis ☐ Before analysis

Thickness integration rule: ☒ Simpson ☐ Gauss

Plies Offset Shell Parameters Display

☐ Make calculated sections symmetric

		Ply Name	Region	Material	Thickness	CSYS	Rotation Angle	Integration Points
1	✓	Ply-1	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
2	✓	Ply-2	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
3	✓	Ply-3	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
4	✓	Ply-4	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
5	✓	Ply-5	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
6	✓	Ply-6	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
7	✓	Ply-7	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
8	✓	Ply-8	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
9	✓	Ply-9	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
10	✓	Ply-10	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
11	✓	Ply-11	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
12	✓	Ply-12	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
13	✓	Ply-13	(Picked)	A12 (CompMod	0.25	<Layup>	0	3
14	✓	Ply-14	(Picked)	A12 (CompMod	0.25	<Layup>	0	3

Figure 123 - ABAQUS composite layup properties inputs.