

ISTANBUL TECHNICAL UNIVERSITY ★ GRADUATE SCHOOL OF SCIENCE
ENGINEERING AND TECHNOLOGY

**MECHANISTIC MODELING OF DRILLING FORCES AND STUDY OF
RESIDUAL STRESSES IN DRILLING OF COMPACTED GRAPHITE IRON**

M.Sc. THESIS

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Department of Mechanical Engineering

Materials and Manufacturing Engineering Programme

Thesis Advisor: Assoc. Prof. Dr. Mustafa BAKKAL

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**KOMPAKT GRAFİTLİ DÖKME DEMİRDE DELİK DELME İŞLEMİNİN
MEKANİSTİK MODELLEMESİ VE KALINTI GERİLMELERİN
İNCELENMESİ**

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To my parents and sister,

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ABBREVIATIONS

CCD	: Charge-Couple Device
CGI	: Compacted Graphite Iron
CNC	: Computer Numerical Control
DET	: Double Exposure Technique
ESPI	: Electronic Speckle Pattern Interferometry
FGI	: Flake Graphite Iron
HSS	: High-Speed Steel
MQL	: Minimum Quantity Lubrication
MRR	: Material Removal Rate
RPM	: Revolution per Minute
SET	: Single Exposure Technique
SGI	: Spheroidal Graphite Iron
TGI	: Tempered Graphite Iron
WEDM	: Wire Electro Discharge Machining
XRD	: X-Ray Diffraction

NOMENCLATURE

A_s	: Area of shear plane
b	: Radial depth of cut, width of cut
Δb	: Elemental chip width
CD	: Core diameter
d_{hkl}	: Lattice spacing
E	: Elastic modulus
F_c	: Resultant force
F_{fc}	: Feed cutting force
F_{fe}	: Feed edge force
F_n	: Normal shear friction force
F_r	: Radial cutting force
F_s	: Shear force
F_{te}	: Tangential edge Force
F_{tc}	: Tangential cutting force
$F_{T,ort}$	: Tangential force of orthogonal chisel part
$F_{th,chisel}$	: Total thrust force of chisel edge
$F_{th,lip}$	: Thrust force of cutting lips
F_{THRUST}	: Total thrust force
F_v	: Normal friction force
F_u	: Friction force
$F_{z,ind}$	: Thrust force of indentation zone
$F_{z,ort}$	: Thrust force of orthogonal chisel part
F_z	: Axial force
f	: feed, uncut chip thickness
h	: Uncut chip thickness
h_c	: Cut chip thickness
i	: inclination (oblique) angle
K_{tc}	: Tangential cutting force coefficient
K_{fc}	: Feed cutting force coefficient
K_{rc}	: Radial cutting force coefficient
K_{te}	: Tangential edge force coefficient
K_{fe}	: Feed edge force coefficient
K_{re}	: Radial edge force coefficient
L_c	: Sliding zone length
L_e	: Total contact length
L_{cs}	: shear plane length
L_p	: Sticking zone length
L_{pt}	: Constant pitch length
P	: Normal stress, Pressure

P_n	: Normal plane
P_u	: Friction power
R	: Radius
$r, r(z)$	: elemental radial distance
r_c	: Chip compression ratio
r_{ind}	: Indentation area radius
T	: Torque
T_{chisel}	: Total torque of chisel edge
T_{lip}	: Torque of cutting lips
T_{TORQUE}	: Total torque
V, V_c	: Cutting Speed
V_t	: Projection of cutting velocity on normal plane
V_{chip}	: Chip velocity
α	: Rake angle
α_n	: Normal rake angle
α_f	: Effective rake angle
β	: Local helix angle
β_0	: Nominal helix angle
β_a	: Average friction coefficient, Apparent friction coefficient
β_n	: Normal friction angle
γ_d	: Reference angle
ε	: Strain
ε_s	: Slip line
η	: Chip flow angle
$\theta(z)$	: Angular position of element
$2\kappa_t$	: Point angle
λ	: Wavelength
μ, μ_a	: Friction coefficient
σ_s	: Normal cutting stress
σ_1, σ_2	: Principle stresses
σ_ϕ	: Stress in arbitrary direction
τ_1	: yield shear stress
τ_s	: Shear stress
ν	: Poission's ratio
ϕ_c	: Shear angle
ϕ_i	: Oblique shear angle
ϕ_n	: Normal shear angle
ψ_c	: Chisel angle
ψ	: Tilt angle
2ω	: Web thickness

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MECHANISTIC MODELING OF DRILLING FORCES AND STUDY OF RESIDUAL STRESSES IN DRILLING OF COMPACTED GRAPHITE IRON

SUMMARY

Outstanding mechanical and thermal properties of Compacted Graphite Iron (CGI) have attracted the engine manufacturers attention recently. This is due to the superior characteristics of the microstructure of CGI, which makes it a promising material in automotive and locomotive industries. This unique microstructure is formed by the additional elements such as magnesium (Mg) and titanium (Ti) in specific amounts and decreasing the amount of sulfur (S) which promotes formation of spheroidal graphite phases. Addition of these elements prevents formation of spheroidal graphite phases and generates vermicular shape graphite phases. Vermicular graphite phases decrease the material's tendency for crack initiation and propagation, and hence increases the fatigue life significantly compared to gray cast iron, besides increasing the tensile strength. Moreover, CGI has better thermal conductivity in addition to its lighter weight compared to commonly used materials for engines such as gray cast iron. As a result, compacted graphite iron becomes a preferable material in the automotive industry especially in manufacturing of diesel engines. Drilling operation is a challenging operation in CGI engine blocks. Since the drilling operations are usually carried out at final steps of manufacturing cycle, any failure in tool will scrap the hole engine block. Therefore, mechanics of drilling and induced residuals by drilling operation in compacted graphite iron is the scope of the current study.

For this purpose, primarily, geometry of the twist drill is modeled analytically. Cutting lips and chisel edges are responsible for material removal and cutting process in drilling. The most volume of material cutting is done by cutting lips, here the chisel edge acts as an indenting tool, which penetrates into material and ploughs the material without serious amount of cutting. Since the twist drill utilized in this study is split-line twist drill, the chisel edge has been considered as two different regions including orthogonal cutting zone and indentation zone. In order to model the cutting forces in cutting lips and orthogonal chisel edge, orthogonal tube cutting tests were carried out to obtain the orthogonal material parameters including shear angle, shear stress and friction angle for the workpiece-tool pairs. These parameters were found as a function of rake angle and cutting speed for several uncut chip thickness values using the regression methods. Cutting lips of the twist drill were divided into several elemental edges, whereas considering each of them as an oblique cutting edge. Using the evaluated material parameters from orthogonal tube cutting tests, orthogonal to oblique transformation was applied and the cutting forces for each elemental tool along the cutting edges of the drill were calculated. Elemental cutting forces, which have three-dimensional directions, were transformed into drill coordinates. Summing the transformed elemental cutting forces in the direction of the axis of the tool, the total thrust force for cutting lips were calculated. Similarly, summing the torque

value of each element will results in total torque value of the cutting lips. The orthogonal cutting region of the chisel edge was also divided into several elements. Having the geometrical parameters of the chisel edge, the cutting forces were obtained and were transformed into the tool's coordinates. Summing the thrust forces values produced from chisel edge and cutting lips, the total cutting forces for the drilling process were obtained. Therefore, having geometrical parameters of twist drill and workpiece-tool pair parameters, drilling cutting forces can be predicted using the model. Finally the predicted values were verified by drilling experiments. The predicted and experimental results are in a good agreement. The predicted values will be used in cutting temperature predictions at other topics of the project.

Drilled CGI parts are subjected to residual stress measurements in order to investigate the effect of mechanical and thermal loading during drilling operation. Drilled specimens were measured from two points in order to study the residual stress profile along the hole. The X-ray diffraction method was employed in order to measure the induced residual stresses at the machined surface. Afterwards, incremental hole-drilling technique and the electronic speckle pattern interferometry (ESPI) method were used to acquire the residual stress profile in depth. Combining the surface residual stress measure by the XRD method and subsurface residual stress results measured using incremental hole drilling method, leads to derive residual stress profile of component in depth from machined surface.

Drilling operation induces compressive residual stress at bulk of the hole. It means that mechanical loadings are more dominant than thermal stresses during cutting operation. Mechanical behavior of material, high thermal conductivity of compacted graphite iron prevent the generation of high thermal stresses, which in turn causes tensile residual stresses. In addition, high rake angle values of twist drill at the periphery of tool where engaged with workpiece results it high shear angles and in turn reduction in cutting forces, which causes compressive residual stresses. Furthermore, the effects of cutting speed and feed rate on residual stress profiles were investigated. Increasing cutting speed alters the residual stress profiles to more tensile manner because of thermal gradients. However, increasing feed rate induces tensile residual stress in subsurface layers due to high elastic relaxation in subsurface layers.

KOMPAKT GRAFİTLİ DÖKME DEMİRDE DELİK DELME İŞLEMİNİN MEKANİSTİK MODELLEMESİ VE KALINTI GERİLMELERİN İNCELENMESİ

ÖZET

Otomotiv ve lokomotif sanayileri uzunca bir süredir ürün kalitesi ve performansını artırmaya çalışırken, bir yandan da çevreyi korumaya yönelik kanunlara da uyma zorunluluğuna bir arada cevap vermeye çalışmaktadır. Bu kanunlardan özellikle emisyon standardındaki kirletici miktardındaki büyük düşüş beklentisini daha verimli yanmayı sağlayacak motor teknolojilerinin geliştirilmesini gerekli kılmaktadır. Özellikle dizel motorları üretiminde kullanılan kır dökme demir yerine bugün daha iyi mekanik ve fiziksel özelliklere sahip olan kompakt grafitli dökme demir kullanımı bu amaca yöneliktir. Dizel motorlarında daha fazla performans, daha az emisyon ve daha verimli yanma sağlanması için, motor içerisindeki ateşleme basıncının artması gerekmektedir. Yanma basıncının artması motor bloğunu daha fazla mekanik ve ısı yüklemelere mağruz bırakır ve dolayısıyla daha fazla yorulma dayanımı gerekmektedir. Kompakt grafitli dökme demir (CGI), kır dökme demire göre sahip olduğu yüksek çekme dayanımı ve yorulma dayanımı sayesinde daha büyük basınçlara dayanabildiğinden daha iyi bir yanma ortamı oluşturmaktadır. Bu iyi özellikler kendine has iç yapısı yani kompakt grafitlerin sayesinde. Kompakt grafit fazını elde etmek için magnezyum, titanyum ve krom gibi alaşım elementleri ilave ederken, sulfur alaşım elementinin azalması gerekmektedir. Sonuç olarak solucana benzer grafit fazı (kompakt grafit fazı) oluşacaktır. Yuvarlak kenarlı ve düzensiz engebeli karmaşık grafit morfolojisi sonucunda grafit ve demir matris arasında güçlü bir adezyon kuvveti ile bağlanır. CGI çatlak oluşumu ve ilerlemesini engellemesi ve daha iyi mekanik özelliklerinden dolayı kır dökme demire güçlü bir alternatiftir. Kompakt grafitin kır dökme demirdeki lamel grafit fazına göre yarattığı farklılık, CGI'nin kır dökme demire göre daha iyi mekanik ve yorulma mukavemeti göstermesine neden olur. Dolayısıyla günümüzde CGI dizel motorları için en çok tercih edilen malzemedir.

Motor bloğu üretim hattında delik delme işlemi genellikle en son adımlarda yapılır. Bu yüzden herhangi bir işlem hatası takımın kırılmasına ve parçanın hurda olmasına neden olur. Bu nedenle delik delme işlemi malzemeye göre modellenmesi ve matkap geometrisinin incelenmesi ve optimizasyonu imalat mühendisleri açısından önemli faktördür. Ayrıca motor bloğunda genellikle normal delik ve derin delikler yağ hazeneleri için kullanıldığından ve bu hazenelerin motor yanma bölümüne yakın olduğundan ardışık mekanik ve ısı yüklenmelere mağruz kalmaktadır. Eğer bahs ettiğimiz deliklerde talaş kaldırma esnasında kalıntı gerilme oluştuysa, ısı ve mekanik yorulma esnasında hasara uğrar. Artık gerilmeler işlenmiş yüzeylerde talaş

kaldırma sırasında düzenli olmayan plastik deformasyonlar ve ısınıp soğumalardan dolayı oluşur. Bu yüzden talaş kaldırma esnasında kalıntı gerilmelerin incelenmesi büyük önem taşır.

Bu çalışmada delik delme işlemi mekanistik olarak modellendi ve delik delme esnasındaki oluşan kalıntı gerilmeler incelenmiştir. Kullanılan matkap geometrisi analitik olarak modellendikten sonra, matkapın gerektiği talaş açısı aralığı bulunmuştur. Genel olarak matkap geometrisinin talaş açısı kesici kenar boyunca -30 ile -30 derece arasında değişmektedir. Bu sebeple farklı talaş açılara sahip takımlar ile farklı kesme hızlarında ve ilerlemelerde dik kesme testleri gerçekleştirilmiştir. Bu aralıkta farklı talaş açıları değerlerinde, farklı kesme hızlarında ve ilerlemelerde dik kesme testleri yapılır ve kesme kuvvetleri ölçülür. Dik kesme işleminde aynı delik delme işlemi gibi, iş parçası malzemesi CGI ve kesici takım malzemesi ise karbür kullanılmıştır. Kesme kuvvetleri ölçüldükten sonra, talaş kalınlıkları ölçülür ve dik kesme mekaniğine kullanarak her bir deney için kayma gerilmesi, makaslama açısı ve sürtünme açısı değerleri talaş açısı hesaplanır. Daha sonra regresyon yöntemi yardımıyla her bir ilerleme (kesilmemiş talaş kalınlığı) değeri için kayma gerilmesi, makaslama açısı ve sürtünme açısı değerleri talaş açısının ve kesme hızının fonksiyonu şeklinde bulunur. Bu işlem spesifik bir ilerlemede her elemanın talaş açısı ve kesme hızına bağlı dik kesme datalarının elde edilmesini kolaylaştırır. Daha sonra matkapın geometrisine göre matkapın kesme kenarı çok sayıda farklı talaş açısı, eğiklik açısı ve kesme hızına sahip eğik kesme elemanlarına bölünür. MATLAB programını kullanarak yazılan modelde, belirli ilerlemede herbir elemanın talaş açısı ve kesme hızına göre makaslama açısı, kayma gerilmesi ve sürtünme açısı hesaplanır ve eğik kesme mekaniğine yerleştirilir. Eğer modeli herhangi bir ilerlemede denenmemiş ilerlemeler hariç-kullanmak istersek model var olan dataları interpolasyon yöntemleri ile istenen ilerlemeye uygun olarak hesaplar. Böylece herbir eleman için dik kesme 3-boyutlu eğik kesme kuvvetleri hesaplanır. Başka bir deyişle matkapın kesme kenarın boyuca her elemanı eğik kesme kenarı varsayarak kesme kuvvetleri hesaplandı ve çok sayıda farklı yönlerde 3boyutlu kuvvetler bulundu. Matkap mekaniğini kullanarak kesme elemanların kuvvetleri matkap kordinasyonuna çevrilir. Matkap eksen yönündeki kuvvetleri toplayarak matkapın kesme kenarlarından üretilen dik kuvvet bulunur. Ayrıca her bir elemanın teğetsel kuvvetlerinin ürettiği torku toplayarak, kesme kenarlarının ürettiği toplam tork hesaplanır.

Ayrıca matkapın çisel kenarı ise iki farklı bölgeye bölünür. Bu bölgeler girici bölge ve dik kesici bölgeleridir. Matkapın merkezi (girici bölge) slip-line mekaniğini kullanarak dik kesme kuvveti hesaplandı. Dik kesme bölgesi ise elemanlara bölünerek ve dik kesme datalarını kullanarak modellenir. Delik delme işleminin toplam dik kuvvet ve tork değerleri iki kesici kenar ve çisel bölgesinden elde edilen kuvvetlerin toplamından hesaplanır. Böylece dik kesme testlerinden elde edilen parametreleri ve matkap geometrik özelliklerini kullanarak delik delme işlemindeki kuvvetleri analitik olarak hesaplanır. Elde edilen kuvvetleri doğrulama amacıyla, delik delme işlemi yapıldı ve dinamometre yardımıyla delik delmedeki dik kuvvet ve tork elde edilir. Analitik yöntem ile elde edilen değerler ve delik delme deneylerinden ölçülen değerler birbiriyle örtüşmektedir.

Delik delme işleminin oluşturduğu kalıntı gerilmeleri incelenmesi için delinen parçalar tel erezyon yöntemi ile ikiye bölünür. Kalıntı gerilme yöntemi iki farklı adım da gerçekleşir. Öncelikle yüzeyde oluşan kalıntı gerilmeler X-Ray kırınım

metodu (XRD) ile ölçülüyor. Genel olarak kalıntı gerilme ölçümleri metaller ve seramikler gibi polikristalin malzemelerde kafes yapılarındaki değişimler dikkate alınarak yapılır. Çünkü uygulanan streten ötürü kafes düzlemleri arasındaki mesafeler değişim gösterirler. Yeni mesafeler Bragg kanununda da ifade edilmiştir ve hepsi sabittir. Uygulanan kuvvetten ötürü oluşan plastik deformasyondan dolayı gönderilen ve dönen ışınlar arasındaki açı değişmektedir. Bu da malzemenin mikroyapısında düzenli olmayan mikro birim şekil değişimleri olduğu anlamına gelir ve bunlarda kalıntı gerilmeye sebep olur. Kalıntı gerilme değerleri gönderilen ve dönen ışınların açısının değişimi ile ölçülür. Bu değişimler kalıntı gerilmeden dolayı ortaya çıktığı için bu stresler ve birim şekil değişimleri X-ray elastik sabiti kullanılarak elde edilebilir. Daha sonra ölçülen noktalarda, delik açma tekniğini kullanarak 0.5 mm derinlikteki kalıntı gerilme profili incelenmiştir. Bu teknikle, Gerilme analizi yapılacak olan parça üzerinde, kör bir delik açarak talaş kaldırılmasıyla kalıcı gerilmenin ölçümü başlamış olur. Delik açılmasından sonra, deliğin etrafındaki malzeme kendine yeni bir gerilme dengesi oluşturur. Gerilmedeki bu yeni düzenleme, deliğin yüzey etrafında çok ufak bir bozunmaya neden olur. Yer değiştirmeler çok küçük olduğundan ölçümleri alınabilir ve delik açma işlemi öncesindeki gerilmelerin hesaplanmasını sağlar. Herbir delik açıldığında kalıntı gerilmelerden dolayılanan bir yüzey rahatlatma oluşuyor. Bu yüzden delik profili değişiyor. elektronik girişim deseni analizi (ESPI) yöntemini kullanarak deliğin etrafındaki girişim desen farklarını delikten önce ve delikten sonra video kayıt birimi tarafından kaydediliyor. Gerilme analizi yapılacak olan parça üzerinde, kör bir delik acılarak talaş kaldırılmasıyla kalıcı gerilmenin ölçümü başlamış olur. Delik açılmasından sonra, deliğin etrafındaki malzeme kendine yeni bir gerilme dengesi oluşturur. Gerilmeledeki bu yeni düzenleme, deliğin yüzey etrafında çok ufak bir bozunmaya neden olur. Lazer ışığı her yüzeyde farklı bir girişim deseni oluşturur. . Delik açılma nedeniyle oluşan bozunma, farklı bir girişim deseni oluşturur. Delik açılımı sonrasındaki girişim deseni değişikliği delik malzemenin gerilim rahatlamasına bağlıdır. Ayrıca, girişim alanının büyüklüğü gerilim rahatlamasıyla orantılıdır. Video kayıt sistemi yüzeyin delik açılımı öncesi ve sonrasındaki oluşan girişim desenini kaydeder. Bigisayar yardımıyla ve image processing tekniklerini kullanarak iki farklı girişim desenini karşılaştırarak yüzeyin toplam deplasmanı hesaplanır. Video kayıt birimi bu girişim farklılıklarını kayıt ederek malzemedeki deplasmanı hesaplıyor. Deplasman miktarını kullanarak ve malzemenin mekanik özelliklerine dayanarak kalıntı kalıntı gerilme değerleri elde ediliyor.

XRD yöntemi ve delik açma yöntemini birleştirerek kalıntı gerilme profilini, işlenmiş yüzeyden derinliğe göre hesaplanır. Sonuçlara dayanarak, delik delme işlemi delik yüzeyinde ve yüzeyaltı bölgelerde basma gerilmesinin oluşmasına neden oluyordur. Malzemenin mekanik özellikleri, matkapın büyük talaş açısına sahip olması ve komakt grafitli dökme demirin ısı iletkenliğinin yüksek olması, termal gerilmenin kolayca oluşmasına izin vermiyordur. Ayrıca delik delme işleminde en yüksek ısı çisel bölgesinde oluşur ve bu bölgeden etkilenen malzeme talaş olarak atılıyordur. Delik yüzeyi sadece matkapın etrafında yer alan büyük talaş açılı elemanlardan etkileniyordur ve bu elemanlar büyük talaş açısı nedeiyle daha az kuvvet ve daha düşük sıcaklıkta kesim yapıyordur. Böylece kalıntı gerilme profilleri çekme bölgeye çıkamıyordur. Bu yüzden, mekanik yüklemelerden yaranan gerilmeler daha etkili oluyorduğu düşünülüyor. Basma kalıntı gerilmeler malzemenin yorulma performansını artırır ve zararlı basma kalıntı gerilmelerin oluşmasını engeller.

1. INTRODUCTION

1.1 Overview and Motivation

Unique performance of Compacted Graphite Iron (CGI) in most of the industries especially at automotive and locomotive industries makes it as an inevitable alternative for commonly used material such as aluminum and gray cast iron. This is due to the outstanding mechanical and thermal properties of compacted graphite iron (CGI) compared to gray cast iron. The superior microstructure of graphite in CGI introduces 75% higher tensile strength, 35% higher tensile strength and 200% higher fatigue strength than gray cast iron [1]. Emission legislations force the engine manufacturers to produce engines with higher peak firing pressure to have efficient combustion, improved performance and reduced emission with quieter engine operation sound. Higher firing pressures provide higher mechanical loads on engine bulk which potentially resulting in fatigue failure. However, because of superior microstructure and round-shape graphite within the structure, CGI has a great resistance to crack initiation and propagation, which introduces higher fatigue strength as well as higher thermal conductivity. Compacted graphite iron (CGI) combines the better mechanical properties with better thermal conductivity and resistance to thermal distortions with a relatively lighter weight, which meets the requirements for diesel engine manufacturers.

Drilling and deep hole drilling operations are high-energy-density operations commonly used in many applications in such as crankshaft oil holes, transmission valve body spool bores, and engine block oil feed holes. Since the drilling operations are usually performed at last steps of manufacturing cycle, any failure of tool or workpiece results in scrapping the machined part. Manufacturers demand economical operations, which brings precise products with a high production speed. Hence, in order to carry out precise drilling operations quality and productivity must be considered simultaneously. The quality of hole can be determined in terms of surface integrity and dimensional accuracy, which depends on both workpiece material and

drill bit geometry characteristics. In case of dimensional errors, position, cylindricity and straightness errors can be due to; 1) mechanical factors such as mechanical bending (buckling), whirling, chip rubbing phenomena, 2) thermal distortion of part during operations, 3) and vibration and dynamical effects, so-called “chatter” during cutting [2], which potentially all the mentioned reasons affected by unbalanced tool geometry and inhomogeneous cutting forces [3]. Surface integrity of the hole is another main aspect that determines the quality and accuracy of a hole. Surface integrity can be attributed to the both mechanical and thermal factors. Vibrations and unbalanced drill geometry deteriorates the surface morphology and roughness of hole dramatically. In addition, high cutting forces (high plastic deformation) and ploughing resulting from tool geometry errors will generate unnecessary thermal gradients, which can induce undesirable residual stresses in workpiece material.

As well as cutting mechanics, residual stresses also play a crucial role in the performance of machined part. If residual stresses are induced in a component, the fatigue life, corrosion resistance and part distortion will be affected dramatically. The residual stresses can be beneficial or detrimental to the mechanical behavior of the component. For instance in automotive industry, if detrimental residual stresses are induced in the drilled hole in an engine block, successive mechanical and thermal loads will result in premature fatigue failure in vicinity of the hole. As a result understanding the residual stresses imparted by machining is an important aspect.

Residual stresses can be caused by plastic deformation of material or volume variations of the material due to thermal gradients. Consequently, the both thermal gradients and mechanical loads are caused by high plastic deformation rate and friction at tool-chip and tool-workpiece interfaces which affects the formation of residual stresses drastically.

Therefore, studying the workpiece material and cutting process mechanics and characteristics as well as residual stress behavior, help avoiding tool and workpiece failure, decrease cycle time, enhance the surface integrity of holes, and decrease the dimensional errors in drilled holes.

1.2 Scope

The scope of current work is to model the cutting forces in drilling operation mechanistically and study the drilling induced residual stresses in compacted graphite iron. Although several researches have been carried out in modeling of drilling operation, but there is still opportunities to advance the geometry of cutting edges to achieve more stable and smooth cutting operation. In addition, due to the geometrical limitations, study of residual stresses inside machined holes is a struggle. In this thesis, drilling residual stresses are investigated in order to deal with the possible errors or failures resulting from machining induced residual stresses.

1.3 Outline of Thesis

This thesis consist of 6 chapters, from the which current chapter presents the motivation, scope of the work and a brief outline of the thesis. In chapter 2, relevant literature about the CGI material used in this study, mechanistic modeling of drilling operation and residual stress and effort carried out by other researchers is review in general. A detailed description of material characteristics and mechanical properties of compacted graphite iron is presented in this section.

Chapter 3 provides a detailed explanation of geometry of a drill bit. The contribution of each geometrical parameters as well as their responsibilities during cutting operation are covered extensively. In additions, the split-point type drill bit used in this study and its properties are described in detail.

The mechanistic modeling of cutting forces in drilling operation is presented in Chapter 4. In this chapter cutting edge geometry of drill bit is modeled analytically. According to the geometrical properties of drill bit, orthogonal tube cutting experiments have been performed in a wide range of rake angles and cutting speed in order to evaluate orthogonal cutting parameters in that range. Orthogonal to oblique transformation were applied to predict the drilling cutting forces. Drilling experiments have been carried out to compare and verify the predicted and experimental cutting force results.

The second scope of thesis, which encompasses the drilling induced residual stress, is presented in Chapter 5. In order to evaluate the residual stresses in drilled holes, XRD measurement have been carried out to measure the stress values at the

machined surface. Furthermore, incremental hole-drilling method has been carried out to acquire the residual stress profile in depth. A brief theory of each technique has been presented at this chapter.

Chapter 6 concludes the thesis with its contributions and recommendation and suggestions for future works.

2. LITERATURE REVIEW

2.1 Overview

Recent advances in cutting tool technology allow for increasing productivity and decrease the errors and failures during operations. These can be practical if the mechanisms behind the metal cutting operations are modeled. Although the higher productivity will be achieved by higher cutting speeds, tool breakage and lower hole tolerances influence the productivity in terms of cycle times of process and manufacturing costs. Tool life reduction and low part accuracy can be controlled by studying cutting forces and which depends on material properties and tool geometry. Higher cutting forces and cutting temperatures lead higher residual stresses in machined parts due to the inaccurate tool geometry design and cutting parameter selection may prohibit increases in productivity, and may cause form errors such as poor hole quality and poor hole profile and distortion in workpieces. Understanding cutting force mechanism will help to prevent the failures and increase the productivity in drilling operation.

In this chapter, the relevant literature on the studied material, drill bit properties, drilling force modeling and drilling induced residual stress are reviewed in summary. Detailed explanation about method and approaches of each section are provided in individual chapters.

2.2 Cast Irons

Cast irons are one of the significant materials in industry especially in recent decades. Cast iron consist the 70% the material of products that are produced by casting methods. This is due to the pleasant mechanical and thermal properties of the cast irons besides the low prices in casting process. The main elements of the cast irons are Iron (Fe) and Carbon (C). In cast irons carbon element must be more than 2% in weight [4]. Another inevitable element that commonly appears in cast irons is silicon (Si) which must be between 1-3% in weight. Carbon can exists in both

graphite and carbide phases in alloy. There are several methods to classify the cast iron based on different criteria. The most conventional method lies on the fracture characteristics. Due to this method, white cast iron, which has a white crystalline fracture surface as fracture, occurs along the carbide plates, and gray cast iron, which has a gray surface due to the fracture occurs in embedded graphite phase in the metal matrix. A more recent classification is based on the microstructural aspects of cast irons. First aspect is to categorize by the graphite morphology present in the matrix in four different groups: lamellar (flake) graphite iron (FGI), spheroid (nodular) graphite iron (SGI), compacted graphite iron (CGI), and tempered graphite iron (TGI). Another classification based on microstructure of the cast iron is based on the metal matrix as follow: ferritic, pearlitic, martensitic, and bainitic (austempered) cast irons. In addition to the above-mentioned classifications, cast irons can be commercially classified into five main type: gray, ductile, malleable, compacted graphite and white cast iron. The first four aforementioned types of cast irons have a common microstructure, which consist of graphite phase scattered in either ferritic, pearlitic, bainitic, tempered martensitic or combination of these matrices. It is to be noted that these four irons are categorized in terms of the morphology of the graphite phase in each of them. Since carbon cannot form graphite in molten iron, therefore, it does not appear in graphite phase in white cast iron, but remains combined with iron in form of carbide.

Table 2.1 : Classification of cast irons by commercial designation, microstructure and fracture type.

Commercial Designation	Carbon-rich phase	Matrix	Fracture	Final Structure
Gray Iron	Lamellar Graphite	P	Gray	Solidification
Ductile Iron	Spheroidal graphite	F,P,A	Silver-gray	Solidification or heat treatment
Compacted graphite iron	Vermicular graphite	F, P	Gray	Solidification
White Iron	Fe_3C	P, M	White	Solidification and heat treatment
Mottled iron	Lamellar Gr + Fe_3C	P	Mottled	Solidification
Malleable iron	Temper graphite	F, P	Silver-gray	Heat treatment
Austempered ductile iron	Spheroidal graphite	At	Silver-gray	Heat treatment

Table 2.2 describes all of the type of cast irons in terms of their microstructure, commercial designation and fracture type are summarized [5, 6]. It is to be noted that the carbon phase is as important as metal matrix composition in determining the mechanical and thermal properties of cast irons. However, combination of each metal matrix with different carbon phase produces different specifications for the cast iron. We can also classify the cast irons in term of graphite morphology. Based on morphology of graphite , three general types can be mentioned as : gray cast iron or flake graphite iron (FGI), ductile cast iron or spheroidal graphite iron (SGI), and compacted graphite iron (CGI) [1, 5]. The three graphite morphologies in different cast irons illustrated in Figure 2.1.

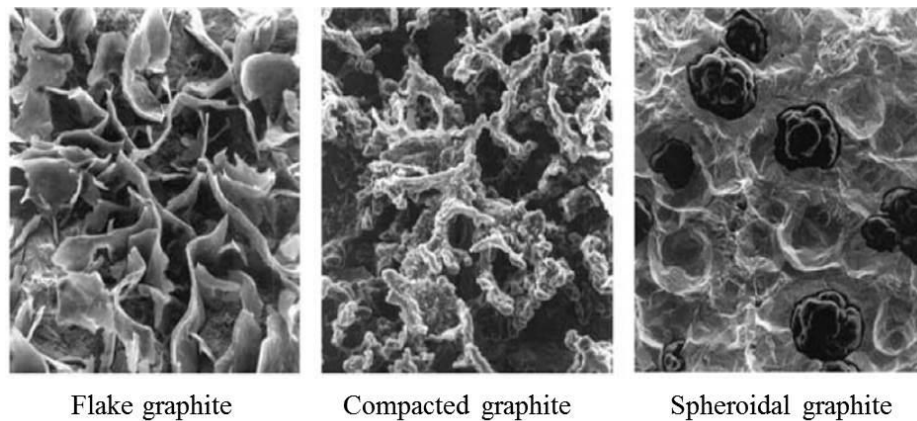


Figure 2.1 : Different 3-D images of graphite morphologies for flake, compacted and spheroidal graphite iron.

Figure 2.2 : also represents the morphology and distribution of the graphite in three different types of cast irons schematically. It can be seen that the black parts which represents the graphite phases are distributed in the white texture as the metal matrix. The shape differences of graphite phase between the flake graphite and compacted graphite is obvious. Compacted type graphite phases have rounder edges compared to the flake type. Whereas, spheroidal graphite phases have different geometry.

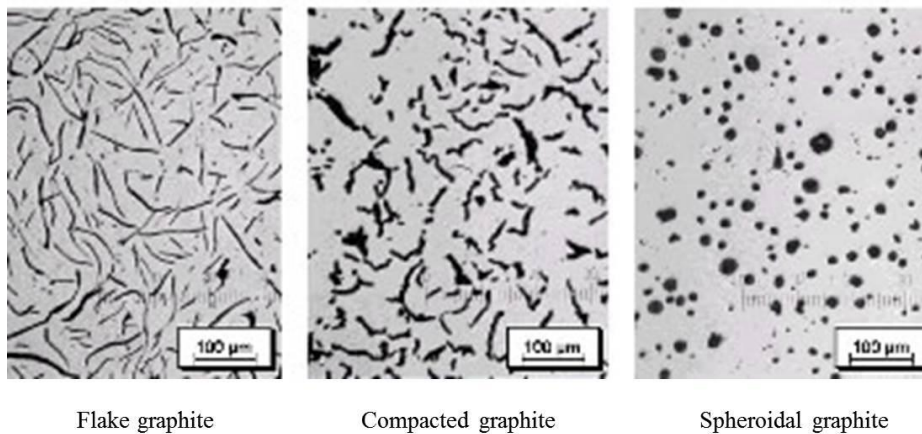


Figure 2.2 : Different graphite morphologies of flake, compacted and spheroidal cast iron captured by optical microscopy.

2.2.1 Compacted graphite iron

The first attempts to produce compacted graphite iron have been done by Morrogh and Williams in 1947 [7]. It is found that, addition of little amounts of magnesium or calcium can influence formation of graphite in cast irons after solidification. Although compacted graphite iron usually contains a percentage of nodular graphite, but it can be controlled by the percentage of the magnesium. However, since the required percentage of the magnesium is very narrow, it is difficult to control the procedure to produce compacted graphite phases. Exceeding the optimum amount of magnesium will results in nodular graphite and low percentage of magnesium will generate flake type graphite in matrix. Later, the effect of titanium on the graphite formation type was studied by Dawson and Evans [8]. In their studies, they conclude that although compacted graphite can be achieved in a narrow range of magnesium addition (0.015 to 0.02%), but by adding the titanium (0.08-0.1%), the upper limit for required magnesium will be increased about 0.03%. They also deduced that presence of cerium Ce is vital to produce a fully compacted graphite iron [9]. Generally there are three primary method to produce compacted graphite structure. The first and most common method based on treatment of liquid iron by adding the both nodularizing elements (magnesium and RE elements) and de-nodularizing elements (titanium or aluminum)[5, 10, 11]. Existence of titanium enlarges the possibility of obtaining compacted graphite iron for higher percentages of magnesium [12]. The second way is to control the liquid iron by rare earth (RE) elements. The third primary method relies on the desulphurizing of the liquid iron and casting with rapid

solidification cooling rate and possibly inoculations with Zr [13]. The standard type of compacted graphite represented in the following figure:

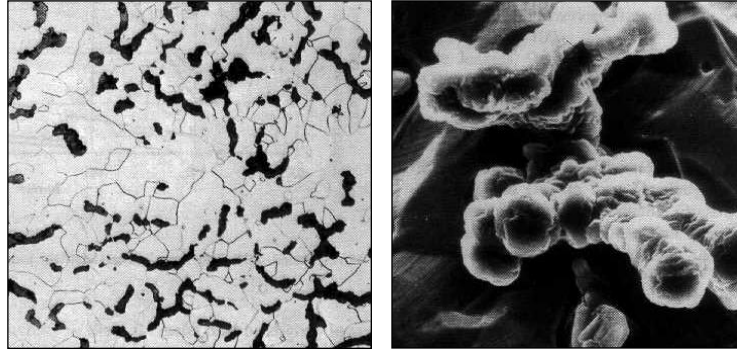


Figure 2.3 : Microstructure of cast graphite iron. (a) Optical Micrograph. (b) 3-D shape of compacted graphite. MEV 395x (ASM, 1996).

2.2.1.1 Properties of compacted graphite iron

The basic difference between the CGI and gray cast iron and ductile cast iron is the shape and morphology of the graphite formed in metal matrix. This basic difference is the main result to make differences in mechanical and physical properties either. As listed in Table 2.2, it can be seen that physical and mechanical properties of the compacted graphite iron lie between the ductile and gray cast iron.

Table 2.2 : Comparative mechanical and physical properties of different types of cast iron. (The index 100% was assigned to compacted graphite iron for comparison purposes) (SAE J1887/2002) [12].

Property	Gray Iron	CGI	Ductile Iron
Ultimate Tensile Strength	55%	100%	155%
Yield Strength	-	100%	155%
Elastic Modulus	75%	100%	110%
Fatigue Strength	55%	100%	125%
Hardness	85%	100%	115%
Damping Capacity	285%	100%	65%
Thermal Conductivity	130%	100%	75%

As we can see from the above table, compacted graphite phases enhance the mechanical properties of CGI compared to gray cast iron in terms of ultimate tensile strength, elastic modulus, fatigue strength and hardness, while show disadvantages in terms of damping capacity and thermal conductivity [12, 14]. Enhanced mechanical properties of the CGI are related to round edges besides the irregular boundaries of

compacted graphite particles, which prevent crack propagation, enhance crack resistance within the matrix and improve the adhesion at the matrix-graphite interface. At the other hand, compacted graphite represents better thermal conductivity due to its interconnected formation within the matrix and growth structure when compared to nodular graphite in ductile iron [5].

Generally physical properties of the cast irons influenced by three main factors; 1) metal matrix composition (pearlite to ferrite ratio), 2) the graphite morphology (nodularity and graphite shape), 3) section size. Existence of higher pearlite in metal matrix of CGI results in high strength, while ferritic compacted graphite iron is known for its ductility and lower strength. The pearlite to ferrite ratio can vary by adding different elements or by heat treatment. In terms of nodularity, as nodularity increases, both strength elongation and fracture toughness will increase. It is worth to mention that, nodularity of compacted graphite iron must not exceed 20% [15]. According to ASTM A-247 standard, nodularity can be described using the following relation:

$$\%nodularity = \left(\frac{\text{Number of graphite types I \& II}}{\text{Number of graphite types I, II, III \& IV}} \right) \times 100\% \quad (2.1)$$

Moreover, changes in cooling rate affect the section size and consequently affect the strength of compacted graphite iron. As the section increases, the strength of compacted graphite iron will decrease. However, an increased cooling rate will increase the capability of pearlite formation and graphite nodularity percentage as well [6, 16].

2.2.1.2 Machinability of compacted graphite iron

Compacted graphite iron is widely used in today industry especially in locomotive, automotive and maritime industry. The suitable combination of excessive mechanical and thermal properties of the compacted graphite iron (CGI) and recent enhancements in its process techniques made it a promising replacement for gray cast iron and aluminum to the manufacturers of engines. However, the poor machinability of the CGI introduce some obstacles during machining of CGI parts and limits the replacement decision [17]. One of main reasons for the poor machinability of the compacted graphite iron (CGI) compared to gray cast iron is the absence of protective and lubricative layer of manganese sulfide (MnS) which is

usually formed on tool-chip interface during machining of gray cast iron [1, 18]. During machining of gray cast iron especially in high cutting velocities, MnS layer become more visible and denser, which acts as a lubricating layer and typically decreases the tool wear. However, percentage of sulfide must be decreased in order to produce vermicular type graphite phases and avoid formation of flake graphite phases. Therefore minimizing the percentage of sulfur, which is needed for compacted graphite iron formation will diminish de manganese sulfide (MnS) layer. Another element that promotes the formation of the compacted type graphite is titanium Ti. As mentioned before, adding 0.1-0.25% of titanium is used to prevent the formation of spheroidal graphite. However, high amount of titanium will results in formation of titanium carbonitride (TiCN) inclusions, which are harder than tungsten carbide (WC). Hence, presence of high amount of titanium will decrease the tool life drastically. Dawson et. al. [1, 8] studied the effect of different elements and metallurgical variables on machinability of compacted graphite iron (CGI). It was deduced that an increase in the level of titanium from 0.01 to 0.02 % will reduce the tool life dramatically by about 50% as shown in Figure 2.4.

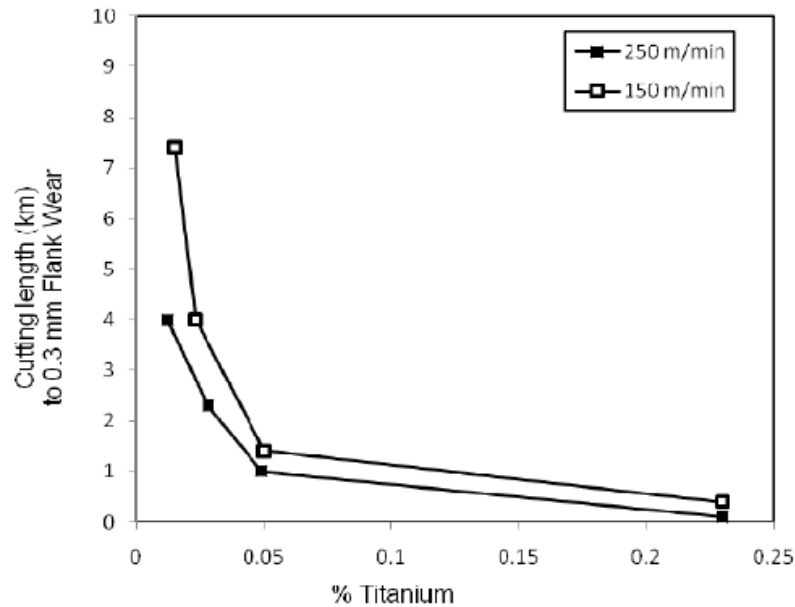


Figure 2.4 : Influence of amount of titanium on machinability of CGI [1].

Hence et. al. [19] studied the effect of MnS layer more extensively. They deduced that MnS layer prevents the adhesion of workpiece particles to the tool. In addition, they characterize the MsS layer by employing XRD, X-ray induced photoelectron spectrometry, atomic force microscopy and secondary ion mass spectrometry

methods. The authors conclude that in order to overcome the poor machinability of CGI because of MnS layer absence, altering cutting parameters such as reducing the cutting speed and replacing the single insert tools with multiple insert can be an economic solution. This will allow increasing the feed rate. Therefore, by setting the feed rate and cutting speed in a constant material removal rate, the productivity can be increased in machining of CGI besides high tool life.

Nayyar et. al. [20, 21] investigated the machinability of CGI more comprehensively. Machinability of flake, compacted and spheroidal graphite iron in continuous machining operations were studied. They have reported that using cutting fluids is advantageous in machining of compacted and spheroidal graphite iron. They have studied the tool life in different cutting speed, feed rate, depth of cut and cutting edge geometry.

Mohammed et. al. [5, 22, 23] have studied the effect of microstructure of compacted graphite iron on cutting forces, tool wear and chip formation during the machining of CGI more comprehensively. They have found that the main reason in formation of segmental chip is the crack initiation and propagation in either graphite particles in the graphite – matrix interface. They have employed finite element methods to model the formation of segmental chips during continuous machining of CGI. Through the developed model by authors, microstructure of chip, microstructure fracture during formation of segmental chip, cutting forces and tool wear mechanisms were studied extensively and validated experimentally. They have performed the model and experiments for orthogonal cutting condition. In addition, they investigated the effect of cutting forces, cutting speed and cutting temperature on tool wear in order to evaluate the optimum conditions for machining of CGI.

Later, many researchers reported that the optimum method to increase the machinability of the compacted graphite iron can be achieved by using minimum quantity lubrication (MQL) [24]. Further details on effect of minimum quantity lubrication (MQL) on machining of compacted graphite iron can be found in Bijanzad [25].

2.3 Mechanics of Drilling

The mechanics of drilling deals with the prediction of cutting forces acting on the tool by taking into consideration of tool geometry, cutting conditions and workpiece material properties. Before the cutting mechanism on the different regions of the drill bit are recognized, simple empirical models were developed in order to predict thrust force and torque. However, some analytical models proposed by different authors to predict drilling cutting forces recently.

One of the most significant approaches have been used to analytically model the cutting forces in metal cutting processes, including drilling, proposed by Merchant [26, 27], and the extension of this theory to oblique cutting model developed by Shaw et al. (1952) which was a basis of several cutting models [28]. Oxley (1989) has expanded this approach with a more comprehensive plasticity based model that assumes the region of shear as a zone rather than a plane [29]. Pal et al. (1965) implemented the shear plane model for orthogonal and oblique cutting in order to evaluate the cutting forces at each element. This is, then, multiplied by the radius of the tool and integrated along the cutting lip of drill to predict the torque [30]. Bera and Bhattacharyya (1967) developed a model to predict thrust force and torque in drilling operation and for the chisel edge. They considered that the whole chisel edge acts as an indenting wedge and the cutting lips as a cutting tool [31].

Williams (1970) determined the diameter of indentation zone is depended on feed rate value. This was later found that this approach can be used only for the 118° point angle drill. Furthermore, Williams developed a model for cutting forces at cutting edges. Their model was based on orthogonal cutting at the cutting edges and shear-plane approach. However, at the vicinity of chisel edge cutting edges have high inclination angles like -30° , which neglecting this fact and assuming cutting edges as an individual orthogonal tool geometry causes significant errors in predicting cutting forces [32].

Armarego and Cheng (1972) proposed the methodology to predict the drilling thrust force and torque using oblique cutting approach for flat face drills and then extended to conventional drill by utilizing geometric similarity between drills. The results for the modified drill was satisfactory but the predictions for the conventional twist drill bit contained considerable errors [33, 34]. Wiriyaosol and Armarego (1979) also

used this approach for calculation drilling forces. Basically, this method consist of dividing cutting edges into limited number of cutting elements which act as oblique cutting tools and the chisel edge for orthogonal cutting elements. The basic data required for the oblique cutting element force prediction was obtained from classical orthogonal cutting test. However, the method relied mostly on empirical and experimental approaches for determining cutting parameters and equations [35].

Later, Rubenstein (1991) attempted to calculate the drilling thrust force and torque for conventional twist drills by assuming them as a spade drills. In this method, Rubenstein considered average rake and inclination angles to determine an equivalent spade drill, therefore, the forces calculated using orthogonal cutting approach. This approach lead to incorrect prediction since there are variety of cutting angles along the cutting edges of a twist drill or other drills with different geometries [36].

Watsons (1985a,b) first considered the cutting lips as a number of cutting elements based on oblique machining theory and the orthogonal cutting theory for the chisel edge to model the thrust and torque in drilling. The model is not in an acceptable agreement with the experimental results. He then proposed that each element could actually not have its own chip flow angle [37, 38]. By applying some modifications on the chip flow angles, a shear plane angle solution have been obtained which had less variation than the previous approach. This method has reduced the deviation from experimental data; however, still did not match the experimental data for different point angle test. Watson (1985c,d) presents a new material removal mechanism at the chisel edge region. This is assumed to be the combination of oblique cutting and extrusion at the vicinity of drill center. Nevertheless, this approach had good agreement only with the varying feed experiments and high errors with varying point angle and spindle speed in comparison with experimental data [39, 40].

All the above-mentioned models have discussed the force model prediction in conical drill point geometry. Stephenson and Agapiou (1992) have made one of the distinguishable attempts in modeling of thrust and torque in arbitrary drill point geometry. In their model a polynomial equation is fit to points measured along the cutting lips and an algorithm developed to equivalent conical drill parameters for points along the lips of arbitrary drill points [41]. Chandrasekharan (1996) has made

the best approach for arbitrary point angle drills. He developed a mechanistic model in which the three dimensional cutting forces in drilling can be predicted. The drill geometry consist of three zones: oblique cutting on the cutting lips, indentation at the center of the drill and orthogonal cutting on the remaining regions of the chisel. In this approach, cutting lip has been segmented to limited number of elements with specific cutting angles and chip thickness. Then the model coefficients for a tool and workpiece combination are determined from a curve fit to experimental data for calibration purpose using a conical twist drills and these are used for the other point geometry drills such as Racon, Helical, Bickford and Double angle drill points [42]. Elhachimi et al. (1999) investigated the drill model and proposed an approach to calculate the distribution of thrust and torque along the cutting edges. The method consists of integration scheme to predict cutting force where the integrand is a function of drill geometry, feed and properties of work material. In order to evaluate the predicting equations, they considered the total cutting edges instead of dividing into elements, therefore the inclination angle and normal rake angle have been assumed to be constant along the cutting lip of drill [43]. More recently, Pirtini et al. (2005) have developed a novel mathematical model, which is based on mechanics of drilling. In this method, cutting coefficients can be obtained directly from set of calibrations test and then cutting forces can be evaluated [44]. Moreover, Armarego has done significant works in modeling of drilling forces by using orthogonal to oblique cutting transformation method, which has been extended by Altintas, and this approach adopted here [45].

2.4 Machining Induced Residual Stress

One of the most important effort in understanding the machining induced residual stresses was performed by Henriksen [46]. In this work it is concluded that the both thermal and mechanical effects have contribution in generation of residual stresses. However, thermal stresses have negligible role while mechanical effects' influence dominated. He also found that ductile materials introduce tensile residual stresses while in brittle materials, compressive residual stresses are generated after machining. Okushima and Kakino [47] were the first researches who used Henriksen's approach and implemented at modeling residual stresses with FEM. They confirmed the modeled results with experimental data using XRD method.

They found that thermal effects resulted in tensile residual stress while the mechanical loads introduce compressive residual stresses.

Liu and Barash [48] performed orthogonal and oblique cuttings to study the effect of machining variables on behavior of residual stress on a machined surface. They examined variables included the length of the shear plane, tool flank wear, shape of the cutting edge, and the depth of cut. Liu and Barash also concluded that thermal expansion has a negligible contribution on formation of residual stresses. They suggested that mechanical loadings are the main reason of both compressive and tensile residual stress while the thermal strains do not have significant influence. Further studies performed by Schlauer [49] and Jang [50] to study the residual stresses in turning operation. Schlauer found that nano-sized grains cause tensile stresses while shear bands in subsurface results in compressive stresses. Jang also investigated the effect of machining parameters on residual stress pattern during turning. The advanced experimental approaches for studying the residual stress carried out on hard turning. Matsumoto [51] performed hard turning experiments and investigated the effect of feed rate and depth of cut in residual stress in the subsurface of component. He also concluded that hard turning induce high compressive residual stresses due to high plastic deformation rate. Similarly, Jacobson [52] conducted hard turning experiments on hardened steel. The main focus of Jacobson's work was on the effect of nose radius and effective rake angle on residual stress behavior of component. He found that higher negative rake angle and smaller nose radius expose compressive residual stresses. They concluded that either of the nose radius and rake angle influence the amount of residual stress.

Sadat [53] investigated machining induced residual stress in Inconel718. This research reported the effect of cutting velocity and tool-chip contact length on the surface integrity of machined component. He concluded that surface and subsurface of the component where undergoes a plastic deformation is being affected by both mechanical and thermal effects. It is deduced that reduction of cutting speed increase the depth of the region that residual stress appears. This is due to lower temperature for lower cutting speeds.

Dahlman [54] studied the effect of feed rate on residual stress distribution when hard turning of AISI 52100 using sharp tools. The effect of feed rate is not significant on magnitude of surface residual stress, however, it influence the subsurface layers

seriously. It is reported that increasing feed rate increases the magnitude of compressive residual stresses at subsurface layers. Later, Hua [55] validated the same results using FEM approach for dry orthogonal cutting of the same material and compared to experimental measurements. They suggested that increase in compressive residual stress with rise of feed rate was attributed to having higher tensile loadings being applied to the workpiece behind the tool during cutting operation.

Liu [56] has conducted a similar work, which examine the effect of nose radius and flank wear on residual stress produced in hard turning. Results indicate that flank wear affects the residual stress pattern significantly.

The above-mentioned studies have provided a general understanding of residual stresses during machining operation using experimental measurements. Basically, the results have shown that residual stress profile depend on the combination of thermal and mechanical loading. If mechanical loading dominate, the resulted residual stresses will be compressive, where thermal loads dominate, the residual stresses show tendency to be more tensile.

Besides the experimental efforts, analytical modeling of residual stresses has a significant contribution on understanding of residual stresses. In this regards, Wu and Matsumoto [57] employed the idea of passing load over a point on the surface of the component in order to model the mechanical loads. They suggested that, in cases that loading are predominantly compressive, the resulting residual stresses will be tensile when strains declined to zero. Whereas, for loads that are primarily tensile, the resulting residual stresses will show compressive tendency as the strains reached to zero. They implemented the mentioned model to machining process by employing Boussinesq equation to predict the stress history occur in the subsurface region of component.

One of the most powerful analytical models presented by Jacobus [58]. This research used an incremental plasticity model. In addition, this work assumed deformation of material beneath the tool, rather than a stress field in the material and cutting region. The results are investigated in terms of cutting edge radius and depth of cut. Required parameters for modeling were calibrated from experimental tests. He explained the effect of thermal loads and mechanical loads on the residual stress

pattern within his model. This model has a significant contribution in basis for residual stress modeling, whereas based on curve fitting techniques.

Many research works have been performed to investigate the machining induced residual stresses using FEM. Shih [59, 60] developed a plane-strain finite element simulation of orthogonal metal cutting. The presented model by Shih included effects of material parameters such as elasticity, viscoplasticity, temperature, and high strain-rate. The predicted data compared to experimental results. In addition, he investigated the effect of rake angle in formation of residual stresses during orthogonal cutting.

Ozel [61, 62] conducted several research works on modeling of residual stresses using FEM approach. These studies encompasses the effect of tool coating, friction coefficients, cutting parameters, tool geometry and material properties on residual stresses behavior during machining. Although these studies have a significant contribution on understanding and study of residual stresses, FEM approach is not the focus of interest at present thesis.

Besides the many research works carried out to investigate the residual stress pattern qualitatively and quantitatively in turning, orthogonal and oblique cutting operations, several researches have been published by Aspinwall [63] and Fergani [64] for milling and Fergani [65] for grinding. However, there have been scarcely research works regarding surface integrity of drilling operation. While the residual stress distribution in holes is important critical pertaining to the fatigue performance of aerospace and automotive component. Whereas, obtaining residual stress distribution and pattern in holes is difficult indeed due to the problems related to the residual stress measurement constraints on curved surface reported by Prevey [66].

The most recent work carried out to investigate the residual stress distribution during hole making operations has been performed by Kwong [67]. Authors investigated the influence of cutting parameters and conditions on surface integrity of Ni-based superalloy in hole drilling. These conditions also studied for drilling roughing and finishing by plunge milling after drilling operations. For both situations, finishing of surface and surface roughness of machined surface was inspected. Later, the metallurgical analysis carried out to investigate the surface in terms of material drag, white layers, lapping or cracking occurrence in all machining condition. Moreover,

micro hardness profile of drilled and plunge milled surface was obtained to have a better understanding of the effect of work hardening and hear gradients in microstructure. Residual stresses measured using XRD method by incremental layer removal process in both hoop and axial directions. It is deduced that axial stresses have more tendency to be compressive while the hoop stresses show tensile behavior. It shows that mechanical deformation is responsible for compressive stresses, whereas thermal stresses may be dominant for no coolant cases and introduce tensile stresses.

Brunnet [68, 69] investigated the residual stresses along the holes after Autofrettagged and after subsequent boring and reaming operations on AISI 4140 steel. Having induced large amount of compressive residual stresses to holes, reaming and boring operations are carried out. Having used the XRD method for surface residual stress measurements, incremental hole drilling technique with electronic speckle pattern interferometry (ESPI) have been employed to measure residual stresses in depth. The experimental residual stress profiles are compared to numerically obtained results from FEM analysis. It is deduced that the residual stress profiles are very compressive; however, finishing operations like reaming causes a loss in beneficial compressive residual stress. In addition, it is found that during the reaming of holes, mechanical loads are dominated compared to thermal loads.

3. DRILL BIT MODEL

3.1 Overview

In this chapter geometry of the twist drill bits commonly used in industry and the effect of different geometrical specification on the cutting mechanics described comprehensively. Since the main focus of the study is on the twist drill bits, the mechanism and the properties of the indexable drill bits are excluded. Twist drills consist of a solid body made of high speed steel (HSS), solid carbide or combination of both materials (brazed). It is to be noted that in brazed type drills the cutting region is usually made of solid carbide welded to the steel shank. The geometry of the twist drills can be obtained by grinding a cylindrical bit. Therefore, it is possible to re-grind and sharpen the tip of the drilling tool during long operation times. In addition, in order to increase the cutting performance and tool life, different coatings can be applied to the drill bit tool. Typically, coatings increase the tool life by improving the friction properties and wear resistance.

3.2 Principles of Cutting Mechanics in Drilling

A drill bit is being used to making holes in materials by rotation of tool or workpiece. This operation is done by using a chisel at the center of the drill and two or more cutting edges. The chisel works as indenter to center the drill during the cutting path. The most material removal rate occur by shearing the material by symmetric cutting edges. Symmetry of cutting edges and flutes in drilling plays a crucial role in avoiding undesirable hole profiles, lateral deflection and whirling vibrations. If the drill bit ground perfectly, the both cutting edges will cut the same volume of chip. However, any difference in symmetry of the tool geometry leads to different hole profile. Lateral deflections of a drill bit are caused by unequal distribution of cutting forces, due to grinding errors, workpiece material inhomogenities and misalignment of the drill bit with tool holder axis and leads to whirling vibrations. These vibrations

usually produce a three-sides hole profiles in two-fluted drill bits. Hence, grinding accuracy in producing twist drills are essential especially in solid carbide drills and brazed drills (steel shank with solid carbide cutting area).



Figure 3.1 : a) Twist drill geometry b) indexable drill geometry [2].

The only types of the drills that the asymmetry is inevitable are the indexable drills. The main body of the indexable drill is made of steels, but accommodates exchangeable-coated inserts at the periphery and the center of the tool. An indexable drill is always unbalanced due to the asymmetric design and unequal weight and geometry of the inserts. Therefore, unbalanced rotation causes unbalanced force during cutting and finally leads to non-circle hole shape.

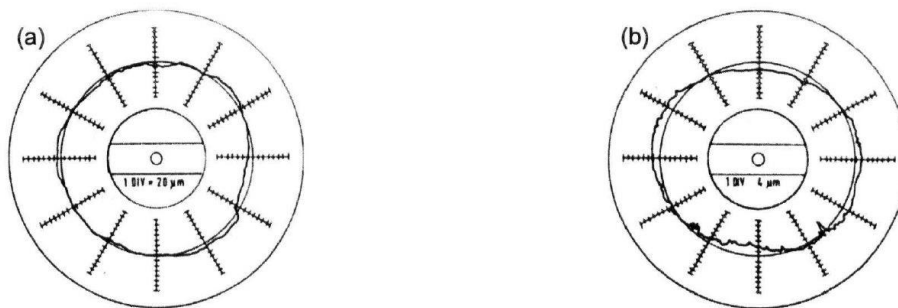


Figure 3.2 : a) 18 mm hole profile generated by a twist drill b) 18mm hole profile generated by an indexable drill (oval shape) [70].

Cutting forces also vary by cutting conditions and drill geometry parameters that will be discussed in subsequent sections.

Mechanics of drilling deals with the cutting forces acting on the tool during the material removal operation. A mechanistic model helps to predict the cutting forces by using tool geometry parameters under various cutting conditions. The cutting parameters, illustrated in Figure 3.3, which affects the cutting mechanics are as following: a) Cutting Speed V_c – measured at the periphery of the drilling tool [m/min], b) feed f – the distance that the tool moves in to workpiece per revolution

[mm/rev], and c) radial depth of cut b – determined by the length of the cutting edges where engaged with the workpiece and removes material. Radial depth of cut can vary by employing the pilot holes which have smaller diameters in comparison with the main cutting tool.

The uncut chip thickness and uncut chip area is determined by the feed value as illustrated in Figure 3.3. Uncut chip thickness value depends on radial depth of cut and feed rate value. If there is no vibration during cutting operation each cutting each cuts in the same chip thickness. As shown in Figure 3.3, if a twist drill has two flutes, each tooth cuts half the feed value per revolution in ideal condition. However, if the number of flutes is more than two, the chip thickness per tooth will vary. The cutting thrust force F_z and torque T depend on the uncut chip area. Different geometries of twist drills results on various geometries of the cutting edges which affect the distribution of the chip load along cutting edges. Since the geometry varies along the cutting edges, governing laws of the cutting mechanics will change significantly, which is the main focus of the thesis and the related calculations presented in this study.

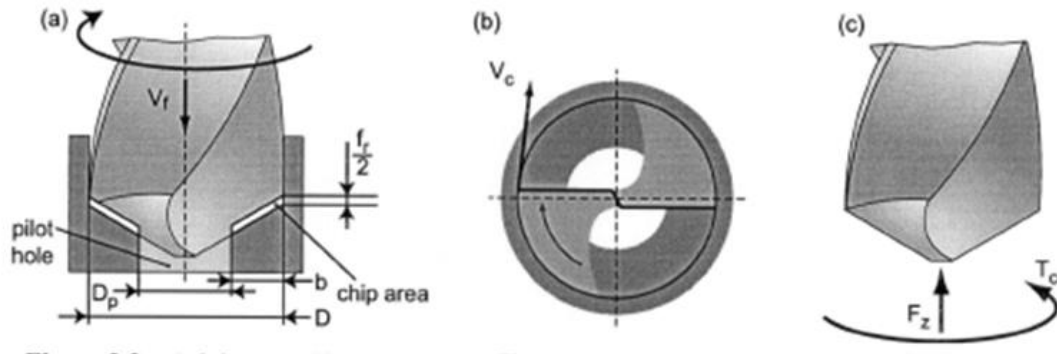


Figure 3.3 : a) Definition of feed and uncut chip area, b) direction of cutting speed, c) main cutting forces in drilling [2].

3.3 Twist Drill Structural Properties

The schematic view of the normal and commonly used drill bit illustrated in Figure 3.4;

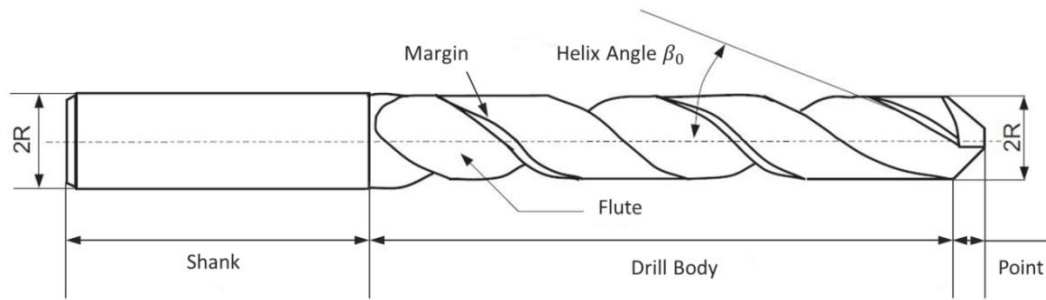


Figure 3.4 : Twist drill geometrical parameters.

From the structural point of view, a drill bit consists of the shank, drilling body and the drill point. The shank part of the bit is used to clamping the drill to tool-holder. The design of shank is important since it determines the roundness accuracy and stiffness of the drill bit. Drill point cuts the material and conveys the chips out of the hole through the flutes. The flutes grinded on the drill body do not cut material, but have the responsibility to evacuate the chip to outside the hole and brings the coolant to the cutting zone. The most important factor regarding the flutes of the drill is helix angle. The helix angle affect the cutting mechanics by influencing the rake angle distribution along cutting edges and also chip evacuating time. In addition, geometry of the drill point is the most critical issue in drill bit design since the material cutting occurs at this part. Since the structural properties of the drill bit directly influence the cutting mechanics and resultant hole quality and forces, each parameter studied separately as following:

- **Helix Angle:** Helix angles generally falls into three categories of slow helix angle, regular helix angle and high helix angle as illustrated in Figure 3.5. The influence of each type discussed as follows:
 - **High helix angle:** These types of drills have the helix angle of approximately 34° - 40° along the drill body. These drills are used to drill softer ferrous and non-ferrous materials such as low-carbon steel, aluminum, magnesium, copper and plastics, which produce stringy and continuous chips. High helix drills are generally used in deep hole drillings since it provides better lifting power for chips. However, it elongates the path of chip ejection, there is the risk of chip clogging in high helix angles and therefore leads in lip chipping and also poor heat

dissipation. In addition, high helix angle is detrimental to the drill body stiffness and strength of cutting edges due to the high rake angles.

- **Regular helix angle:** A wide variety of the drilling tools has the helix angle of $28^\circ - 32^\circ$. In comparison with slow helix angle, increasing the helix angle in drill bit results in higher rake angles and lower torque and thrust force values. In addition, increasing the helix of angle irregular range will enhance chip formation and chip ejection.
- **Slow helix angle:** This type of drill have the helix angles of $12^\circ - 22^\circ$. Due to the low lifting force for chips, therefore, weaker chip ejection capability, this type of drills are usually used to drill material producing broken chips such as brass, bronze, aluminum alloys and some hard-to-machine materials. They are commonly used in drilling shallow holes (up to 4 times the drill diameter) due to the low chip ejection capabilities.

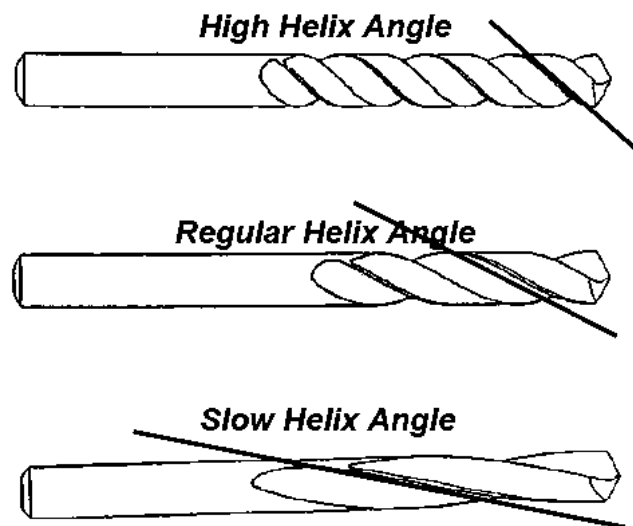


Figure 3.5 : Representation of different helix angles.

- **Point angle:** As a definition, the angle between the projection of the cutting lips upon a plane parallel to the drill axis and parallel to the cutting lips, called point angle. As illustrated in the Figure 3.6, the angle so-called $2\kappa_t$ is the point angle of the drill bit. Point angle is the one of significant parameters that influence the point geometry of the drill bit, which affects the cutting mechanics. As point angle varies, the length of the cutting edges change and the force distribution alter significantly. A standard point angle recommended for commonly used drill bits is 118° . In drills for general purpose, the drill

flute contour and helix angle are usually designed to produce a straight cutting edges with the point angle of 118° . However, the selection of the point angle value depends on the application and the material of workpiece. An optimum point angle which yields maximum tool life and hole quality exist for every material. In general, higher point angle increases the thrust force and decreases the torque value. As the point angle of the drill increases the axial force also rises than can affect drill buckling stability [71]. It is to be noted that the thrust force varies parabolically with the point angle and reaches to the minimum point at approximately 118° . Point angles greater than 118° (roughly about 130°), are recommended for harder and tougher materials since it would have stronger cutting edges. However, point angles lower than 118° are generally preferred for soft non-ferrous materials, copper, fiber aluminum alloys and abrasive materials. As a result, since the higher point angles produce high axial force and consequently high penetration rate, it is recommended to use high point angle drill for drilling of hard materials.

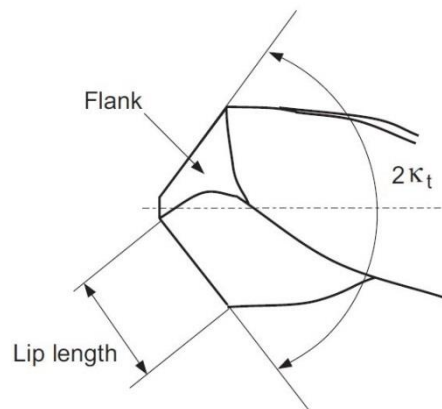


Figure 3.6 : Geometry of point at conventional twist drills.

- **Web thickness:** Drill web thickness directly influences the torsional and bending strength. Obviously, the larger the web thickness, the greater the drill strength. Web thickness determines the drill rigidity and deflection of the drill bit which affect the hole quality and tool life. The web thickness represented as $2w$ in the Figure 3.7. Usually web thickness occupies about 15% to 20% of the drill diameter, but it may reach up to 40% - 50%, which are called wide-web drills. The so-called wide web drills has more rigid and stronger body than conventional drills, thus higher penetration rates and better accuracy of

holes are achieved. Therefore, these kinds of drills are normally used to produce holes in high-strength steels, difficult-to-cut materials and heat-treated steels.

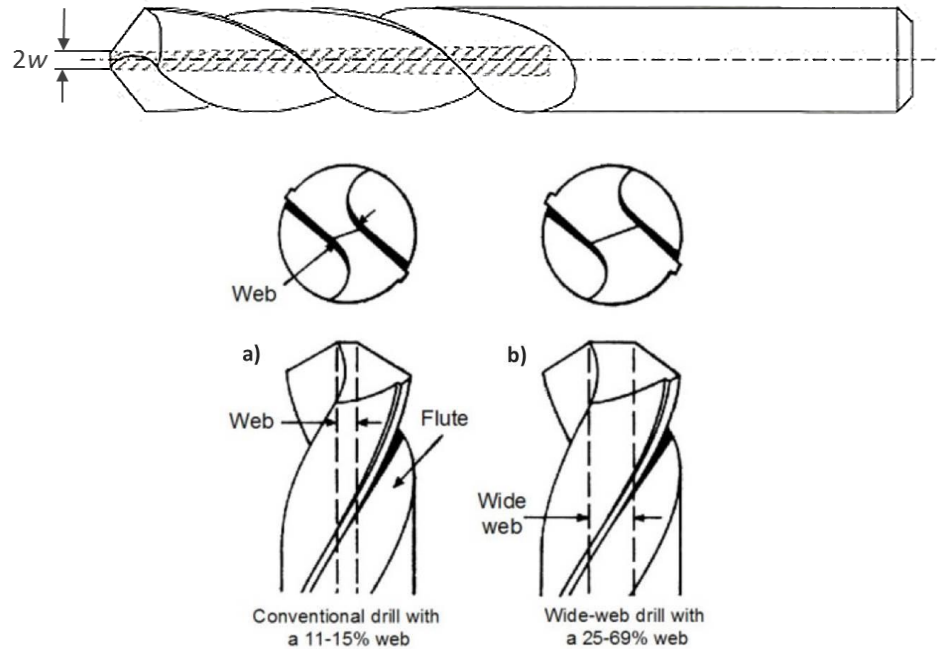


Figure 3.7 : Definition of web thickness a) a normal web drill b) wide web drill.

- **Chisel edge angle:** The angle between the chisel edge and the cutting lips defined as chisel edge angle. Chisel edge angle is important in means of point geometry and cutting performance. Chisel edge angle, chisel length and web thickness are the parameters that affect each other, thus the cutting performance and tool life vary. Moreover, this angle influence the distribution of the relief angle along the cutting edges, values of the rake angle along the chisel edge. Also, a smaller chisel edge angle leads to a greater relief angle along the cutting edge and a longer chisel edge. It is worthy to note that as the point angle increases, the chisel edge point decreases and length of this edge increases [72].

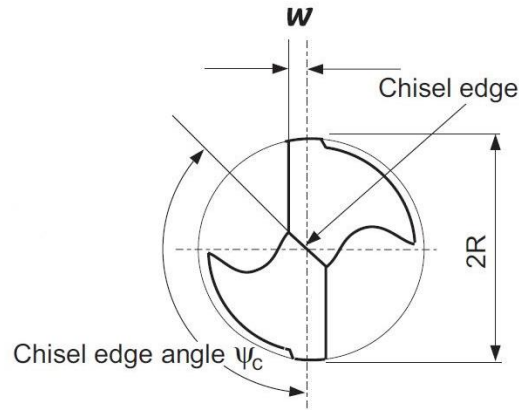


Figure 3.8 : Schematic illustration of chisel edge angle.

3.4 Split-Point Drill Properties

As mentioned before chisel edge has a small length in comparison with drill diameter. In conventional drills the chisel part does not cut the material but in plough the material with high plastic deformation rate. Therefore, it has a big contribution in resultant axial force besides in penetrating the workpiece. It has been reported that in a conventional drill, chisel edge contribute about 50-60% to the thrust force where the remaining thrust force belongs to the cutting edges [72]. Therefore, tip modifications, which affect the geometrical parameters of the both chisel and cutting edges, can reduce the resultant axial force. Optimizing the geometry of the drill point not only reduce the overall thrust (axial) force but also alters the contribution of the drill components to this force. Thus, this modification results in improvements in tool life, part quality and accuracy.

High axial force in drilling restricts the feed rate significantly due to the following reasons:

- It influences the buckling stability of the drill excessively. Buckling instability causes low hole quality, low tool life in terms of both tool tip wear and margin wear. In addition, high buckling in drill bit causes tool breakage during the operation.
- Thrust capacity in spindle systems of the machines limits the high feed rates especially in conventional drill.

Therefore, in order to increase the allowable penetration rate of the drill reduction, reduction of the resultant thrust force is crucial. Since the chisel edge has the most contribution to this force two modification can be applied: 1) reducing the length of chisel edge 2) improving the geometry of chisel edge. There are several point

modification method, so-called “point thinning” with different applications and properties. However, the focus of this section is on split-point drills. As can be seen in Figure 3.9, point part of the split-point drills undergoes an additional grinding operation shown as painted area. As a result of this grinding, the flank face formed by two planes with an additional cutting edge at the chisel area. This additional cutting edge cuts as an orthogonal cutting edges with the rake angle of 0° - 5° in spite of conventional drill which have negative rake angles [72].

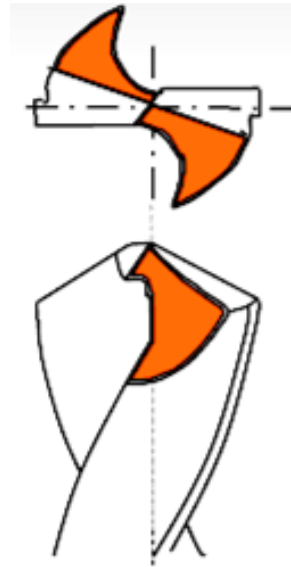


Figure 3.9 : Geometry of split-point drill.

As a result, a split-point drill consists of three cutting areas: 1) indentation point at the center 2) orthogonal cutting edges at chisel area 3) major cutting lips. The most important advantages of the split-point drill can be listed as follows:

- Self-centering nature of the drill due to the elimination of chisel and existence of cutting edges at this area
- Reduces whirling and drill wandering
- Minimized chisel length and reduced thrust force consequently
- High penetration rates can be achieved in constant cutting parameters in comparison with conventional drills
- Split-point drills produce positive rake angle which improve the chip cutting ability at the center of drill point

Figure 3.10 shows the carbide drilling tool employed in this study manufactured by Karcan Cutting Tools Company. As can be seen, the utilized tool is a split-point tool which the geometrical specifications indicated at subsequent section. The following section, mechanistic force modeling of the split-point drill discussed.



Figure 3.10 : Split-point drill used in experiments.

4. MECHANICS OF DRILLING

4.1 Overview

In this chapter, modeling of cutting forces generated in drilling process is presented. The prediction of cutting forces is performed using theoretical and experimental approaches simultaneously. Mechanistic modeling of drilling operation lies on geometry of tool, cutting conditions and workpiece material properties. Primarily, orthogonal cutting experiments are done as experimental approach in the model to obtain the initial data that correspond to properties of workpiece material and tool combination. These experiments have been performed in a large range of cutting conditions in order to cover the material properties in variety of cutting speed and feed rate values. Finally, using the data corresponds to material properties, parameters of drill geometry and cutting conditions cutting forces generated in drilling are modeled.

Due to the complex geometry of twist drill, cutting force modeling has been done in different phases. Since the geometry of drill varies dramatically along the cutting lip and chisel edge that influence the modeling process extremely, different modeling strategies performed for chisel edge and cutting lips. As can be seen in Figure 4.1 (a), at the drill center, chisel edge is ploughing the material with negative rake angles at the vicinity of the center. In conventional drills, whole the chisel edge area acts as a rotating indenter extruding the material on both sides of the chisel edge [2]. Figure 4.1 (b) represents the cutting conditions at vicinity of drill center at the chisel edge area with a highly negative rake angle and low cutting speed. In this part chisel edge produces irregular chips. Due to the friction and high plastic deformation at this area, cutting temperature is high. Figure 4.1 (c) and (d) show the rake angle increase along the cutting lips, respectively. As can be seen, moving from the center of the drill toward the periphery of drill, both rake angle and cutting speed increase, shearing the material become easier, consequently, more regular chips are achieved.

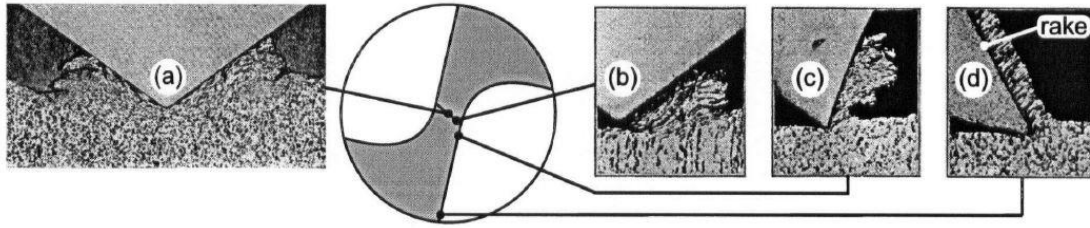


Figure 4.1 : Cross sectional views of drilling process a) indentation at the center of the drill; b) orthogonal cutting with high negative rake angle at the vicinity of the chisel edge; c) Oblique cutting with negative rake angle on the cutting lip; d) oblique cutting with positive rake angle on the cutting lip [73].

4.2 Cutting Mechanics for Prediction of Drilling Forces

This section presents the steps for evaluating the cutting force model for drilling operation. Critical cutting forces in drilling operation are thrust force, which is in the axial direction and the torque. Total axial force and torque of the twist drill are calculated by sum of torque and axial force at each cutting lips and chisel edge. Since the split-point twist drills are employed in this thesis, chisel edge consist of two areas; indentation point and cutting chisel edge. Corresponding thrust force for the indentation zone was obtained supposing as an indenter in material. The cutting chisel edge area has been divided into several segmentations and each segment considered as an orthogonal cutting tool. Since the geometry of the tool changes dramatically along the cutting lips in terms of vast range of rake and inclination angles, the cutting lips were divided into several segments and the oblique cutting mechanics applied to each segment. In order to calculate the corresponding oblique cutting forces for each segment, orthogonal tube cutting tests were performed in a large variety of rake angles and cutting conditions (cutting speed and feed rates). Obtaining the material and tool pair properties using orthogonal cutting mechanics, oblique cutting forces were calculated for each element of cutting lips. Twist drill geometry were employed to transform the elemental cutting forces to the drill coordinates and evaluate the total axial force and torque. Finally, drilling experiments were performed and the measured cutting forces were compared with the ones predicted.

4.2.1 Mechanics of orthogonal cutting

The most simple and elementary form of cutting which the cutting operation happens in two-dimensional form is orthogonal cutting. This general case is usually used for defining the principles of metal cutting operation. Most of the commonly-used machining operations such as turning, milling, drilling, and broaching have three-dimensional cutting mechanics. However, evaluating the three-dimensional cutting forces needs to obtain the principle cutting forces and tool-workpiece pair properties in two-dimensional orthogonal cutting. Figure 4.2 represents an orthogonal cutting operation. The main specification of orthogonal cutting is that the straight edge of the cutting tool is perpendicular to the cutting velocity V and cutting direction. In orthogonal cutting, cutting assumed to be uniform along the cutting edge with a two-dimensional shearing without side spreading of the material [45]. As can be seen in Figure 4.2, a tool cuts and shear away the material with a width of cut b and depth of cut (or uncut chip thickness) h . Hence, the cutting forces are generated only in two dimensions, which are tangential force F_t in cutting velocity direction and feed force F_f in uncut chip thickness direction. The width of chip will be altered after cutting which is shown as h_c .

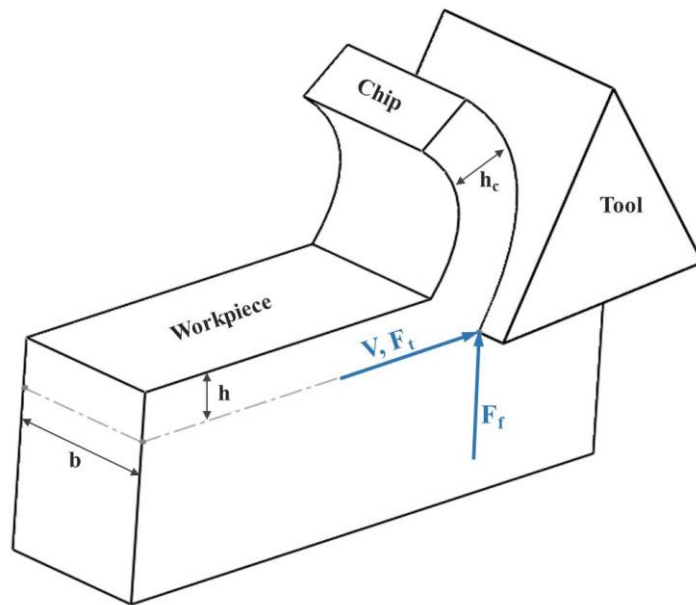


Figure 4.2 : Schematic geometry of orthogonal cutting.

As the tool penetrates in the workpiece material, the material ahead of the cutting edge sheared over the primary deformation zone, consequently, chip is generated.

The generated chip moves along the rake surface of the tool, which is called the secondary deformation zone. The area between the flank face of the tool and machined surface of the workpiece called tertiary deformation zone. In this zone, flank face of the tool rubs the workpiece and cause ploughing and friction. Representation of three deformation zones is shown in Figure 4.3.

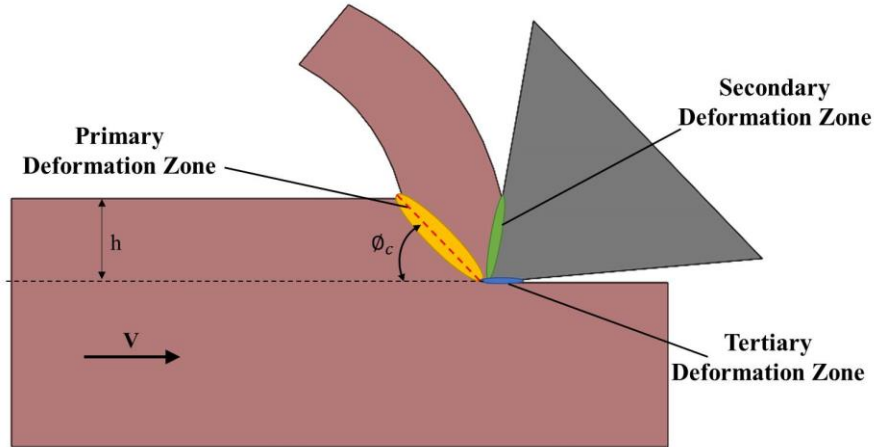


Figure 4.3 : Deformation zones.

There are basically two approaches in analysis of primary deformation zone. Firstly, Merchant assumes the shear zone as a thin plane [74]. Another approach developed by Oxley and Palmer in 1959 lies on the theory that, shear zone in material is a thick shear deformation zone, proposing “shear angle prediction” models in accordance with the laws of plasticity [75]. This assumption is based on plasticity laws, which are dominant during metal cutting, and usually is used in micro-machining modeling studies. In this study, shear zone in primary deformation zone considered as thin plane and Merchant’s model is employed. After the material sheared in primary deformation zone, chip will move on the rake face of the cutting tool and generates the secondary deformation zone. This region consist of two areas with different characteristics; sticking zone and sliding zone [76]. In sticking zone chip sticks to the rake face of the tool due to the high pressure (friction stress) generated due to the shearing of the material and chip moves over. The friction stress is approximately equal to yield shear stress of the material τ_1 at the sticking zone with the length of L_p where the chips moves over the material stuck at the rake face of the tool. Immediately after sticking zone, sliding zone begins. At sliding zone with the length of L_c , chip slides on the rake face of the tool with a constant friction coefficient. At this zone shear stress is equal to product of the sliding friction coefficient and the

normal stress P which Coulomb's friction law can be applied. Figure 4.4 resembles the distribution of stress at both sticking and sliding zones.

$$\begin{cases} \tau = \tau_1 & x \leq L_p \\ \tau = \mu P & L_p \leq x \leq L_c \end{cases} \quad (4.1)$$

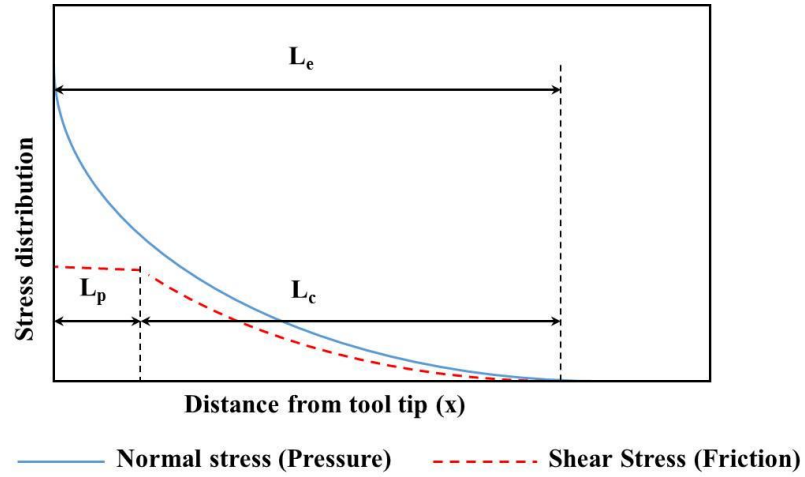


Figure 4.4 : Stress distribution at tool-chip contact zone.

Determination of sticking-sliding contact length on the rake face is important in terms of cutting mechanics and tool wear. Several studies have been done to understand the influence of cutting conditions, workpiece material properties, tool geometry and coating and cutting geometry on friction behavior at both sliding and sticking regions [77, 78].

Regarding to the force equilibrium in Figure 4.5, resultant force F_c is formed from the tangential force F_{tc} (in direction of cutting velocity) and feed force F_{fc} (in direction of uncut chip thickness). As can be seen in the Figure 4.5, all the forces generated in the first and secondary deformation zone, and also the cutting forces are in equilibrium to the resultant force F_c . This equilibrium is schematically represented in Figure 4.5, so-called “Merchant Circle”.

$$F_c = \sqrt{F_{tc}^2 + F_{fc}^2} \quad (4.2)$$

These forces can be called as “principal cutting forces” since they can be easily measured during experimental orthogonal cutting. Therefore, theoretical formulation of these forces can help to predict and compare with the measured principal forces from experiments. Since the generated forces at three deformation zones affect

orthogonal cutting mechanics, all the forces are exerted at these areas must be studied and evaluated in order to obtain the formulations for feed and tangential forces. The mechanics of orthogonal cutting for three deformation zones are shown as follows;

Primary Shear Zone

In orthogonal cutting geometry, it is assumed that the cutting edge of the tool is sharp without a radius and the shearing takes place at infinitely thin shear plane. The angle between the shear plane and the direction of cutting speed is shear angle ϕ_c . It is also assumed that the shear stress τ_s generated at the shear plane and the corresponding normal stress σ_s is constant along the shear plane. The shear force F_s acting on shear plane is derived from the geometry as follows:

$$F_s = F_c \cos(\phi_c + \beta_a - \alpha) \quad (4.3)$$

where β_a is the average friction between the tool's rake face and the moving chip, α is the rake angle of tool. We can also describe the shear force in terms of the feed and tangential cutting forces as following:

$$F_s = F_{tc} \cos(\phi_c) - F_{fc} \sin(\phi_c) \quad (4.4)$$

Similarly, the normal force acting on the shear plane can expressed as:

$$F_n = F_c \cos(\phi_c + \beta_a - \alpha) \quad (4.5)$$

$$F_n = F_{tc} \sin(\phi_c) - F_{fc} \cos(\phi_c) \quad (4.6)$$

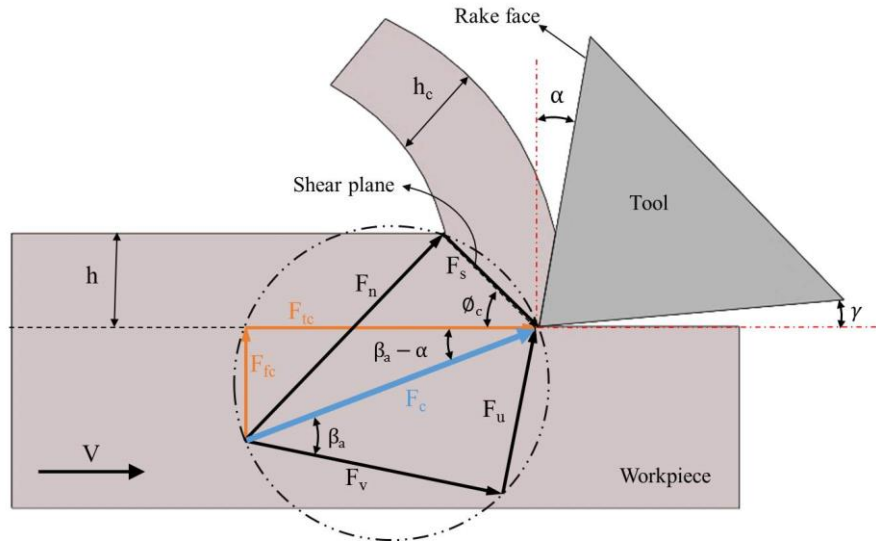


Figure 4.5 : Cutting force diagram in orthogonal cutting process

Since the shear stress distribution assumed uniform and constant along the shear plane, the shear stress can be expressed as

$$\tau_s = \frac{F_s}{A_s} \quad (4.7)$$

where the area of shear plane A_s is

$$A_s = b \frac{h}{\sin(\phi_c)} \quad (4.8)$$

Similarly the normal stress acted on the shear plane can be described as:

$$\sigma_s = \frac{F_n}{A_s} \quad (4.9)$$

where the b is width of cut and h is uncut chip thickness. Regarding to the chip deformation geometry, shear plane length is found to be

$$L_{cs} = \frac{h}{\sin(\phi_c)} = \frac{h_c}{\cos(\phi_c - \alpha)} \quad (4.10)$$

h_c is the deformed chip thickness as shown in Figure 4.5. This parameter is used to define chip deformation ratio r_c as follows

$$r_c = \frac{h}{h_c} \quad (4.11)$$

By employing geometry of orthogonal cutting and chip deformation ratio, the shear angle can be calculated using the following formula

$$\phi_c = \tan^{-1} \frac{r_c \cos(\alpha)}{1 - r_c \sin(\alpha)} \quad (4.12)$$

Secondary Shear Zone

In order to study the force equilibrium in secondary shear zone, an average constant friction is assumed at the chip-tool's rake face contact zone [45]. In this study influence of sticking zone length and sliding zone length on cutting mechanics are not taken into consideration. As shown in Figure 4.5, there are two force components acting on the rake face of the tool. Using the geometry of cutting the normal force F_v is found to be:

$$F_v = F_{tc} \sin(\alpha) - F_{fc} \sin(\alpha) \quad (4.13)$$

similarly the friction force at the chip sliding direction on the rake face;

$$F_u = F_{tc} \sin(\alpha) + F_{fc} \cos(\alpha) \quad (4.14)$$

In the present study, it is assumed that the chip slides on the rake face of the tool with a constant and an average friction coefficient of μ_a . This friction coefficient is the apparent friction coefficient that can be evaluated from orthogonal cutting tests [77]. However, in reality, the chip sticks to the rake face of the tool at the sticking contact area and then slides over the rake face at the sliding zone with the constant apparent friction coefficient [76]. The average apparent friction coefficient at the rake-chip contact zone is given as;

$$\mu_a = \tan \beta_a = \frac{F_u}{F_v} \quad (4.15)$$

The friction angle β_a can be found from tangential and feed force alternatively.

$$\tan(\beta_a - \alpha) = \frac{F_{fc}}{F_{tc}} \rightarrow \beta_a = \alpha + \tan^{-1} \frac{F_{fc}}{F_{tc}} \quad (4.16)$$

The friction angle depends on the lubrication used, tool coating, and the tool and workpiece materials. As a result, the chip-rake contact length can be approximately calculated as follows;

$$L_e = \frac{h \sin(\phi_c + \beta_a - \alpha)}{\sin \phi_c \cos \beta_a} \quad (4.17)$$

The total power consumed in cutting is the sum of energy spent in the shearing the material at primary deformation zone and the friction force at rake contact zone. Similarly, the heat generated during the metal cutting is consequence of high plastic deformation at shear plane and the friction between tool and chip. The friction power P_u is the main result for increasing of the temperature of tool and chip.

$$P_u = F_u V_c \quad (4.18)$$

where the V_{chip} is the deformed chip velocity which slides on the rake face,

$$V_{chip} = r_c V = V \frac{\sin(\phi_c)}{\cos(\phi_c - \alpha)} \quad (4.19)$$

As can be seen from Equation 4.18, higher the velocity, bigger the friction power and therefore, higher tool and chip temperature. The high heat generation in the tool leads to softening of the tool and consequently high wear rates and breakage. On the other hand, high values of velocity are desirable for achieving high material removal rate. As a results, the main struggle for the researches is to decrease the friction force F_u in at high cutting velocities by moving the heat toward chips in order to rapid heat dissipation and enhancing the tool's material to preserve hardness at high temperatures.

Tertiary Shear Zone

Due to the nature of cutting process, there is a contact between the flank face of the tool and the machined surface of the workpiece. Dimensions and influence of this contact depends on the tool geometry, preparation of cutting edge and the friction characteristics of the tool and work materials. However, during this contact at tertiary

deformation zone, the flank face of tool is “ploughing” or “rubbing” the machined surface of workpiece without any material deformation. This ploughing causes an additional force (F_{te}, F_{fe}) besides the shearing force (F_{tc}, F_{fc}). Thus the measured force components can be given as a superposition of shearing and edge forces as follows [45];

$$\begin{aligned} F_t &= F_{tc} + F_{te} \\ F_f &= F_{fc} + F_{fe} \end{aligned} \quad (4.20)$$

It is to be noted that F_t, F_f express the measured forces during orthogonal cutting operation. In order to calculate other forces and apply orthogonal cutting mechanics using shearing forces (F_{tc}, F_{fc}), the edge forces (F_{te}, F_{fe}) must be subtracted from the measured forces. In order to calculate the edge forces, orthogonal cutting tests were performed in large variety of rake angles, cutting speeds and feed rates. The fundamental parameters are obtained from tests by measuring the deformed chips thickness and feed and tangential cutting forces. Primarily, cutting forces in tangential F_t and feed F_f direction are measured by a dynamometer. Orthogonal cutting forces are measured at a wide range of feed rates for a specific rake angle and cutting speeds. Then the measurements are repeated for each rake angle and cutting speed values. Measured cutting force results for a specific rake angle and cutting velocity are shown in Figure 4.6. The corresponding edge force values for the specific cutting velocity and rake angle are obtained by extrapolating the measured forces to zero chip thickness. As we evaluate the edge forces and subtracting from the measured forces, we can calculate the apparent friction coefficient and friction angle using the Equation 4.16. By measuring deformed chip thickness, chip compression ratio r_c can be found employing Equation 4.11. As a result, the shear angle ϕ_c and shear stress τ_s can be calculated using the Equations 4.12 and 4.7 respectively.

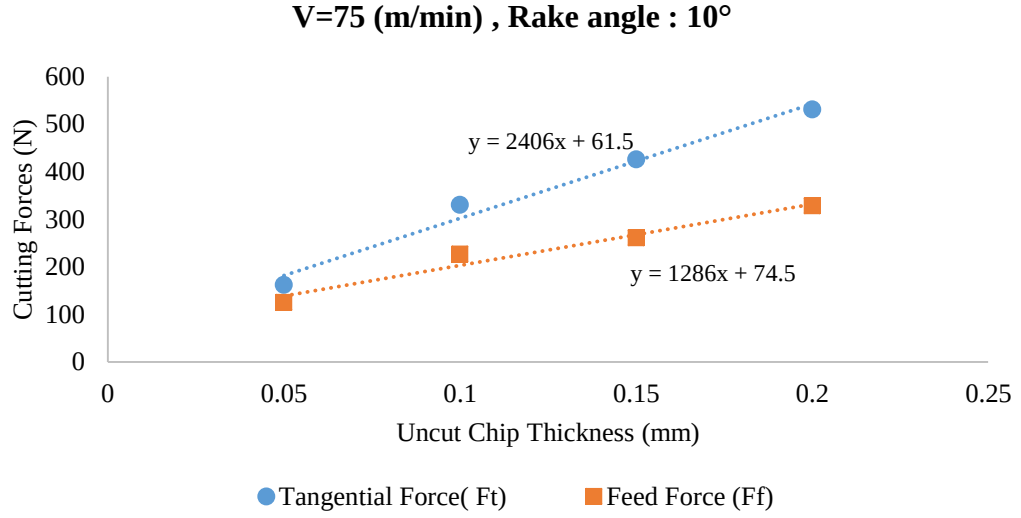


Figure 4.6 : Cutting forces measured during orthogonal cutting of CGI tubes

4.2.1.1 Mechanistic modeling of orthogonal cutting forces

In previous section shear angle, shear stress and friction angle are identified from measured tangential and feed cutting forces. These parameters, which are called as “material-based parameters” in this study, are specific for the tool-workpiece material pairs. As the material of both workpiece and tool or the coating properties of the tool changes, these parameters will not be true anymore.

Shear force can be expressed as a function of shear stress and shear angle using the Equations 4.7 and 4.8 as following;

$$F_s = \tau_s A = \tau_s b \frac{h}{\sin(\phi_c)} \quad (4.21)$$

Using the Equations 4.4 and 4.21, we can express the resultant force as a function of parameters obtained from orthogonal tests and cutting geometry.

$$F_c = \frac{F_s}{\cos(\phi_c + \beta_a - \alpha)} = \tau_s b h \frac{1}{\sin(\phi_c) \cos(\phi_c + \beta_a - \alpha)} \quad (4.22)$$

From the force equilibrium resembled in Figure 4.5, tangential and feed cutting forces can be expressed as:

$$\begin{aligned} F_{t_c} &= F_c \cos(\beta_a - \alpha) \\ F_{f_c} &= F_c \sin(\beta_a - \alpha) \end{aligned} \quad (4.23)$$

Substituting Equation 4.22 into Equation 4.23, we can evaluate the measured principle cutting forces in terms of cutting conditions (h , b) and tool-material dependent parameters ($\phi_c, \beta_a, \alpha, \tau_s$).

$$F_{t_c} = bh[\tau_s \frac{\cos(\beta_a - \alpha)}{\sin(\phi_c) \cos(\phi_c + \beta_a - \alpha)}] \quad (4.24)$$

Similarly, the feed force can be found to be;

$$F_{f_c} = bh[\tau_s \frac{\sin(\beta_a - \alpha)}{\sin(\phi_c) \cos(\phi_c + \beta_a - \alpha)}] \quad (4.25)$$

Excluding the cutting condition parameters, we can obtain the “tangential cutting force coefficient” K_{t_c} as follows;

$$K_{t_c} = \tau_s \frac{\cos(\beta_a - \alpha)}{\sin(\phi_c) \cos(\phi_c + \beta_a - \alpha)} \left[\frac{N}{mm^2} \right] \quad (4.26)$$

And the “feed cutting force coefficient” as;

$$K_{f_c} = \tau_s \frac{\sin(\beta_a - \alpha)}{\sin(\phi_c) \cos(\phi_c + \beta_a - \alpha)} \left[\frac{N}{mm^2} \right] \quad (4.27)$$

Since the uncut chip thickness has no contribution on the generation of edge forces, the edge force coefficients are obtained by the following relations;

$$\begin{aligned} K_{t_e} &= \frac{F_{t_e}}{b} \left[\frac{N}{mm} \right] \\ K_{f_e} &= \frac{F_{f_e}}{b} \left[\frac{N}{mm} \right] \end{aligned} \quad (4.28)$$

Note that the edge coefficients change as the edge cutting tool wears or chipping occurs at the cutting edge. As a result, we can define the cutting forces mechanistically in terms of cutting constants (K_{t_c} and K_{f_c}) and cutting conditions (b and h) as follows;

$$\begin{aligned} F_t &= K_{t_c}bh + K_{t_e}b \\ F_f &= K_{f_c}bh + K_{f_e}b \end{aligned} \quad (4.29)$$

The cutting force coefficients (K_{tc} and K_{fc}) which directly contribute in shearing of the material and edge force coefficients (K_{te} and K_{fe}) which contribute only in ploughing of material in tertiary deformation zone, are directly calibrated from metal cutting experiments for a tool-workpiece pair [45].

4.2.2 Mechanics of oblique cutting

Oblique cutting mechanics is active in most of cutting operations such as milling, turning drilling and broaching. The cutting mechanics has some differences in compared to orthogonal cutting due to the inclination of tool's cutting edge relative to the direction of cutting velocity. In other words, the cutting velocity is perpendicular to the cutting edge in orthogonal cutting, whereas, in oblique cutting mechanics, it is inclined at an acute angle i to the cutting of tool. Figure 4.7 resembles the geometry of oblique cutting mechanics in detail.

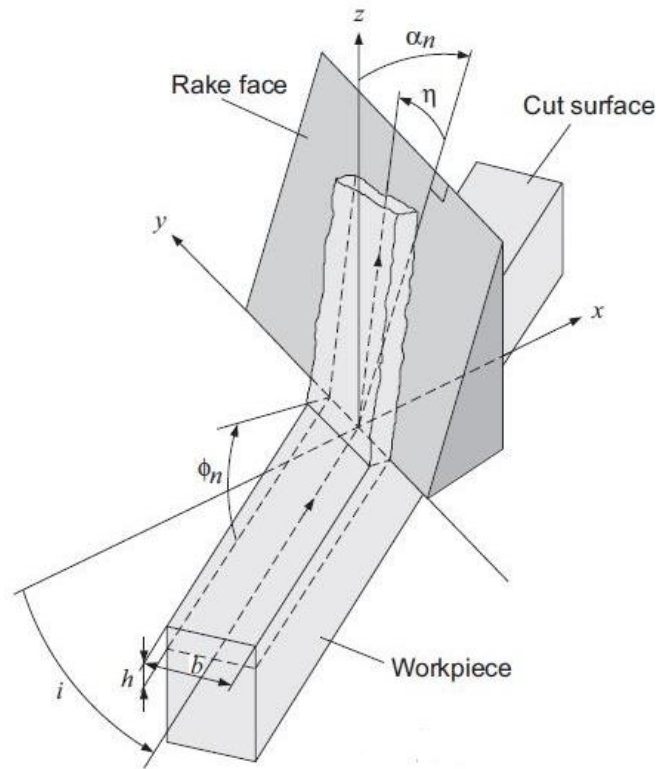


Figure 4.7 : The geometry of oblique cutting mechanics.

In order to explain the cutting force geometry in oblique cutting, a plane normal to cutting edge, and inclined at an acute angle i with the cutting velocity V is defined as the normal plane P_n . By projecting all the forces and vectors on the normal plane P_n and deriving geometrical relation between forces calculating process become much

easier. In this case, as in most of the analyses, it is assumed that the mechanics of oblique cutting in the normal plane are equivalent to that of orthogonal cutting. In oblique cutting, the angle between the shear and XY planes called normal shear angle ϕ_n . The shear velocity lies on the shear plane but makes an oblique shear angle ϕ_i with the vector normal to the cutting edge on the normal plane. The detailed view of the planes and force vectors are covered in Altintas [45]. Another additional angle that appeared in oblique cutting is chip flow angle η . Chip flow angle lies between the sliding direction of the chip on rake face and the vector on the rake face but normal to the cutting edge, which also lies on the normal plane. Moreover, normal rake angle α_n can be measured between the above-mentioned normal vector and z -axis. Elemental view of oblique cutting is represented in Figure 4.8.

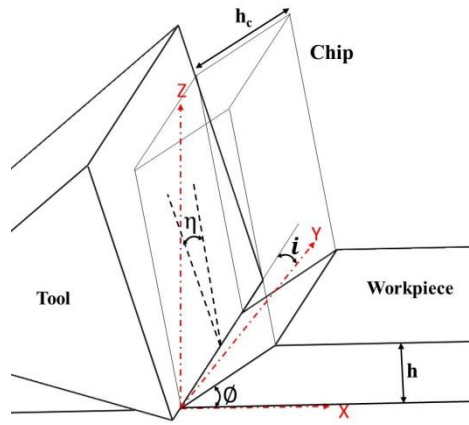


Figure 4.8 : Schematic representation of oblique cutting.

Armarego and Whitfield [79] made two assumptions in their model on the shear direction and chip length ratio: 1) the shear velocity is collinear with shear force 2) the chip length ratio in oblique cutting is the same as that in orthogonal cutting. Employing their assumptions, the normal shear angle ϕ_n can be obtained from the following equation of chip geometry:

$$\tan \phi_n = \frac{r_c (\cos \eta / \cos i) \cos \alpha_n}{1 - r_c (\cos \eta / \cos i) \sin \alpha_n} \quad (4.30)$$

However, in order to avoid the iteration and long calculation procedures, Stabler's empirical chip flow rule ($i = \eta$) is applied to Equation 4.30. By applying Stabler's rule we can consider the normal shear angles in oblique cutting equal to that of in orthogonal cutting mechanics [80].

4.2.2.1 Modeling of oblique cutting forces

The cutting force components are derived by projecting of the resultant force F_c after subtracting the edge force component from the measured forces. The Armarego's classical oblique model is used and the force expressions in the directions of cutting speed called “tangential force” (F_{t_c}), uncut chip thickness called “feed force” (F_{f_c}), and in normal direction called “radial force” (F_{r_c}) are derived as following by employing the geometrical relations mentioned above:

$$F_{t_c} = b \cdot h \cdot \frac{\tau_s}{\sin(\phi_n)} \frac{\cos(\beta_n - \alpha_n) + \tan(i) \tan(\eta) \sin(\beta_n)}{\sqrt{(\cos^2(\phi_n + \beta_n - \alpha_n) + \tan^2(\eta) \sin^2(\beta_n))}} \quad (4.31)$$

$$F_{f_c} = b \cdot h \cdot \frac{\tau_s}{\sin(\phi_n) \cos(i)} \frac{\sin(\beta_n - \alpha_n)}{\sqrt{(\cos^2(\phi_n + \beta_n - \alpha_n) + \tan^2(\eta) \sin^2(\beta_n))}} \quad (4.32)$$

$$F_{r_c} = b \cdot h \cdot \frac{\tau_s}{\sin(\phi_n)} \frac{\cos(\beta_n - \alpha_n) \tan(i) - \tan(\eta) \sin(\beta_n)}{\sqrt{(\cos^2(\phi_n + \beta_n - \alpha_n) + \tan^2(\eta) \sin^2(\beta_n))}} \quad (4.33)$$

Similar to orthogonal cutting mechanics, if the cutting condition parameters (b, h) excluded from the force expressions, oblique cutting force coefficients will be achieved as follows;

$$K_{t_c} = \frac{\tau_s}{\sin(\phi_n)} \frac{\cos(\beta_n - \alpha_n) + \tan(i) \tan(\eta) \sin(\beta_n)}{\sqrt{(\cos^2(\phi_n + \beta_n - \alpha_n) + \tan^2(\eta) \sin^2(\beta_n))}} \quad (4.34)$$

$$K_{f_c} = \frac{\tau_s}{\sin(\phi_n) \cos(i)} \frac{\sin(\beta_n - \alpha_n)}{\sqrt{(\cos^2(\phi_n + \beta_n - \alpha_n) + \tan^2(\eta) \sin^2(\beta_n))}} \quad (4.35)$$

$$K_{r_c} = \frac{\tau_s}{\sin(\phi_n)} \frac{\cos(\beta_n - \alpha_n) \tan(i) - \tan(\eta) \sin(\beta_n)}{\sqrt{(\cos^2(\phi_n + \beta_n - \alpha_n) + \tan^2(\eta) \sin^2(\beta_n))}} \quad (4.36)$$

Therefore, it is convenient to express the total cutting forces in terms of cutting force coefficients ($K_{t_c}, K_{f_c}, K_{r_c}$), edge force coefficients ($K_{t_e}, K_{f_e}, K_{r_e}$), and cutting conditions (b, h) as follows;

$$\begin{aligned}
F_t &= K_{tc}bh + K_{te}b \\
F_f &= K_{fc}bh + K_{fe} \\
F_r &= K_{rc}bh + K_{re}
\end{aligned}
\tag{4.37}$$

By using assumptions described below, this approach can be used in predicting the oblique cutting forces from the orthogonal cutting database [81]

- The shear angle ϕ_n , average friction angle $\beta_n = \beta_a$, and shear stress τ_s are evaluated from the orthogonal cutting tests. These parameters are the same in both orthogonal and oblique cutting operations for a given speed chip load, and tool-workpiece material pair.
- Assume that the orthogonal shear angle is equal to normal shear angle in oblique cutting ($\phi_c \equiv \phi_n$); the normal rake angle in oblique cutting is equal to rake angle in orthogonal cutting ($\alpha_n \equiv \alpha$); and the chip flow angle is equal to the inclination angle in oblique cutting ($\eta \equiv i$) by adopting Stabler's chip flow rule.
- Predict the cutting forces using the oblique cutting coefficients given in Equations 4.34, 4.35, 4.36.

4.2.3 Mechanics of drilling

As mentioned in previous sections, point part of twist drill, which is responsible for cutting the material and producing of holes, has complex geometry. As shown in Figure 4.9, point part of drill consists of two cutting regions along the chisel and cutting lip. At the center of drill, the chisel edge acts as an indenter that pushes and ploughs the material sideways. At the cutting part of the chisel edge, orthogonal cutting mechanics is active with high rake angles, which produce undesirable chips. From the chisel edge toward the periphery of the drill, rake angle along the cutting lips vary from high negative values to values that are more positive. Consequently, the chip formation will change from extrusion, with discontinuous chip types, to shearing the material with continuous chips. At the cutting lips, oblique cutting mechanics is active, where the inclination angle, rake angle and cutting speed vary along the cutting lips.

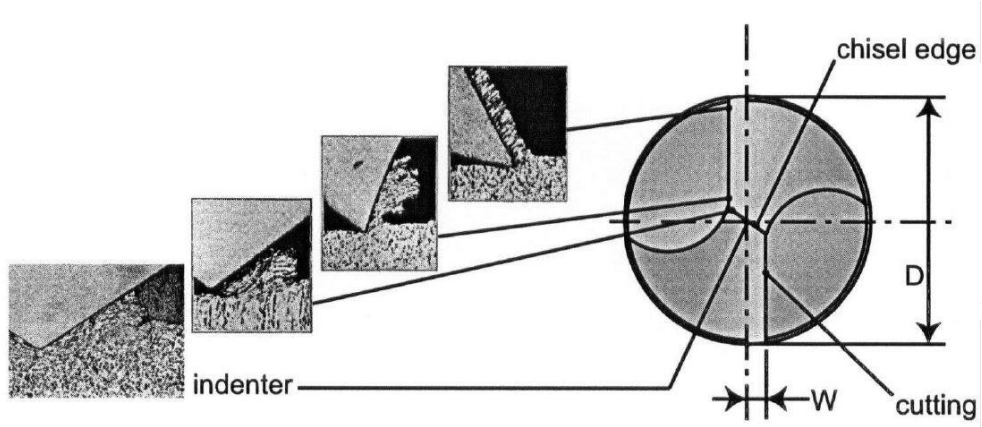


Figure 4.9 : Different chip formations of different cutting regions for a twist drill; from ploughing at the chisel edge to oblique cutting at the periphery [73].

Therefore, mechanics of drilling must be analyzed separately for the chisel edge and cutting lip region by using above-mentioned cutting mechanics fundamentals.

4.2.3.1 Chisel edge

Chisel edge consist of two regions; 1) indentation zone 2) orthogonal cutting region at chisel edge. Two different force modeling strategies used for both regions are described as following;

Indentation zone at chisel edge

In this zone, the drill center acts as a rigid wedge indenting a plastic material extruding on both sides of wedge [42]. The size of indentation area r_{ind} can be calculated using he geometrical analysis proposed by Mauch [82] as;

$$r_{ind} = \frac{f}{2 \tan(\pi/2 - \kappa_t)} \quad (4.38)$$

where f is feed per revolution. Figure 4.10 illustrates the indentation process. Axial force resulted by the indentation zone described using slip line field solution [42]. The normal rake angle at the chisel edge can be computed from the drill point angle and chisel edge angle as below;

$$\tan \alpha_n = \tan \kappa_t \sin(\pi - \psi_c) \quad (4.39)$$

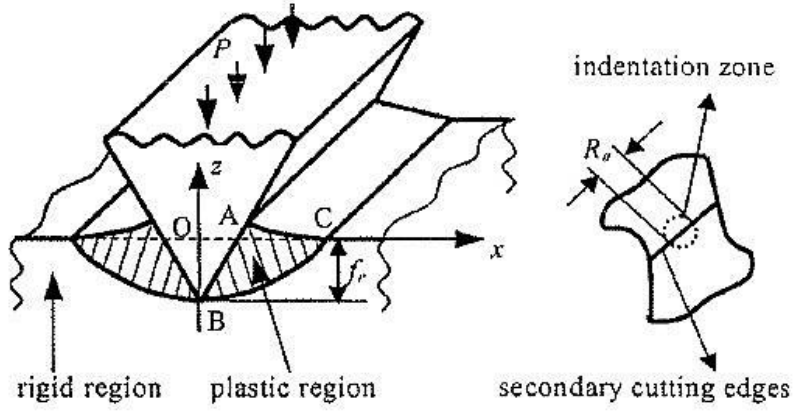


Figure 4.10 : Indentation zone at the chisel edge [83].

The contribution of the indentation zone to the total drilling thrust is found to be:

$$F_{z,ind} = \frac{4f\tau_s(1 + \varepsilon_s)r_{ind} \sin \alpha_n}{\cos \alpha_n - \sin(\alpha_n - \varepsilon_s)} \quad (4.40)$$

where the τ_s is the shear yield stress of the workpiece material, which is assumed to be constant along the shear plane. The solution for the slip line ε_s is given by;

$$2\alpha_n = \varepsilon + \cos^{-1} \left(\tan \left(\frac{\pi}{4} - \frac{\varepsilon_s}{2} \right) \right) \quad (4.41)$$

Orthogonal cutting zone at chisel edge

Split-point twist drills employed in this study have orthogonal cutting edges between the indentation zone and cutting lips along the chisel edge. Resulted cutting forces at the axial and cutting velocity direction are identified using orthogonal cutting mechanics as follows;

$$\begin{aligned} F_{z,ort} &= F_{f2} = K_{f_c}bh + K_{f_e}b \\ F_{T,ort} &= F_{t2} = K_{t_c}bh + K_{t_e}b \end{aligned} \quad (4.42)$$

where the cutting force coefficients (K_{f_c}, K_{t_c}) and edge force coefficients (K_{f_e}, K_{t_e}) are obtained from orthogonal cutting database. Moreover, depth of cut b is obtained from tool geometry. As a result, total corresponding thrust force at chisel edge $F_{th,chisel}$ and total torque T_{chisel} are evaluated by sum of thrust force and torque in each region. Note that, Due to the lower radial distance at the indentation zone the

resulted torque is neglected. Therefore, while calculating total torque at chisel edge, only orthogonal cutting region is taken into account.

$$F_{th,chisel} = F_{z,ind} + F_{z,ort} \quad (4.43)$$

$$T_{chisel} = 2 \sum_{m=1}^M dF_{T,ort} \cdot r \quad (4.44)$$

4.2.3.2 Cutting lips

Cutting lips have a quite complex geometry. Existence of helical flutes, offset caused by chisel edge and the varying diameter produces wide range of rake, inclination and helix angles along the cutting lips. Hence, in order to model the cutting forces along the cutting lips, oblique cutting mechanics are employed. For this purpose, normal rake, helix and inclinations angle identified along cutting lips and orthogonal to oblique transformation is applied. The cutting lips were divided into several segments, considering each as an oblique cutting tool with specific normal rake, inclination and helix angle. Each segment has specific cutting speed, rake and inclination angles and radial distance of $r(z)$. Cutting forces of each oblique cutting segment were derived using orthogonal cutting database and orthogonal to oblique transformation. Using the cutting force coefficients elemental cutting forces at tangential, feed and radial direction are obtained. Figure 4.11 illustrates the directions of the forces for an oblique cutting element on the lip of drill.

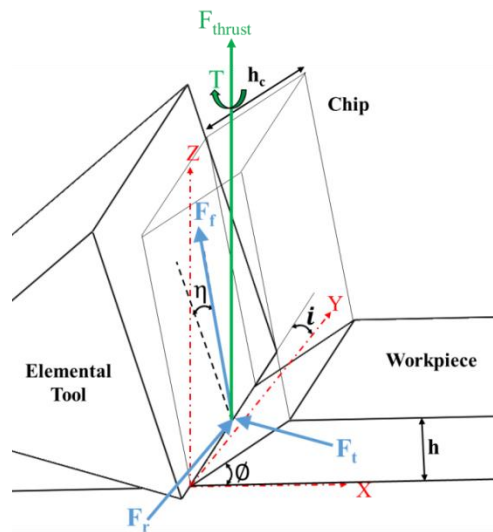


Figure 4.11 : Schematic illustration of oblique cutting element on the lip.

$$\begin{aligned} x(z) &= r(z) \cos(\theta(z)) \\ y(z) &= r(z) \sin(\theta(z)) \end{aligned} \tag{4.45}$$

$$y(z) = r(z) \sin(\theta(z)) \quad (4.45)$$

$$r(z) = \sqrt{\omega^2 + [\omega^2 \cot(\pi - \psi_c) + z \tan k_t]^2} \quad (4.46)$$

$$r(z) = \sqrt{\omega^2 + [\omega^2 \cot(\pi - \psi_c) + z \tan \kappa_t]^2} \quad (4.46)$$

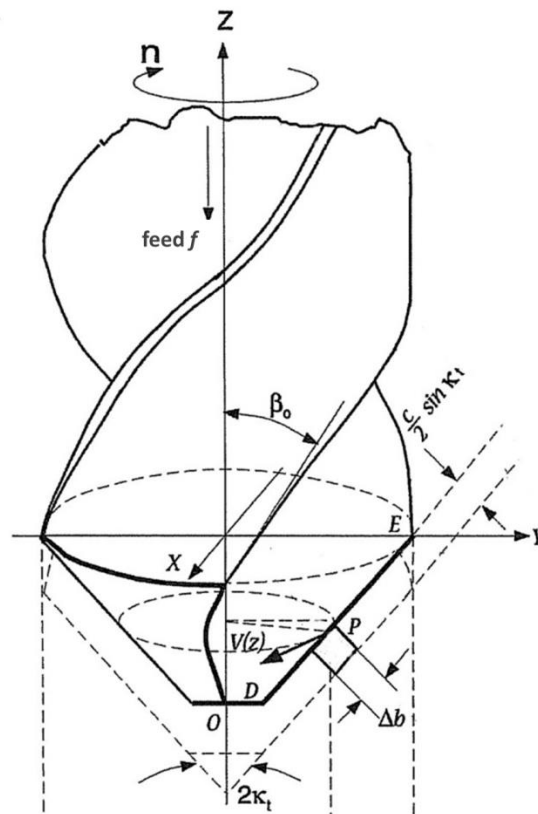


Figure 4.12 : Geometric model of drilling process.

$$\tan(\beta(z)) = \frac{2\pi r(z)}{L_{pt}} \quad (4.47)$$

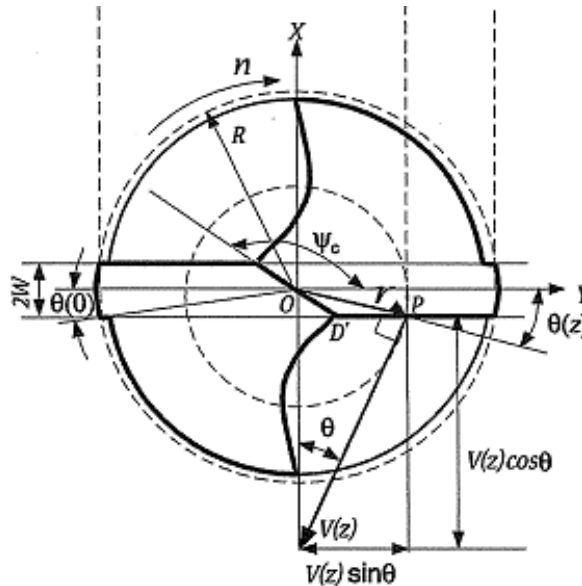


Figure 4.13 : Geometric model of drill from top view.

where L_{pt} is the constant pitch length of the helix. This can be calculated from the drill geometry. Although the helix angle changes along the lip, the nominal helix angle β_0 of the drill is defined for the flutes at the cylindrical region ($r(a)=R$) as follows:

$$\tan(\beta_0) = \frac{2\pi R}{L_{pt}} \quad (4.48)$$

The cutting velocity is perpendicular to the radius $r(z)$ at each point along the cutting lips. Figure 4.13 shows the components of cutting velocity projected on the XY plane as a function of elevation z and can be found as follows;

$$\begin{aligned} V_x(z) &= V.\cos\theta(z) \\ V_y(z) &= V.\sin\theta(z)\cos\kappa_t \end{aligned} \quad (4.49)$$

The projection of the cutting velocity V on the cutting lip, which has the point angle of κ_t , is;

$$V_t = V_y \sin \kappa_t = V \sin \theta(z) \sin \kappa_t \quad (4.50)$$

Similar to the oblique cutting mechanics, inclination (oblique) angle of each point can be defined as the angle between the cutting velocity and normal to the cutting edge which, varies with the elevation at each point as follows;

$$\sin i = \frac{V_t}{V} = \sin \theta(z) \sin \kappa_t \quad (4.51)$$

Since the each element acts as an oblique cutting edge, the cutting mechanics in drilling process are defined in the normal plane P_n , which is perpendicular to the cutting edge. The normal rake angle α_n is the angle between the tangent to the flute in the normal plane at each point and the normal to the projection of the resultant cutting speed, which also lies in plane P_n . Since cutting velocity has two components, the angle between these two component so-called “reference angle” can be found;

$$\tan \gamma_d = \frac{V_y}{V_x} = \tan \theta(z) \cos \kappa_t \quad (4.52)$$

By projecting points P inside cutting lip and along the cutting velocity on the normal plane, effective rake angle is found;

$$\tan \alpha_f = \frac{\tan \beta_z \cos \theta(z)}{\sin \kappa_t - \tan \beta_z \sin \theta(z) \cos \kappa_t} \quad (4.53)$$

The normal rake angle we are interested in can be found from geometrical relations as follow;

$$\alpha_n = \alpha_f - \gamma_d \quad (4.54)$$

Since the drill lip divided into m number of elements with height dz and width Δb , each elements will remove a chip with the area of:

$$dA(z) = \Delta b \cdot h \quad (4.55)$$

where the h is chip thickness removed by one of the flutes of the drill. Thickness and width of the cut chips can be calculated as follows respectively;

$$h = \frac{c}{2} \sin \kappa_t \quad (4.56)$$

$$\Delta b = \frac{dz}{\cos \kappa_t} \quad (4.57)$$

The cutting forces in tangential (parallel to cutting velocity) $dF_t(z)$, chip flow $dF_f(z)$ and radial $dF_r(z)$ directions for each element can be found as:

$$\begin{aligned} dF_t(z) &= K_{tc}(z) dA + K_{te} \Delta b \\ dF_f(z) &= K_{fc}(z) dA + K_{fe} \Delta b \\ dF_r(z) &= K_{rc}(z) dA + K_{re} \Delta b \end{aligned} \quad (4.58)$$

where the cutting force coefficients (K_{tc} , K_{fc} , K_{rc}) and edge force coefficients (K_{te} , K_{fe} , K_{re}) are derived from orthogonal to oblique transformations using equations 2.34, 2.35, 2.36. Note that the cutting coefficients for each element at different elevation z are different because of varying helix, normal rake, and inclination angles. The method of calculation of each orthogonal parameter for each element is discussed in section 4.3. The elemental forces are transformed into Cartesian coordinate systems in x, y, z directions using the following equations.

$$\begin{aligned} dF_x(z) &= [dF_f \sin \gamma_d - dF_t \cos \theta - dF_r \sin i] \\ dF_y(z) &= [dF_r (\sin i \cdot \sin \gamma_d \cdot \cos \kappa_t) - dF_f \cos \gamma_d \cdot \cos \kappa_t - dF_t \sin \theta] \\ dF_z(z) &= [dF_f \cos \gamma_d \cdot \sin \kappa_t - dF_r (\cos i \cdot \cos \kappa_t + \sin i \cdot \sin \gamma_d \cdot \sin \kappa_t)] \end{aligned} \quad (4.59)$$

The total thrust force and torque values for the lips can be obtained by summing all the elemental thrust forces and torques at each direction. The total torque and thrust force produced by the two lips of a twist drill can be obtained as:

$$F_{th, lip} = 2 \sum_{m=1}^M dF_z(z) \quad (4.60)$$

$$T_{lip} = 2 \sum_{m=1}^M dF_t(z) \cdot r(z) \quad (4.61)$$

The total thrust force and torque resulted by summing the corresponding thrust force and torque values produced by chisel edge and cutting lips as shown in Figure 4.14. Total drilling forces can be evaluated as follows:

$$F_{THRUST} = F_{th,chisel} + F_{th,lip} \quad (4.62)$$

$$T_{TORQUE} = T_{chisel} + T_{lip} \quad (4.63)$$

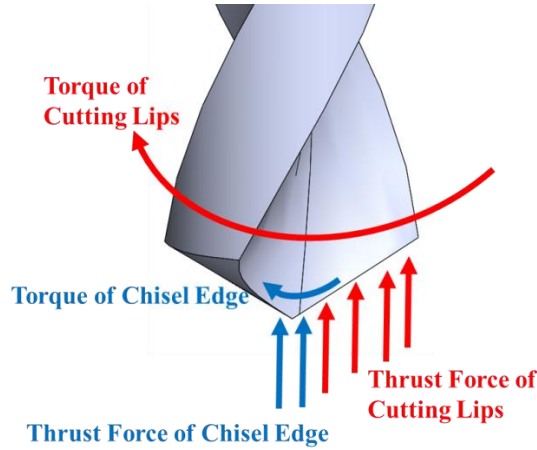


Figure 4.14 : Representation of total thrust force and torque.

4.3 Cutting Force Modeling Using Experimental Approach

This chapter describes the evaluation of drilling cutting forces using the theories mentioned in previous sections and experimental data obtained from orthogonal cutting tube tests. The orthogonal to oblique transformation relations need parameters that they will be achieved from experimental tests. For this purpose, CGI tubes were produced with outer diameter of $D_1=60mm$ and thickness of $b=2mm$ as illustrated in Figure 4.15.

Table 4.1 : Cutting parameters for orthogonal tube cutting test

Parameters	Unit	Values
Rake Angle	Degree	-30; -20; -10; 0; 10; 20; 25; 30
Cutting Velocity	m/min	125; 100; 75; 50; 12,5
Feed	mm	0.05; 0.1; 0.15; 0.2

The orthogonal tests are performed utilizing YCM-GT200A CNC turning lathe. Cutting forces were measured by 3-axis Kistler 9257BA dynamometer as shown in Figure 4.15. Principle and edge forces in tangential and feed direction derived from the measured cutting force coefficients using Equations 4.20 and 4.29. Note that edge force coefficients will be derived by extrapolating the measured forces to zero chip thickness as shown in Figure 4.6. After measuring deformed chip thickness in order to calculate the chip compression ratio, shear stress, shear angle, and friction angle parameters are calculated for each experiment employing the Equations 4.7, 4.12, and 4.16 respectively. The cutting speed and rake angles vary along the cutting lips and the orthogonal cutting parameters are different for each elemental tool. Hence, each shear stress, shear angle, and friction angle were interpolated as a function of rake angle and cutting velocity in order to obtain the each orthogonal parameters at the rake angle and cutting velocity values that are not tested during orthogonal cutting experiments. For 0.05 mm uncut chip thickness, linear regression plots of shear stress, shear angle and friction angle are illustrated within Figure 4.17. Using regression methods, shear stress, shear angle and friction angle equations derived as represented in Equation 4.64-66 where, α_n and V_c represent the rake angle and cutting speed respectively. For instance, regression equations for 0.05 mm-uncut chip thickness are represented as following:

$$\tau_{sh=0.05} = 339.8 + 0.01752\alpha_n + 0.147V_c \quad (4.64)$$

$$\phi_{nh=0.05} = 17 + 0.2097\alpha_n + 0.02658V_c \quad (4.65)$$

$$\beta_{nh=0.05} = 27.21 + 0.5513\alpha_n - 0.006033V_c \quad (4.66)$$

The regression diagrams and equations for further uncut chips thickness values are given in Appendix A.

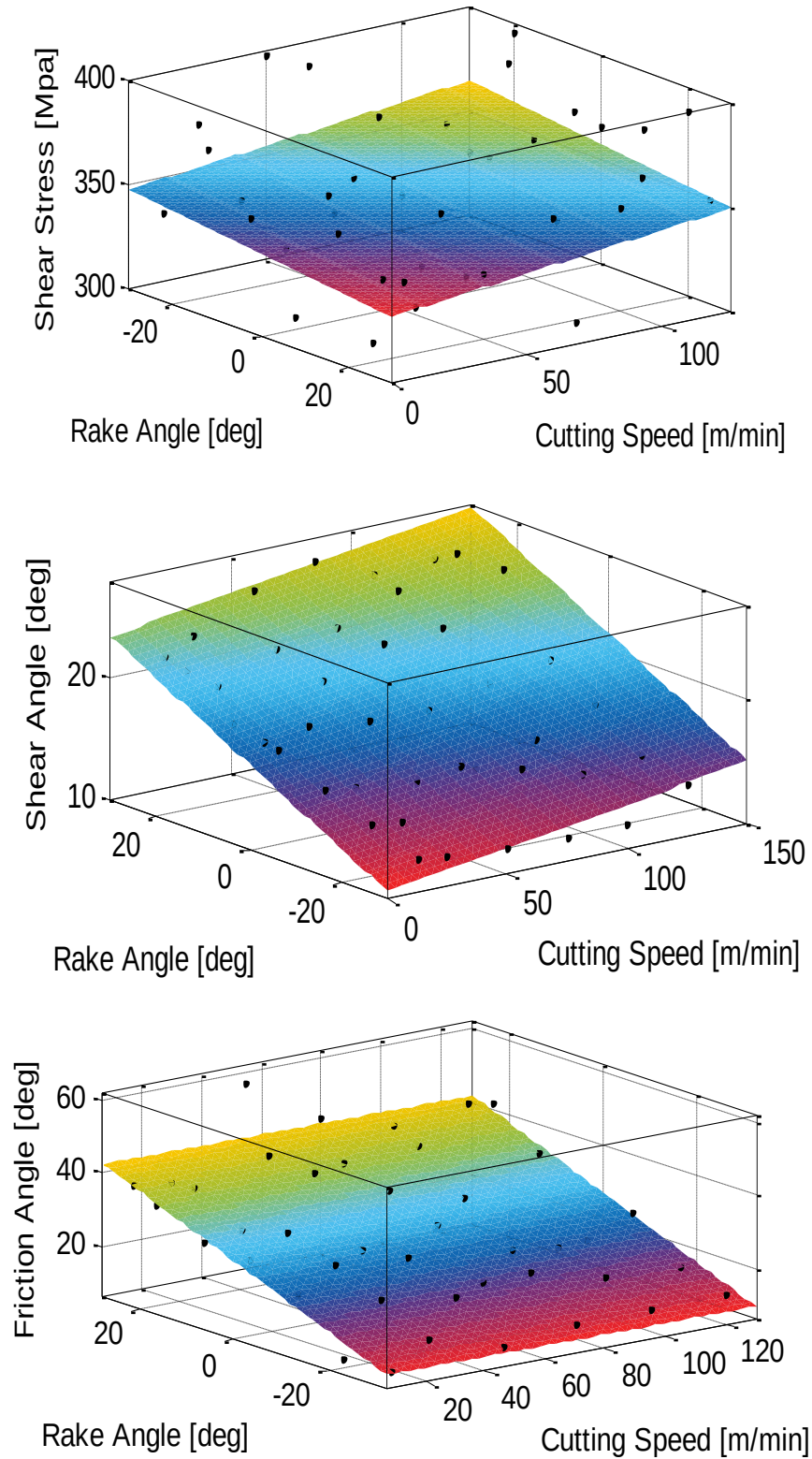


Figure 4.17 : Linear regression of shear stress, shear angle and friction angle equation plane depending on cutting speed and rake angle for 0.05 mm uncut chip thickness respectively.

Obtaining the regression equations, shear angle, shear stress and friction angle values can be calculated for each elemental tool on the cutting lips for a specific feed rate

value. Using the calculated parameters and drilling tool geometrical parameters, drilling cutting force along the cutting lips and chisel edge can be determined using the theories presented in previous chapters. The geometrical properties of the twist drill bit used in experiments presented in the Table 4.2.

Table 4.2 : Geometrical properties of drill bit.

Geometrical property	Symbols	Value
Helix angle	β	30°
Cone angle	κ_t	118°
Pitch length	L_p	50 mm
Drill diameter	D	10 mm
Chisel angle	ψ_c	120°
Chisel length	2ω	2.55 mm
Core diameter	CD	3 mm

Figure 4.18 illustrates the variation of the oblique (inclination) angle (using Equation 4.51), normal rake angle (using Equation 4.54), effective rake angle (using Equation 4.53) and helix angle (Using Equation 4.47) in terms of relative distance from the drill center.

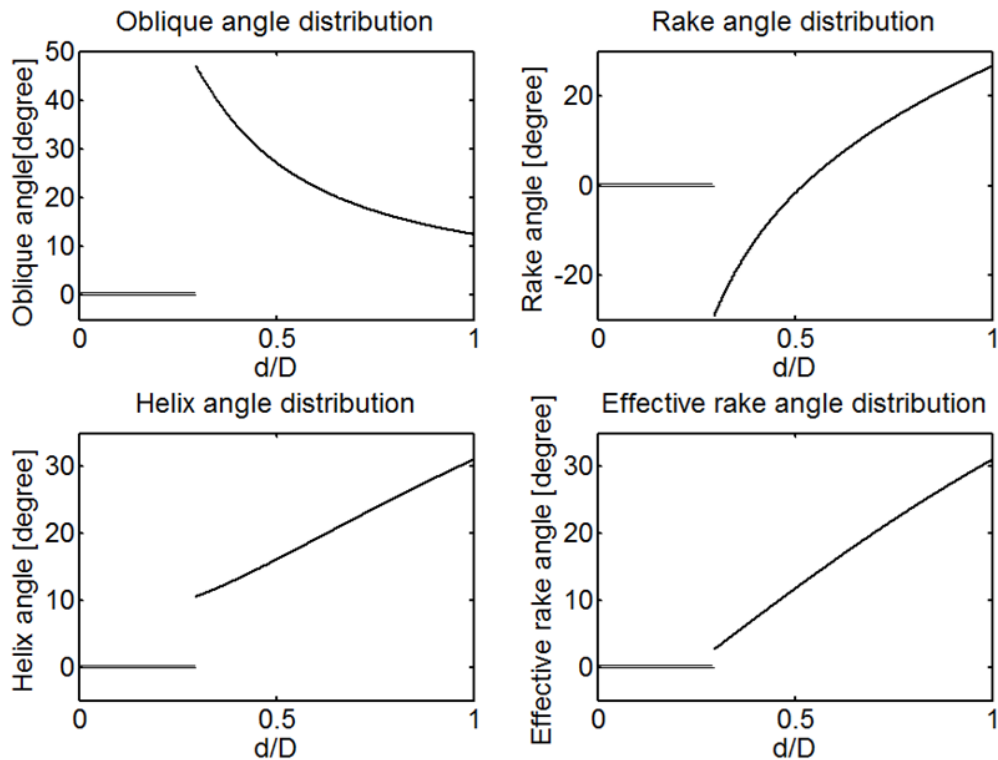


Figure 4.18 : Variation of the inclination, rake, effective rake and helix angle along the cutting lip.

As can be seen in Figure 4.18, inclination angle decreases from the chisel edge toward the periphery of the tool, while the helix, effective rake and normal rake angles have increasing trend along the cutting lips of the drill. As a result, material shearing at the vicinity of periphery is easier due to the high negative rake angles besides continuous chip formation. Figure 4.19 shows the distribution of the shear stress, shear angle and friction angle along the radius of the drill bit for a specific cutting condition. Each one of shear stress, shear angle and friction coefficient evaluated for each elemental cutting edge which has different rake angle and cutting speed. In the case that the elemental tool incorporates uncut chip thickness between the selected values, linear interpolations have been applied to calculate shear stress, shear angle and friction angle. These parameters are identified as increasing from chisel-lip interface to outer surface of drill, which is influenced by the development of the rake angle and cutting velocity along cutting lips of drill.

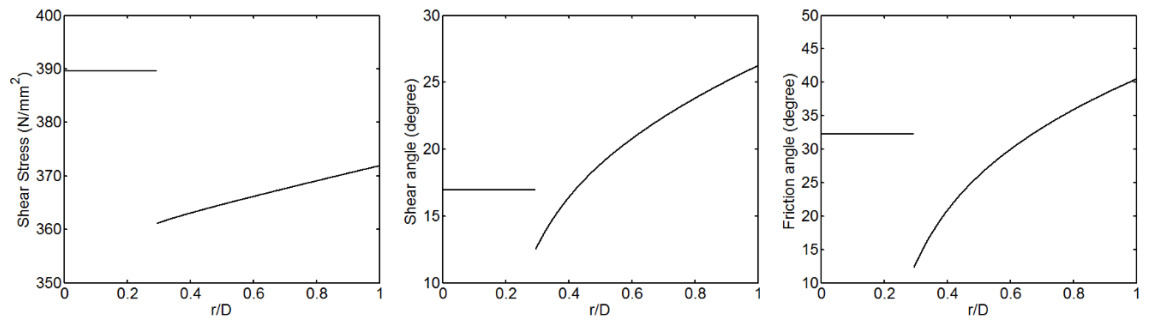


Figure 4.19 : Distributions of Shear Stress, Shear Angle and Friction Angle along radial distance $V=100$ m/min, $f= 0.15$ mm/rev

Similarly, by assuming the each element as an oblique cutting edge, by using the orthogonal cutting parameters, cutting force coefficients in radial, tangential and feed directions can be derived in terms of radial distance as illustrated Figure 4.20.

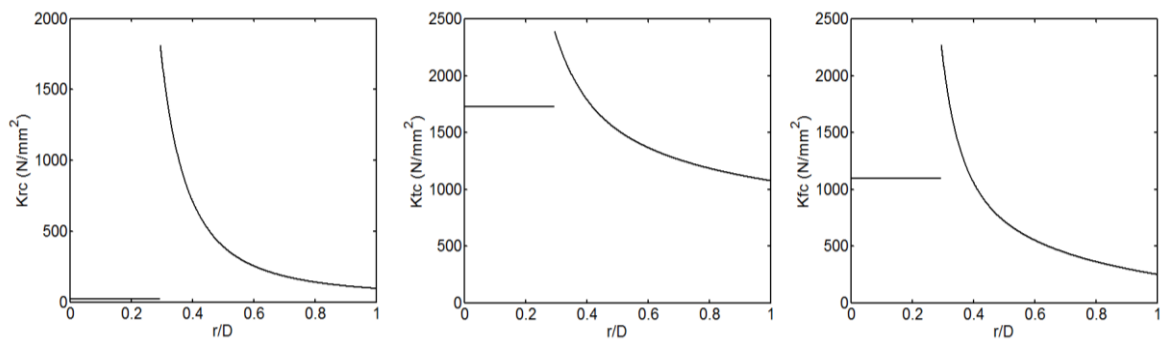


Figure 4.20 : Distributions of cutting and edge coefficients along cutting lip at $V_c=100$ m/min, $f= 0.15$.

As a result the cutting forces for chisel edge and cutting lips are modeled using the orthogonal cutting data, tool geometry properties and presented theories. Later, the elemental forces transformed into the coordinate of the tool and summed. The results are indicated in the following chapter.

4.4 Experimental Verification of Drilling Forces

After the drilling cutting forces modeled, drilling experiments are performed in order to confirm and compare the resulted forces. The study utilized a 10 mm diameter uncoated solid carbide drill, with the geometric properties illustrated in Table 4.2. The workpiece material is compacted graphite iron (CGI) with 30 mm in diameter and 50 mm in length. The grade of CGI is GJV 400 with 7150 kg/m³ density, 39 W/m-K thermal conductivity, and 475 J/kg-K specific heat. The experimental setup is illustrated in Figure 4.21.

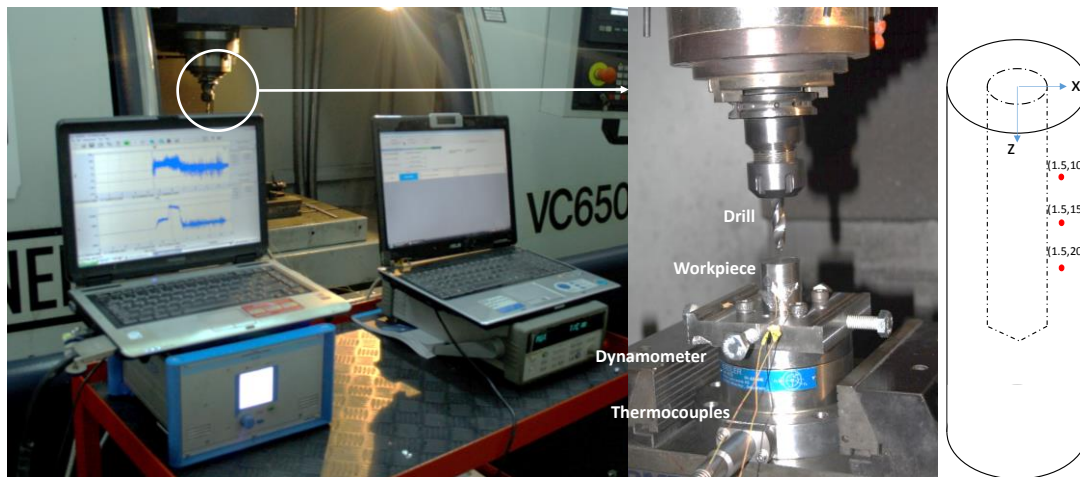


Figure 4.21 : a) Experimental Setup b) close up picture of experiment system c) (x,z) location of thermocouples.

During the temperature measurements, K type thermocouple with 0.127 mm in diameter wire are integrated into workpiece at 10, 15 and 20 mm below the top surface. The thermocouple hole was 1 mm in diameter and 1.5 mm near to drilled surface. The temperatures at the point which thermocouple positioned were recorded at a 2 Hz sampling rate. These thermocouples are utilized for controlling the temperature of cutting during the experiments. Drilling experiments performed in Spinner-VC 650 CNC machining center. Kistler Type 9272 dynamometer was integrated into the system to acquiring of cutting force and torque data, which is considered vital in the validation of cutting force model. Some experiments are done

at specific cutting conditions and results compared with that of obtained from the prediction. In order to measure the cutting forces at chisel edge and cutting edges separately, a pilot hole with the diameter of the chisel edge (core diameter) of the tool, drilled as illustrated in Figure 4.22. At Step A, cutting lips are engaging with the material and increasing of cutting forces can be seen in Figure 4.23. At step B the cutting lips are engaged completely, while chisel edges does not cut due to the existence of pilot hole. Therefore, the experimental cutting forces can be obtained separately for cutting lips, as shown in Figure 4.23. At Step C both of the cutting lips and chisel edge are engaged with workpiece and hence, the resulted forces is the summation of both cutting edges.

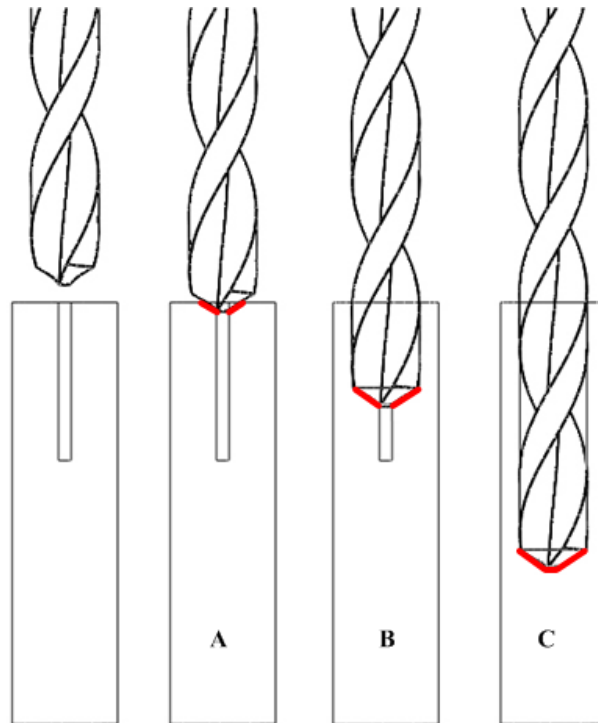


Figure 4.22 : Representation of drilling steps with pilot hole.

Experimental and calculated cutting forces are given in Table 4.3 in time domain. The experimental and predicted cutting forces are presented for each of cutting edges individually in two different cutting conditions.

Table 4.3 : Measured and predicted cutting forces for a specific cutting condition.

Vc [m/min]	f [mm/rev]	Measured force			Predicted Force		
		Cutting lips	Chisel edge	Total	Cutting lips	Chisel edge	Total
100	0,116	347	340	687	480	375	855
75	0,116	359	347	706	435	398	833
50	0,116	252	338	590	391	431	822
100	0,233	664	761	1425	650	740	1390
75	0,233	708	728	1436	584	749	1333
50	0,233	629	771	1400	518	767	1285

Figure 4.23 represents the comparisons of predicted and measured forces for torque and thrust force. The graph incorporates three main regions identified as A, B and C. At the region A, the cutting lips are gradually integrated within the material, which influence an increase in the identified forces. The forces raise more up to completely cutting lip contact with material, which influences the characterization of region B. Region B identifies a thrust force, which is influenced by the cutting lip, but does not incorporate any chisel edge. At region C, the entire tool body penetrates the cutting action, which identifies the forces as the total of chisel and cutting lip. Predicted thrust force at the cutting lip is in a good agreement with experimental results on the other hand, predicted thrust force is about 5-10% higher than measured value.

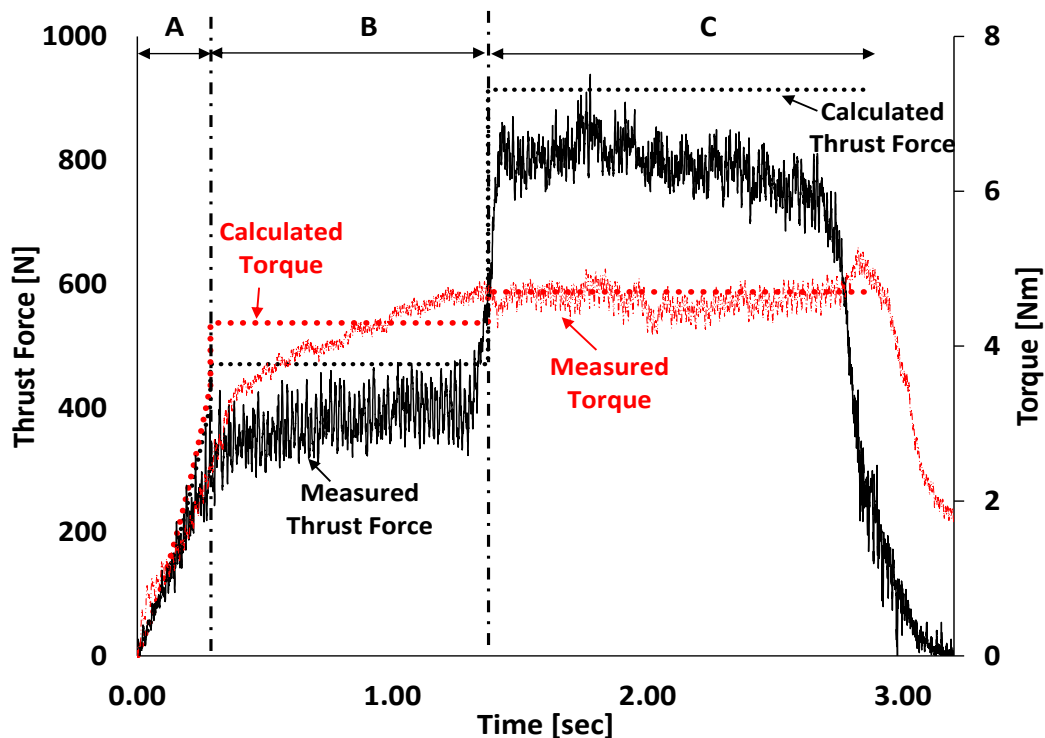


Figure 4.23 : A representation of the forces generated by cutting lip and whole drill.

Figure 4.24 represents the temperature measurement results for the drilling operation. These results will be discussed further in the residual stress chapter.

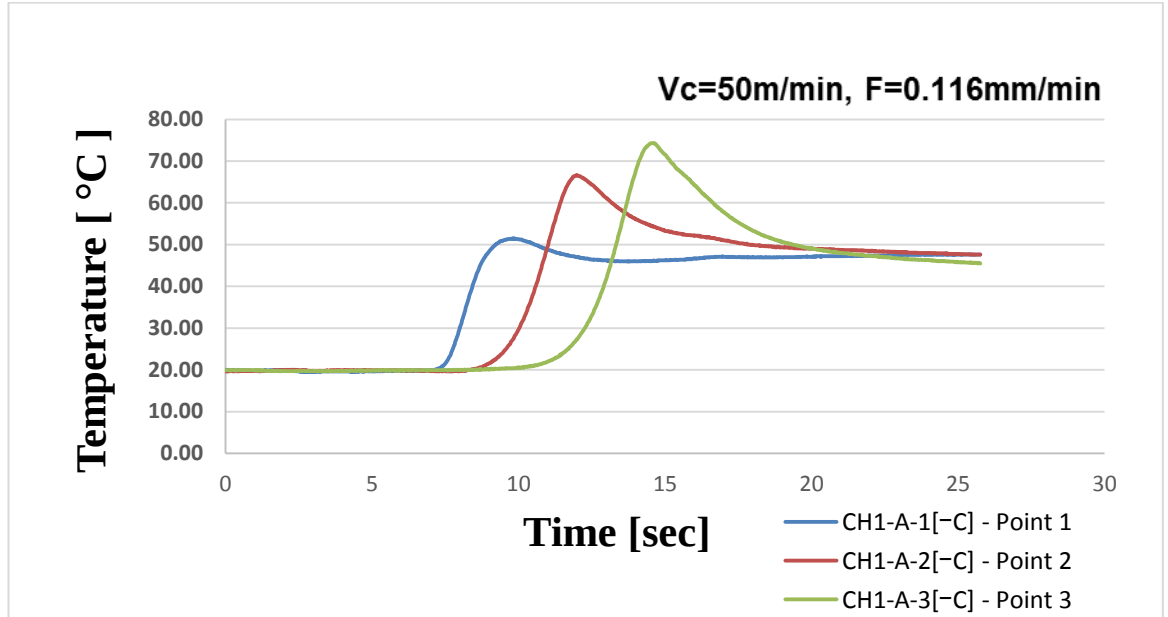


Figure 4.24 : Measured temperature results for drilling CGI.

4.5 Conclusion

In this chapter, a mechanistic model was developed to predict cutting forces during CGI drilling under dry condition. Mechanistic approach was used to model thrust force and torque values by employing orthogonal to oblique transformation. Predicted and experimental results of cutting force at the control points are in a good agreement where the error of the results is less than 10%. Through the analysis of the obtained results, cutting force values and specific cutting force coefficient distribution along the radial distance are validated.

5. DRILLING INDUCED RESIDUAL STRESS IN CGI

5.1 Overview

Residual stresses produced by non-uniform plastic deformation during the material removal processes. These stresses can be emerge in surface and sub-surface layers of the material due to different effects of mechanical loads, thermal treatments and microstructure transformations. Residual stresses affect the fatigue life, geometrical stability and even stress corrosion. As like as other metal cutting operations, determining residual stresses in drilled surface is crucial, especially when high thermal and mechanical loads applied to the hole. In this case, existence of undesirable residual stress at drilled surface would be harmful and may cause failure. Hence, it is essential for manufacturing and material engineers to study the machining induced residual stress profiles to prevent undesirable failures. In this study, residual stresses in surface and sub-surface layers are determined using X-ray diffraction and hole drilling and ESPI techniques which are described in following sections. XRD analyses are applied to measure the residual stresses at the surface of the drilled hole. Later, hole drilling method and ESPI (electronic speckle pattern interferometry) techniques employed to derive the residual stress profiles in depth. Results are presented in following sections by considering the cutting forces.

5.2 Principles of Machining Induced Residual Stress

Residual stresses are the interior stresses that remain inside the microstructure of the deformed component, as in most of material processing operations, even after all the external forces have been removed. Residual stresses commonly emerge when the material undergoes non-uniform plastic deformation, thermal treatments and microstructure deformations. These stresses are the consequence of the prevention of one region from expansion or contraction by adjacent region. They emerge as an elastic response of the material to a non-uniform plastic deformation or high

temperature rises. Residual stresses can exist as tensile or compressive stresses, or both at the same time, within a metal component. These stresses depend on several deformation parameters, can be distributed in surface or sub-surface layers in different depth. Residual stress profile (tensile or compressive) can vary within the surface toward the depth of the bulk. In general, the sign of the residual stress produced by non-uniform deformation will be opposite the sign of the plastic deformation that produced the residual stress. The residual stresses in a metal can be large enough to cause distortion or cracking. In addition, the presence of surface tensile residual stresses can contribute to fatigue and stress corrosion cracking. Usually compressive types of residual stresses are not harmful. In fact, they impede crack propagation and increase the fatigue life of the part.

During machining operations, the near-surface region of the workpiece is deformed plastically where regions near the plastic deformation undergo only elastic deformations. After removing the force, plastically deformed regions prevent the adjacent regions from completely elastic relaxation to reach non-deformed condition. Therefore, these regions introduce tensile stresses and the plastically deformed regions show compressive residual stresses due to the constraints of the adjacent bulk. Indeed, this does occur for shallow, slow, well-lubricated cuts with a sharp tool [84]. However, if the surface undergoes high rate of work hardening, the surface will be in states of tensile residual stress, because the elastic relaxation of the surface is greater compared to the adjacent regions. As mentioned before, the local compressive plastic deformation of the tool (compressive external force) results in tensile residual stress, and if the sum of local and external tensile stresses exceeds the yield strength of the material, plastic relaxation will take place at the region by initiation of the cracks. In order to eliminate tensile residual stresses and produce compressive stresses, operations such as shot peening and stress-relief heat treatments can be applied. In addition, lack of lubrication, worn tools and lower feed rate values can produce high heat at cutting deformation zones. Since the cooler adjacent regions prevent expansion of locally-heated regions, tensile residual stresses would appear in heated regions (surface). Thus, residual stress pattern and levels depend on several parameters such as cutting conditions (tool wear, cooling media, tool geometry, depth of cut, feed rate, cutting velocity, etc.), plastic behavior of material (chemical

composition) and physical properties (mechanical strength, heat transfer ratio, hardness and etc.) of workpiece and tool material.

Cutting forces during cutting operation act as the external compressive forces, which affect the residual stress distribution and magnitude within the layer of the material. Besides the other cutting conditions, studying the levels and directions of the cutting forces would help to understand and control the levels and types of machining induced residual stresses.

5.3 Residual Stress Measurements

There are several methods to measure the residual stresses in a component. These methods can be classified as destructive and non-destructive methods. One destructive method to measure the residual stresses is making a hole and measuring the residual stresses around the hole. Removing material by drilling would cause mechanical relaxation at the region because of existence of residual stresses. Using the electrical-resistance strain gauges or optical methods such as electron speckle pattern interferometry (ESPI), distortion around the hole is measured and elasticity principles are applied to calculate stresses. In this method, it is important to avoid introducing new stresses while making a hole. Non-destructive methods consist of X-ray diffraction methods, neutron diffraction, ultrasonic method and electromagnetic method where XRD and ultrasonic methods are the most common measurement methods [85]. In ultrasonic method, velocity of sound wave introduced to the component is measured. The wave velocity depends on the state of strain in the material and hence, on the residual stress. However, due to the distribution of multiple phases in materials, complex crystallographic textures, dislocation density etc. limit the general application of this technique [84]. X-ray methods for measuring the residual stresses in crystalline materials are being used for decades as a relatively reliable technique. In this method, x-ray beams with a single wavelength λ diffracted to the polycrystalline materials to measure the lattice spacing alterations, which resulted from residual stresses.

5.4 Surface Measurements Using XRD Method

X-ray diffraction method is being used commonly as a precise and non-destructive method to measure the residual stresses. Because of the feasibility and applicable nature of the X-ray devices, this method is considered as a suitable method to measure the residual stresses introduced by high plastic deformation such as machining or cold forming or etc. operations. XRD technique measures the surface residual stresses induced by plastic deformations. In addition, using the material removing operations which do not induced stresses such as electro-chemical machining, residual stress profile in sub-surfaces can be obtained. There are three mostly-used X-ray diffraction method; 1) double exposure or two angle technique (DET), 2) single exposure or one angle method (SET), 3) sine-square-psi or multi angle technique ($\sin^2 \psi$) technique. In present study, XRD measurements have been performed using $\sin^2 \psi$ method.

X-ray diffraction technique is a feasible method to measure the residual stresses in polycrystalline materials such as metals and ceramics by measuring the change in lattice spacing resulting from residual stresses utilizing a X-rays that have a constant wavelength λ . When a polycrystalline material undergoes an elastic deformation, such that the strain is uniform over a relatively large distance, the lattice spacing in the individual crystal changes from a stress-free value to a new value, depending on the magnitude of the applied stress. According to Bragg's law $\lambda = 2d_{hkl}\sin\theta$, the new spacing d_{hkl} is equal between crystals for any specific set of (hkl) planes. However, the uniform strains cause changes in lattice spacing, consequently, shift in the angle (2θ) between diffracted and incident beams [86]. The illustration of diffracted and incident beams on a crystal lattice is resembled in Figure 5.1.

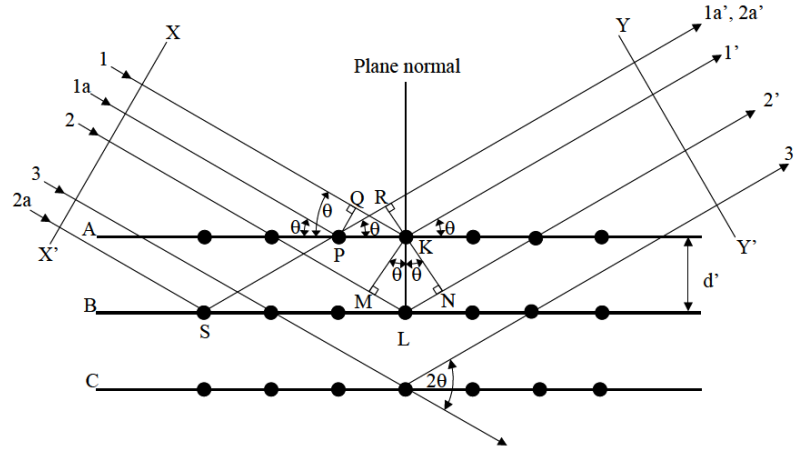


Figure 5.1 : Diffraction of X-rays by a crystal lattice.

If the material deformed plastically, the lattice planes become distorted in such a way that the distance between lattices in a set of planes varies from one crystal to another. In residual stress measuring using XRD technique, we are only interested in the elastic component of the strain and only the shifted beams resulted from uniform strains are taken into account. From the line shift, the changes in lattice spacing of (hkl) planes and consequently resulted strain in the direction of normal to the planes are calculated [53, 54]. It is to be noted that the strains resulted from residual stress are derived for the direction normal to (hkl) plane. The details of theory and procedure for calculating the residual stresses will be described in next section.

5.4.1 Principles of XRD method

Assume a portion of a surface on a stressed polycrystalline material. The principal stresses σ_1 and σ_2 which are parallel to the surface and lie on the plane of surface are shown in Figure 5.2.

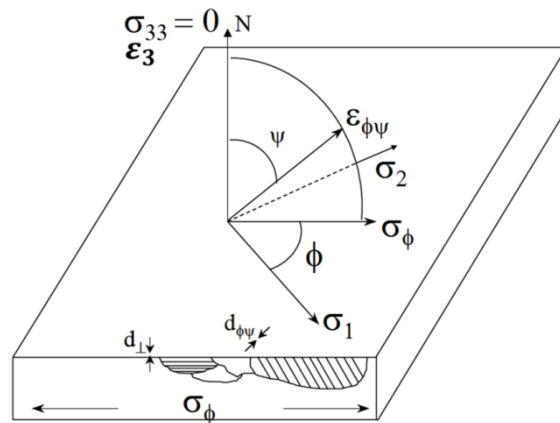


Figure 5.2 : Stress in biaxial system and definition of angles ϕ and ψ .

If it is desired to measure the single stress σ_ϕ in an arbitrary direction on the surface that makes an angle ϕ with the direction principle stress σ_1 it is convenient to use XRD method and measure in any arbitrary angle (See Figure 5.2). Although the principle stress σ_{33} is zero, but the corresponding strain ε_3 in the direction of normal is not necessarily zero and can be written as a function of principles stresses due to Poisson contraction:

$$\varepsilon_3 = \varepsilon_{\phi, \psi=0} = -\frac{\nu}{E}(\sigma_1 + \sigma_2) \quad (5.1)$$

The value of ε_3 can be found using the Equation 5.2:

$$\varepsilon_3 = \frac{d_n - d_0}{d_0} \quad (5.2)$$

where d_0 is the unstrained inter-planer spacing and $d_n (= d_{\phi, \psi=0})$ is the spacing between (hkl) planes parallel to the surface which can be identified experimentally by measuring the peak position 2θ , and solving the Equation 5.3 for a value of d_n regarding to Bragg's law;

$$n\lambda = 2d_{hkl}\sin\theta \quad (5.3)$$

Substituting the Equation 5.2 into Equation 5.1 will give:

$$\frac{d_n - d_0}{d_0} = -\frac{\nu}{E}(\sigma_1 + \sigma_2) \quad (5.4)$$

Although this measurement yields the sum of principles stresses, we cannot acquire the value of σ_ϕ ; nevertheless, we need the value of d_0 to obtain the sum of principle forces $(\sigma_1 + \sigma_2)$, which is not applicable. In order to evaluate the value of σ_ϕ , we perform two strain measurements, one in the ε_3 which is along the surface normal, and the other strain measurement $\varepsilon_{\phi\psi}$ in the direction oriented at the angle ψ to the surface normal and lying in the plane formed by the surface normal and σ_ϕ as shown in Figure 5.2. The measurement on normal direction gives ε_3 which is derived from the spacing d_n of (hkl) planes parallel to the surface. However, by altering the relative tilt between the specimen and the diffractometer, we can obtain the spacing $d_{\phi\psi}$ of (hkl) planes whose normal is inclined at an angle ψ [84]. The

representation of rotating diffractometer of the XRD device, which is utilized in our experiments, is given in Figure 5.3.



Figure 5.3 : Representation of the diffractometer of the XRD device.

Therefore, the planes that are at an angle to the surface are measured by providing the tilt so that the planes are brought into a position that they satisfy Bragg's law (See Figure 5.3).

Using the elasticity theory, we can obtain the strain at the direction ψ as follows;

$$\varepsilon_{\phi\psi} = \frac{1}{E} [\sigma_{\phi}(1 + \nu)\sin^2\psi - \nu(\sigma_1 + \sigma_2)] \quad (5.5)$$



Figure 5.4 : Schematic illustration of measuring d_n at (a) and $d_{\phi\psi}$ at (b) by diffraction, N_s is normal to specimen surface and N_p is normal to reflecting plane [84].

by subtracting the Equation 5.1 from Equation 5.5, we find;

$$\varepsilon_{\phi\psi} - \varepsilon_3 = \frac{\sigma_{\phi}}{E} (1 + \nu)\sin^2\psi \quad (5.6)$$

The equation above establish a basic fundamental for the X-ray diffraction technique. From the above-mentioned equation we can deduce that the difference between the strains of a stresses component, depends only on the plane of those strains. Expressing the Equation 5.6 regarding the plane spacing yields

$$\frac{d_{\phi\psi} - d_0}{d_0} - \frac{d_n - d_0}{d_0} = \frac{d_{\phi\psi} - d_n}{d_0} = \frac{\sigma_\phi}{E} (1 + \nu) \sin^2 \psi \quad (5.7)$$

An approximation made by Gisen et. al. which assumes that $d_n = d_0$, since they are nearly equal to each other with a negligible error [87]. If d_0 is superseded by d_n , Equation 5.7 is found to be;

$$\sigma_\phi = \frac{E}{1 + \nu} \frac{1}{\sin^2 \psi} \left(\frac{d_{\phi\psi} - d_n}{d_n} \right) \quad (5.8)$$

Equation 5.8 allows us to calculate the stresses in any arbitrary directions employing the inter-planar spacing measurements that made in a plane normal to the specimen surface and in the direction of the stress to be measured. Using the above-mentioned assumption, the need to know the stress-free lattice spacing d_0 is eliminated [84, 88].

5.4.2 $\sin^2 \psi$ method

In the present study, $\sin^2 \psi$ method is chosen to evaluate the residual stresses. In this method, values of lattice spacing $d_{\phi\psi}$ were measured at different positive and negative ψ -tilt angles. Using the least square method a line of the form $y = mx + d_{\phi, \psi=0}$ was fitted to the measured inter-planar spacing values $d_{\phi\psi}$ (or 2θ peak position) versus $\sin^2 \psi$ as shown in Figure 5.5.

The stress can be measured from the such a plot by calculating the slope of the fitted line and with basic knowledge of the elastic properties of the material such as Poisson ratio ν and elastic modulus E . This assumes that stress is zero at $d = d_n$ where d is y-intercept when $\sin^2 \psi = 0$.

$$d_{\phi, \psi=0} = \frac{\sum X_n^2 \sum Y_n - \sum X_n \sum (X_n Y_n)}{k \sum X_n^2 - (\sum X_n)^2} = y - intercept \quad (5.9)$$

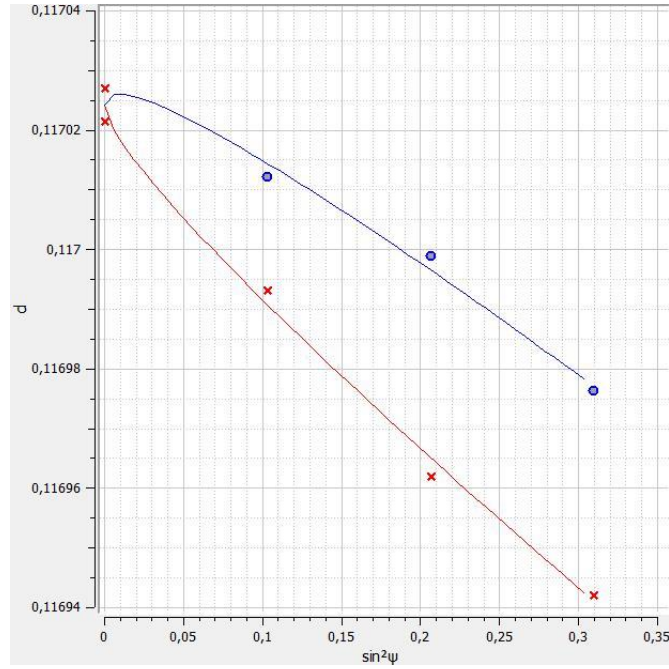


Figure 5.5 : Example of d vs. $\sin^2\psi$ for CGI specimens used in experiments.

where the slope of d versus $\sin^2\psi$ is;

$$m = \frac{k \sum (X_n Y_n) - \sum X_n \sum Y_n}{k \sum X_n^2 - (\sum X_n)^2} = \frac{\partial d_{\phi,\psi}}{\partial \sin^2\psi} \quad (5.10)$$

Substituting the slope and y-intercept in Equation 5.8 gives the following equation to calculate the residual stress values at desired direction ϕ ;

$$\sigma_\phi = \frac{E}{1 + \nu} \frac{m}{d_{\phi,\psi=0}} \quad (5.11)$$

This is worthy to note that, only the basis of the residual stress measurements using X-ray diffraction method with the utilized calculation technique is presented in this study. More complex solutions exist for non-ideal situations, for instance, psi splitting occurs (caused by presence of shear stress) or any inhomogeneous stress state within the material [88]. Details of such solutions are provided in Noyan [84] and may be embedded within software packages.

5.5 Residual Stress Measurements Using Hole Drilling and ESPI Method

Incremental hole-drilling in a commonly used mechanical measurement procedure for residual stress. In this study optical techniques are used instead of strain gauges

which are more accurate, easier and quicker without any need for specimen preparations [89]. Especially is it more convenient when several similar samples or series of location must be analyzed on one part. This method combines the hole-drilling operations with digital imaging and electronic speckle interferometry (ESPI). Basically, this technique can be applied for any material that can be drilled. The main application of this method is evaluating residual stress profile in depth by deriving the planar stresses. Figure 5.6 represent a measurement system using hole-drilling and ESPI.

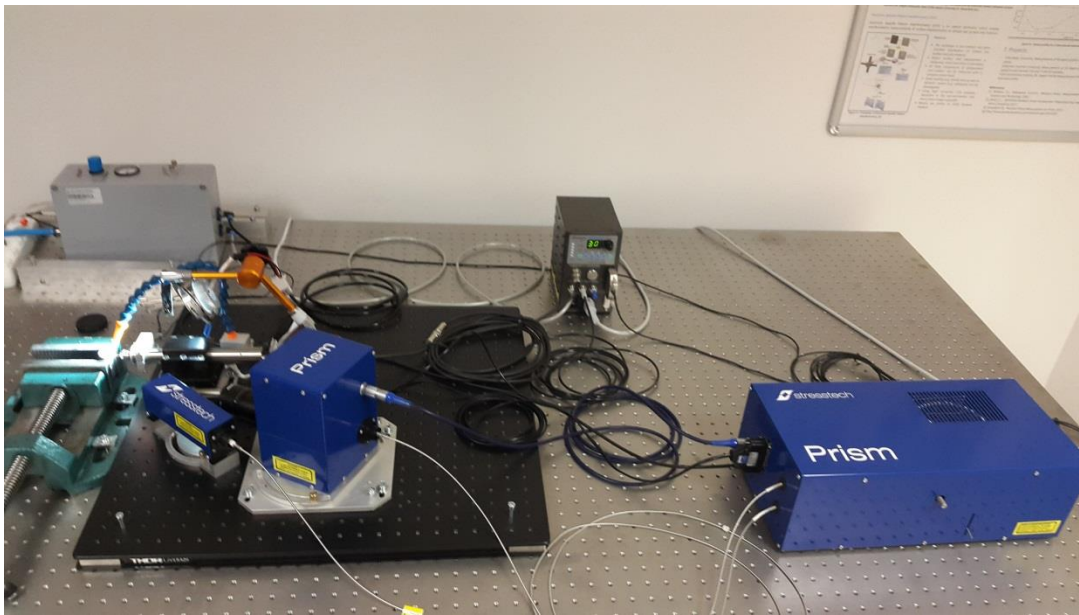


Figure 5.6 : General view of Prism measurement system.

The standard PRISM® system developed by Stresstech group is represented in Figure 5.6. This technology employs elastic stress relaxation of the stressed material, which is caused by hole drilling. This system integrates incremental hole drilling with the electronic speckle pattern interferometry (ESPI) method. Removing the material from the stressed bulk will cause rebalancing of the remaining stresses. This leads to surface displacements (distortions), which are measured optically by the electronic speckle pattern interferometry (ESPI) technique.

Further details on the principles of residual stress measurements using incremental hole drilling and ESPI are discussed in section 5.5.1.

5.5.1 Principles of hole drilling method using ESPI

Electronic Speckle Pattern Interferometry (ESPI) is an optical technique using laser beams, which enable interferometric measurements of surface distortions on almost any surface of any materials. As a coherent laser light illuminates a rough surface, each point on the surface of the object acts as a secondary source of the same light wave. The scattered light from each point of the surface contains the waves in light field. The surface roughnesses scatter the light waves in different directions and made path-length differences. If the path-length deviations exceed one wavelength, the resultant light will have a phase shift greater than 2π and corresponding amplitude, and hence intensity will varies randomly [90]. The interference of many waves of the same frequency, having different phases and amplitudes (which gives a resultant wave whose amplitude, and therefore intensity, varies randomly), is called speckle effect. As a result, capturing subsequent images using a CCD (charge-coupled device) camera generates interference patterns, the so-called speckles. Speckles of any surface are inherent and specific pertaining its surface conditions and geometry. Superimposing a reference light, which is split out of the same light (laser) source, on these speckles results in an interferogram [90, 91]. Figure 5.7 shows the typical setup for ESPI system.

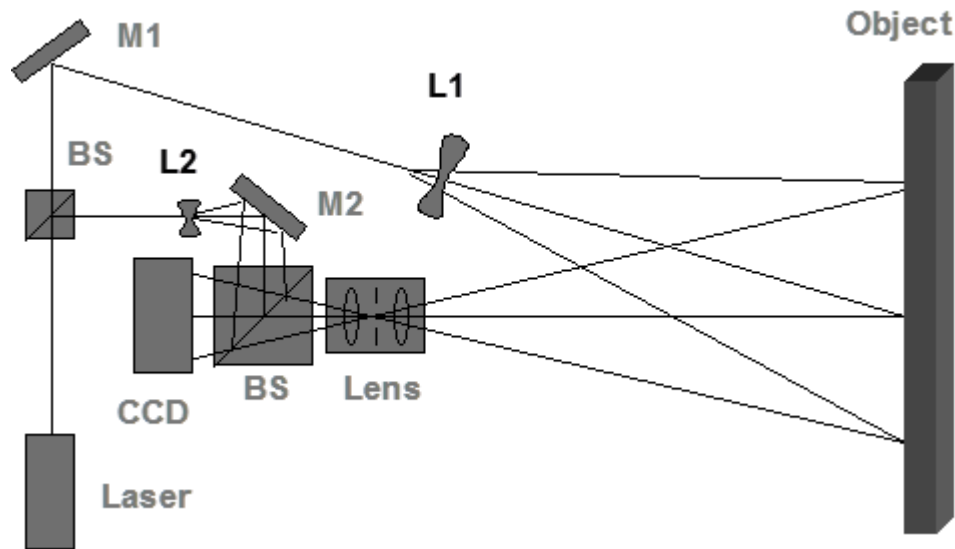


Figure 5.7 : Setup for electronic speckle patter interferometry.

As can be seen in Figure 5.7 the laser light, which used to illuminate the object's surface in a full-field manner passes through the diverging optics *L1*. Simultaneously, a reference beam, which diverged from optics *L2* and originated

from the same laser light source, is superimposed through a beam splitter BS on the CCD camera. Both wave fronts, back scattered from the object which collected with a lens and the one split out of the reference beam are imaged on the CCD camera and form a speckle pattern on interferogram. This speckle pattern of the non-deformed object will be considered as a reference for later measurements. When the object surface deformed, the wave front emanating from the object is deformed, whereas the wave front of the reference beam remains constant. Similarly, the interference of the reference beam and emanating beam, also results in a new speckle pattern (interferogram) which carries information of the deformed surface morphology. Subtracting the interferograms of the prior and after deformation states, will results in a fringe patterns. The fringe pattern reveals the displacement of the surface between the un-deformed and deformed states [92, 93]. Figure 5.8 resembles a four fringe patterns for different phase steps of reference beam. Moreover, in order to deal with the phase changes established with deformations, a set of speckle interferograms taken with different phase of reference light can be evaluated before and after deformation phase states. Using especial algorithms that yields phase changes caused by deformations, resulting intensity distributions can be analyzed [94].

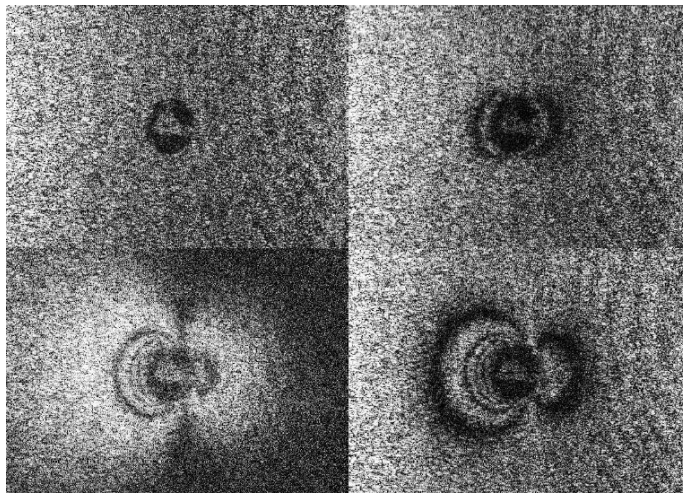


Figure 5.8 : Example of fringe patterns.

As a result, by removing the volume of material by drilling from the stresses surface, the equilibrium of the stresses has to be re-balanced which results in elastic relaxation and local strains. The elastic relaxation leads to a slight distortion of the surface near hole as shown in Figure 5.9.

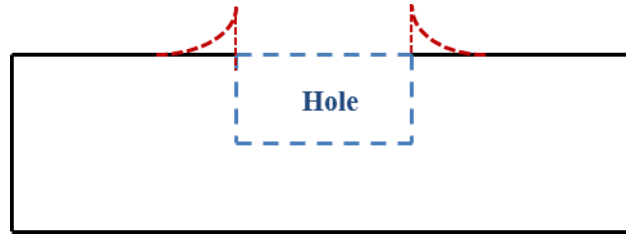


Figure 5.9 : Schematic illustration of the distortion after drilling of stressed surface.

As mentioned before, the surface distortions cause changes in fringe pattern of distorted surface compared to the that of un-deformed surface. Employing the image processing techniques, the differences of fringe patterns can be evaluated. The fringe pattern is a quantitative indications of the displacement of the surface, and hence evaluation of local strains. Using the known material parameters (Young's modulus and Poisson's ratio) the corresponding residual stresses in the elastic region are deduced from the calculated strain components.

5.6 Experimental Procedure for Residual Stress Measurements

The drilled specimens shown in Figure 4.21 are used for measuring cutting forces and residual stress either. As mentioned before, all the drilling experiments are performed in dry condition. The specimen material is GJV 400 type compacted graphite iron (CGI) which are drilled employing tungsten carbide (WC) drilling tools in 10mm diameter. The mechanical properties of compacted graphite iron (CGI) are presented in Table 4.1.

Table 5.1 : Mechanical properties of compacted graphite iron.

GJV 400 at 23°C					
Tensile Strength (MPa)	Yield Strength (MPa)	Young's Modulus (MPa)	Poisson's Ratio	Elongation (%)	Hardness (HBS)
460	350	140000	0.26	1.6	180-240

The cutting parameters that experiments are carried out are listed in Table 5.2.

Table 5.2 : Cutting parameters for drilling.

Specimen	Cutting Speed (m/min)	Feed Rate (mm/rev)
Drilling - D1	25	0.15
Drilling – D2	50	0.15
Drilling – D3	200	0.15
Drilling – D4	200	0.3

In order to measure the residual stresses from the drilled surface, the specimens are cut into two parts utilizing wire electro discharge machining method (WEDM) as shown in Figure 5.10.



Figure 5.10 : Representation of the specimens.

Residual stress measurements are performed in two different points in order to investigate the values and behavior of residual stresses at vicinity of entrance and end of a blind hole. Locations of the holes are illustrated in Figure 5.11.

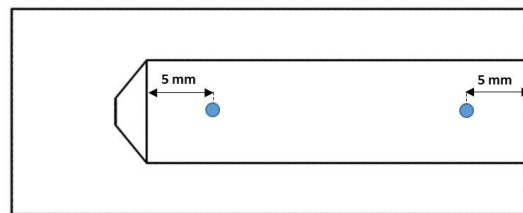


Figure 5.11 : Illustration of location of measurements.

Note that the XRD measurements are performed to acquire the residual stresses induced at surface and the incremental hole drilling method is employed to obtain the residual stress profile in depth at each measurement point. The detailed procedure for each measurement are described as follows:

XRD measurements:

Drilling induced residual stresses at the surface of the hole evaluated employing Stresstech XStress 3000 G2R- XRD Stress Analyzer device with a collimator having 1 mm of diameter (See Figure 5.12).



Figure 5.12 : XRD measurment setup.

Important parameters that are considered during XRD measurements are listed in Table 5.3.

Table 5.3 : XRD setup parameters.

Parameter	Type - Value
φ axis mode	$\sin^2 \varphi$
φ position number	18
φ positions (chi) - Axial	Between -24° and 24° (9 tilts)
φ positions (chi) -Tangential	Between -18° and 18° (11 tilts)
φ oscilations	$\pm 5^\circ$
Φ positions	0° and 90°
Radiation	$\text{CrK}\alpha_1$
Tube Voltage and Current	30 kV, 6.7mA
Peak	156.4° (211)

In order to acquire the sufficient and complete data about the residual stress components and principle stresses at each point, measurements are performed in

tangential and axial directions. Measurements were emerged in a way that collimator was able to be positioned right in the middle of the hole cavity. Figure 5.13 illustrates the measurement directions in XRD measurements.

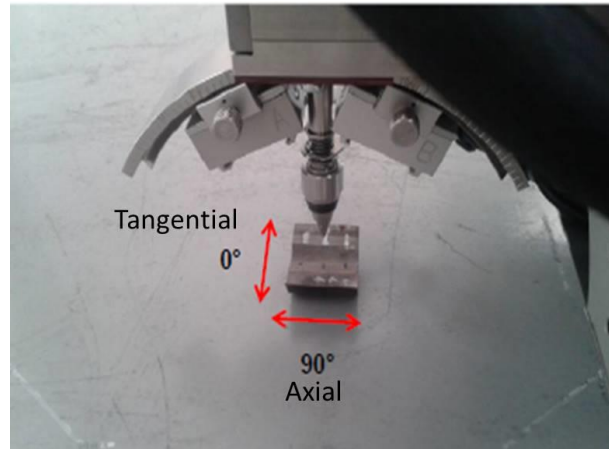


Figure 5.13 : Directions of XRD measurements.

The residual stress values obtained from the surface of the hole in both directions are listed in Table 5.4;

Table 5.4 : XRD results measured from surface.

	Points	σ (MPa)	$\pm$ (MPa)	σ (MPa) Axial	$\pm$ (MPa)
		Tangential			
Specimen	D1-1	-176.2	35.5	-218	17.5
	D1-2	-172.7	41.5	-2.7	8.9
	D2-1	-197.2	41.7	-222.3	10.2
	D2-2	-105.6	39.2	-124.7	15.6
	D3-1	-234.4	39.3	-250.1	23.2
	D3-2	-133.3	41.6	-177.2	24.7
	D4-1	-22.7	24.7	27.2	24.3
	D4-2	-131	20.1	-185.3	14.6

Incremental hole-drilling method:

After evaluating the residual stress data from surface of the hole at each point, hole-drilling operation applied at each point in order to obtain the residual stress profile in depth. For this purpose, PRISM® – hd1000 system developed by Stresstech group, is employed. PRISM® device uses the incremental hole-drilling and Electronic Speckle Pattern Interferometry (ESPI) in order to calculate the residual stress at measurement

locations in depth. Different components of the measuring device are shown in Figure 5.14.

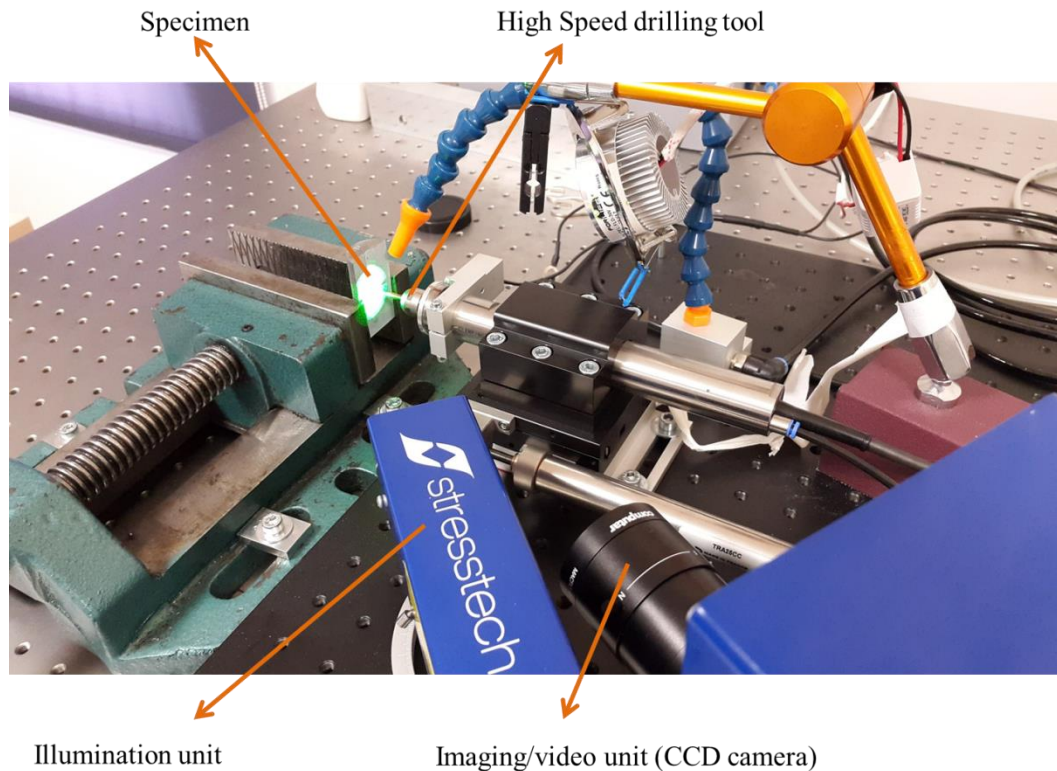


Figure 5.14 : Hole drilling and ESPI setup for residual stress measurements.

In order to remove bulk and generate elastic relaxation in stressed area, the sample was drilled employing a high speed drilling with a drill bit size of 0.8 mm diameter. The depth studied for evaluation of residual stress profile is about 0.5 mm, since the optimum depth for the hole-drilling technique is 50-60% of the drill bit diameter [69]. Along the measurement depth, 10 measurement steps were acquired in different step size. In order to obtain a better reflection during optical measurements, the specimens were lightly spray-painted as shown in Figure 5.15.



Figure 5.15 : Spray-painted sample.

The corresponding parameters for the hole-drilling using ESPI method are listed in Table 5.5.

Table 5.5 : Parameters for incremental hole-drilling technique.

Drill bit diameter	0.8 mm
Drill bit type	2-fluted, TiN coated
Drilling system speed	40,000 rpm
Illumination angle	60°
Observation angle	27°
Laser light wavelength	532nm
Laser light power	25 mW
Measurement depth	0.5 mm
Masurement direction	Tangential and Axial

Figure 5.8 and Figure 5.16 resemble the fringe pattern of point 2 of condition D2 (See Table 5.1) captured during the experiments for each reference beam phase step.

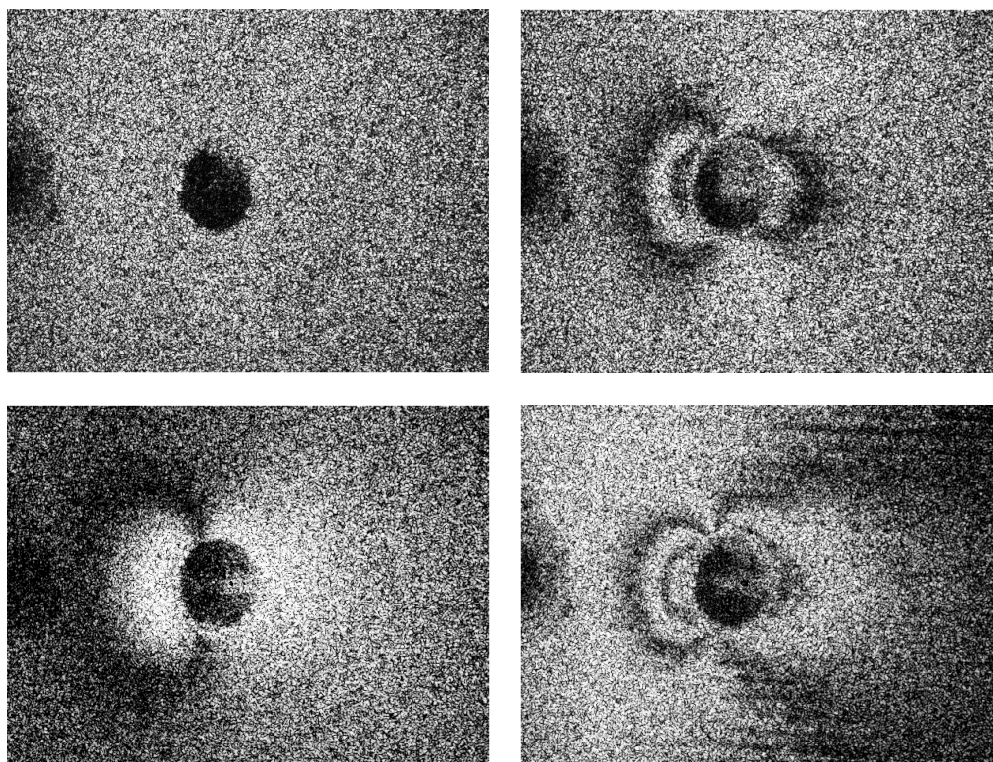


Figure 5.16 : Fringe pattern for the point D2-2.

According to the theories discussed in previous chapters, surface displacements are evaluated by illuminating laser light on the surface of the material and capturing the speckle pattern before and after the deformation. Removing material with high-speed drilling device results in elastic relaxations, and hence local strains. The capturing device will record the fringe patterns generated by illuminated laser light on the rough bulk surface. The systems captures three records for each step (before drilling and after drilling).

5.7 Results and Discussion

Measured residual stress results using XRD and incremental hole-drilling method presented for the cutting conditions listed in Table 5.2. In order to obtain the full residual stress profile in depth, the XRD measurement results from the machined surface are integrated with the optical hole-drilling method. It is noteworthy that due to geometric limitations the measurements had been performed in axial direction only. The curvature of the hole hinders evaluating reliable results in tangential (hoop) for both XRD and hole-drilling techniques. Therefore, the meaningful stresses via this method can be obtained only in axial direction up to a certain depth. For this reason, only the axial stresses are considered for investigation of surface integrity in holes [69]. In addition, due to the large thickness of the bulk material in specimens, the stress relaxation due to the cutting apart the hole in axial direction is neglected. The residual stress results are presented in the following figure for each cutting condition. It is to be noted that, as illustrated in Figure 5.11 the *Point-1* and *Point-2* indicate the measurement points at the entrance and end of hole, respectively. The main purpose for selecting these locations is to study the effect of drilling operation along the hole. Although the cutting conditions stay constant during the drilling, chip clogging and thermal gradients are increased by penetrating into the hole and affect the residual stress values. It is known that plastic deformations and thermal effects are the main factors in introducing residual stresses during machining. However, cutting parameters such as cutting velocity, feed rate, tool geometry, cooling media and rake angle have their own influence on generating more thermal gradients or mechanical loads [95].

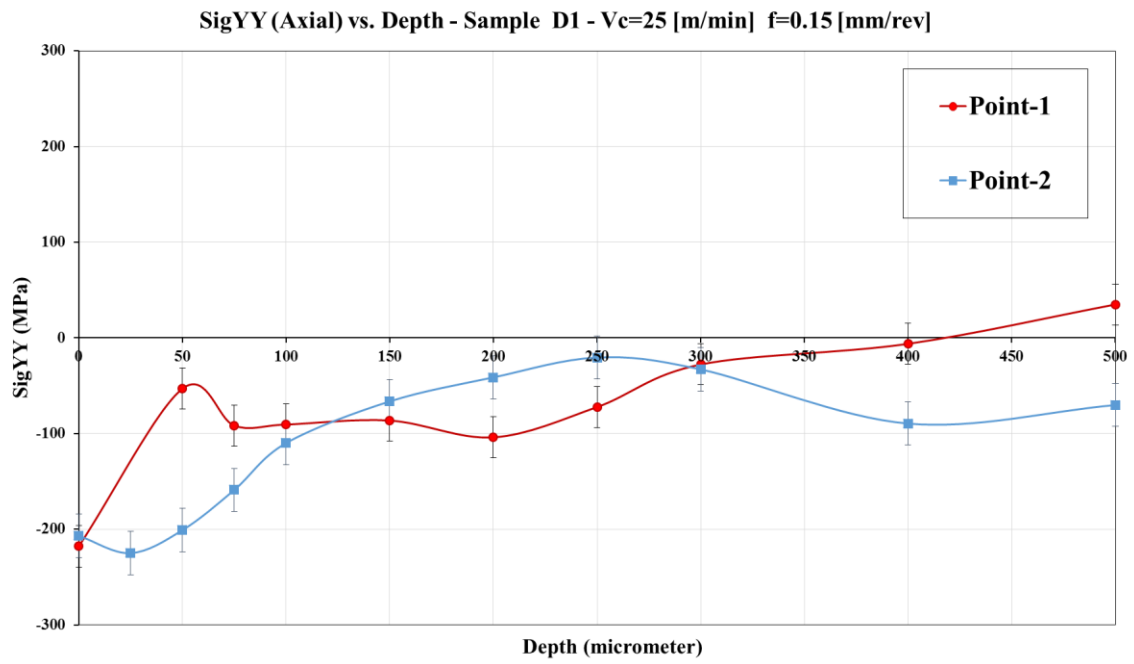


Figure 5.17 : Residual stress profile for condition D1.

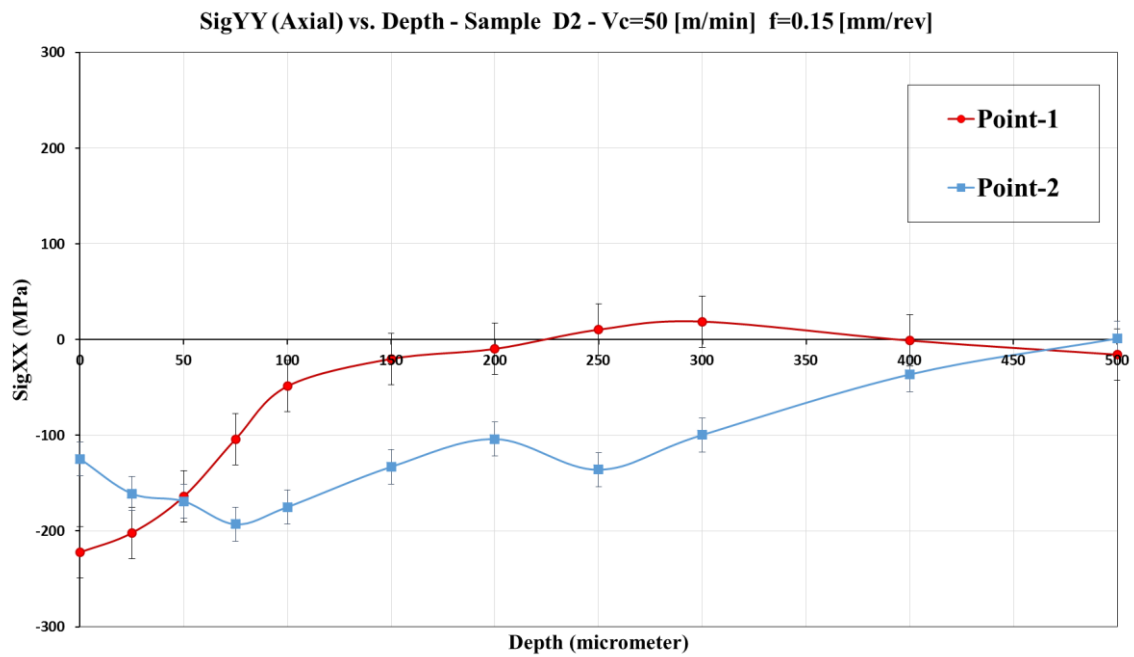


Figure 5.18 : Residual stress profile for condition D2.

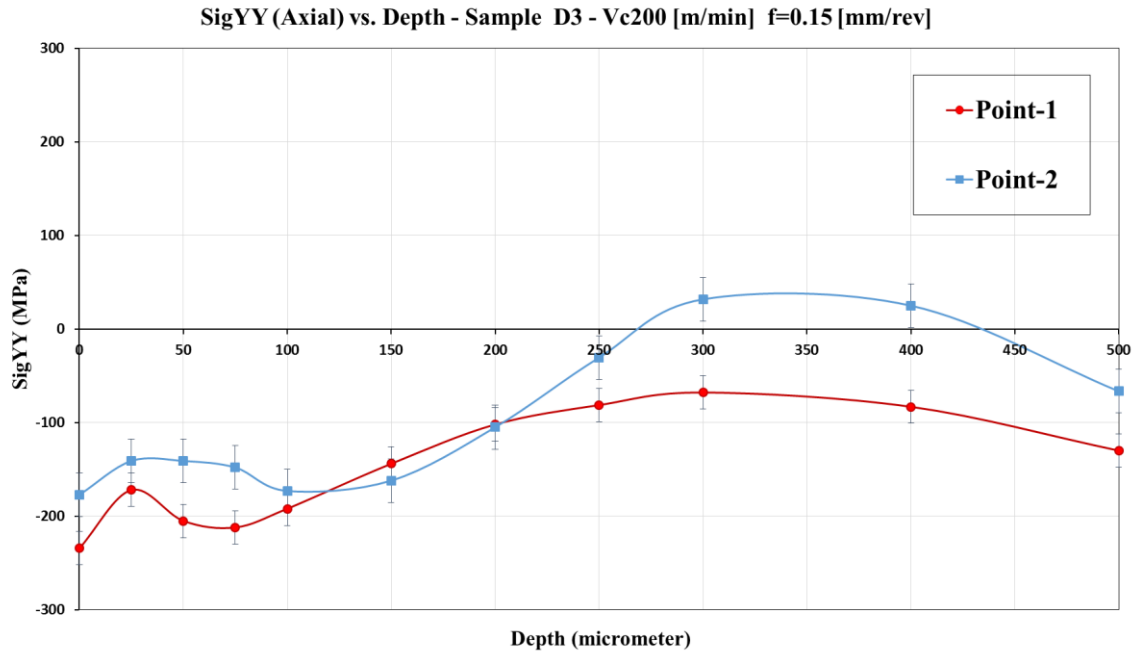


Figure 5.19 : Residual stress profile for condition D3.

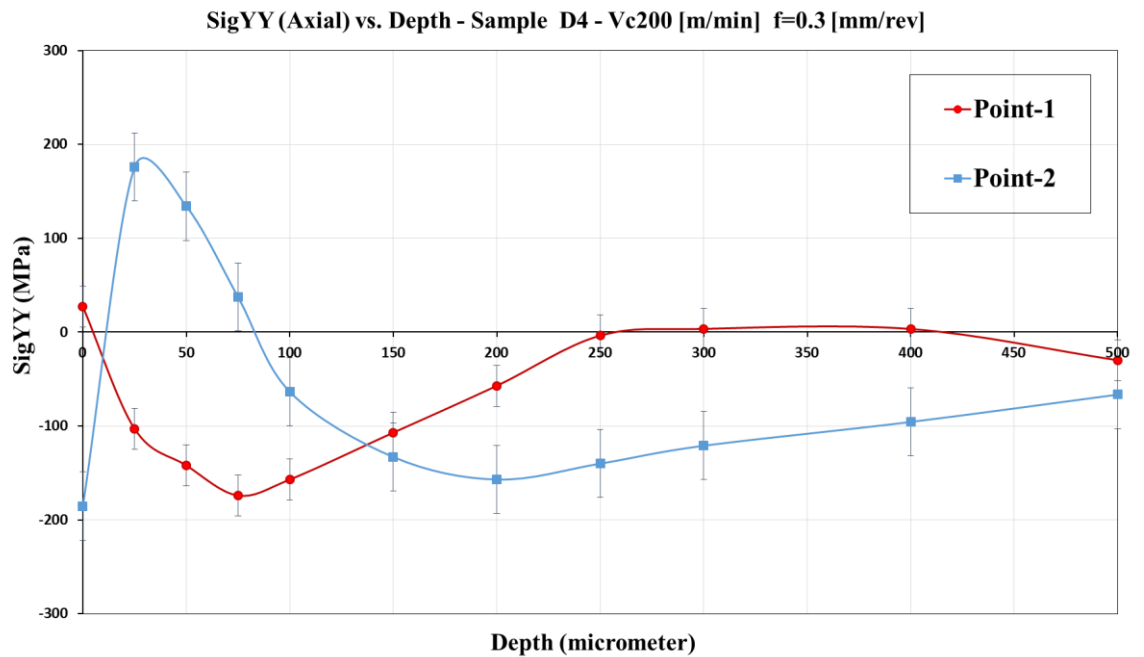


Figure 5.20 : Residual stress profile for condition D4.

As can be seen in Figure 5.17 - 20 the most of residual stress profiles are compressive at surface and subsurface regions and decay to zero by moving forward into the depth of the bulk. In order to have extensive insight of residual stress profiles in machining, the approach proposed by Jacobus et. al. is presented in this study [58]. Primarily, the workpiece is divided into three distinct zones as in Figure 5.21. The Layer indicated as S corresponds to the layer, which had direct contact with the tool

and undergoes thermal and mechanical effects simultaneously. Therefore, the total strain in this region can be written as the sum of elastic strain ε_S^E , plastic strain ε_S^P and thermal strain ε_S^T .

$$\varepsilon_S^E + \varepsilon_S^P + \varepsilon_S^T = \varepsilon_S \quad (5.12)$$

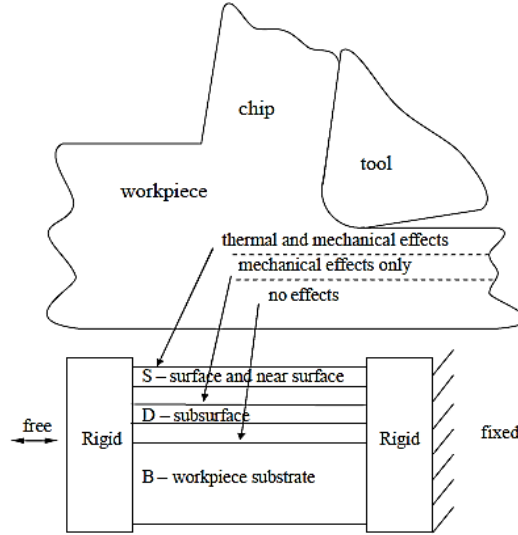


Figure 5.21 : Representation of machining induced layers of workpiece [58].

In addition, layer *D* resembles the subsurface region where the machining induced residual stress are resulted only from mechanical loading. Therefore, the total strain can be written as the sum of elastic relaxation and plastic deformation strain as follows;

$$\varepsilon_D^E + \varepsilon_D^P = \varepsilon_D \quad (5.13)$$

Layer *B* represents the bulk material where the amount of residual stress is negligible and the occurred strain is elastic. According to chapter 5.2, if machining introduce tensile plastic strains to either of surface/near-surface and subsurface layers, the part will have compressive residual stress in that region. Moreover, machining operation imparts thermal gradients which results in thermal expansion, the locally-heated part will have tensile residual stress since the cooler adjacent bulk will not allow to have elastic deformation. Assuming that the corresponding elastic strain of surface/near-surface and subsurface are equal, and using the Equations 5.12 and 5.13 the following relation can be achieved;

$$\varepsilon_D^P - \varepsilon_S^T = \varepsilon_S^P \quad (5.14)$$

It is well-established fact that temperature rise always happen in the workpiece due to the material shearing effect and friction. Therefore, thermal strains in the surface layer would not be negative (thermal strains always occur), $\varepsilon_S^T \geq 0$. Assuming the flow of material around the tool is continuous the far-field boundary conditions require that $|\varepsilon_S^P| \geq |\varepsilon_D^P| \geq 0$ in absence of thermal effect. In other meaning, the magnitude of surface plastic strains are greater or equal to that of the subsurface plastic strain and also their signs are the same. Consequently, considering the plastic and thermal effects and their domination during the process, author drawn a conclusion about the residual stress profiles in depth as follows and shown in Figure 5.22;

- **Case 1 :** Mechanical loadings are dominant and thermal strains are less than plastic strains $\varepsilon_D^P > \varepsilon_S^T \geq 0$. As a result, since the plastic strains are positive are positive in either of surface and subsurface layers, the residual stress at these regions is compressive. The mechanical loads dominate the thermal gradients. Note that the dotted attributed to the residual stresses from pure mechanical loading, whereas, the solid line is combination of mechanical and thermal effects.
- **Case 2 :** Significant heat gradients in the surface leads to thermal strains in this region which is dominant compared to plastic strains in subsurface $\varepsilon_S^T > \varepsilon_D^P > 0$. As a result, the surface/near surface layer influenced less by plastic strains. However, the effects of plastic deformation still remains in the subsurface region. Combination of both effect leads to have tensile residual stress at surface or near surface regions and continues to compressive residual stresses at subsurface region and decays to zero within the bulk.
- **Case 3:** Both of the surface and subsurface residual stresses are influenced by thermal effect. The plastic strain is negative $\varepsilon_S^P < \varepsilon_D^P < 0$. Therefore there is no plastically affected zone. All the workpiece has tensile residual stress. Furthermore, presence of compressive residual stresses decrease the tensile stresses and moves the diagram to compressive direction.

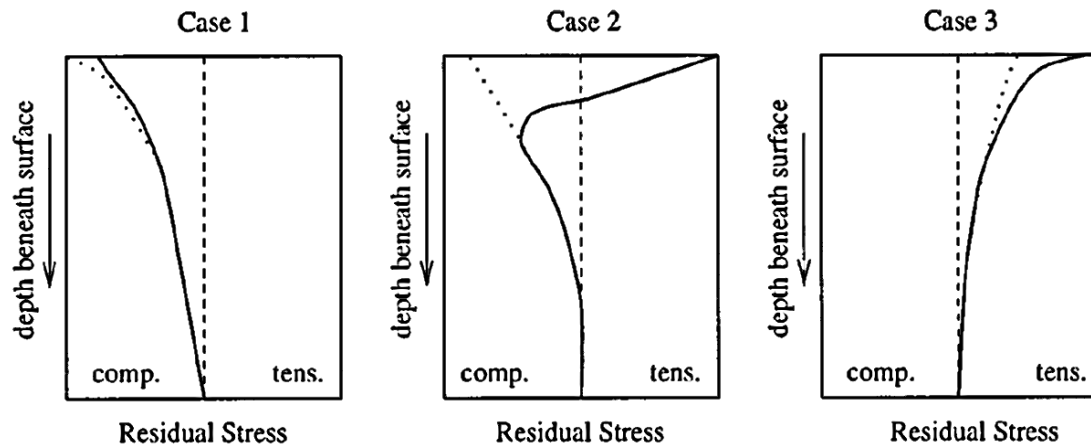


Figure 5.22 : Proposed residual stress profiles[58].

According to the experimental measurements presented in Figure 5.17 - 20 and the approach discussed, it is seen the residual stress profiles of drilling match mostly with Case 1 residual stresses. Therefore it can be deduced that the mechanical effects are more dominant and introduce compressive residual stress in both surface and subsurface layers. It is to be noted that thermal gradients exist and affect the profile, however, they cannot exceed the effect of plastic strains and the resulted residual stress profiles become fully compressive [58]. Several parameters are responsible for this phenomenon. During drilling operation, the workpiece material which is affected from chisel edge, where have negative rake angles, are cut and evacuated as the chip. The only part that affect the hole surface is the outer element of cutting edge at vicinity of periphery of the twist drill. As mentioned in Chapter 4. , outer portion of cutting edge near periphery of drill, has high positive rake angle values and lower inclination angles. Central part of the drill has negative rake angle values, where the highest thrust force and temperatures appear at the vicinity of chisel edge [2, 3, 96]. On the other hand, high values of rake angle at periphery of tool causes more smooth cutting with low cutting forces, consequently, low compressive residual stresses. It is reported that high rake angles usually causes less compressive residual stresses in absence of thermal effects and hence, studying compressive mechanics is less important in high rake angles [97].

Another justification for the completely compressive behavior of residual stresses in drilling operation is the combined effects of harness of material and cutting conditions. Shear angle variation has a significant influence in residual stress pattern caused by different rake angle and material hardness values during chip formation

process. Compacted graphite iron can be considered as a relatively hard material as like as annealed and cold drawn alloy steel. Higher rake angle and hardness values of workpiece material results in higher shear angle values which leads to a less compressive normal stresses ahead of the tool, and hence, a predominantly tensile load concentrated behind the tool. During cutting operation, the material element in front of the tool suffers compressive stress followed by yielding. After relaxation of the material and second yielding occurs due to shearing effect, then the tensile load released until the material returns to zero strain. The distance between the starting and ending of the cycle represents the residual stress. As a result, predominant tensile stresses at the machined surface caused by high shear angles, leads to compressive residual stress pattern[57], as shown in Figure 5.23. Note that, brand new drill bits have been used for drilling at each condition in order to keep the nose radius value constant during experiments.

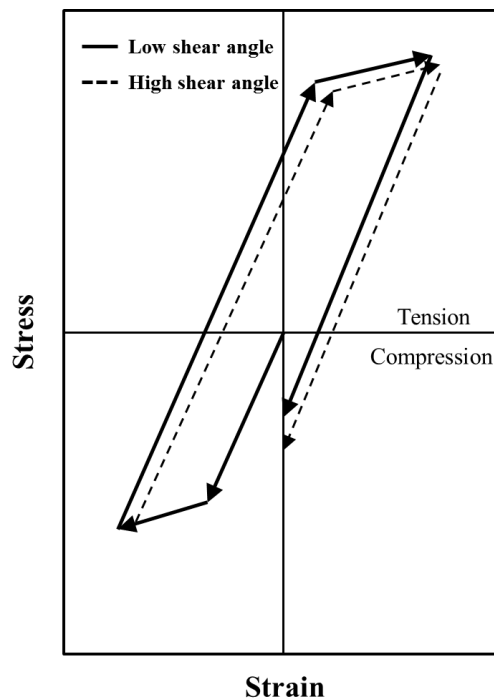


Figure 5.23 : Loading cycle pattern in terms of shear angle [57].

Moreover, according to the minimum energy principle in metal cutting [45], higher shear angles reduce the friction angle relatively and friction coefficient as well. Therefore, thermal stresses will also decreased, which in turn ameliorate the residual stress to more compressive and hence tensile residual stress tendency decreased. In addition, compacted graphite iron has a unique thermal conductivity compared to other alloyed steel. This property allows the CGI to dissipate heat quickly and

undergo a relatively low heat expansion. Consequently, thermal stresses cannot influence the material, neither heat expansion or microstructure transformation [22]. We can conclude that, during drilling operation compressive residual stresses are resulted due to plastic strains. Effect of relative temperature rise along the hole can be seen in subsurface regions of holes in Figures 5.17 - 20. As can be seen in the results, at condition D1(See Figure 5.17) the stress profile at surface in both of the points are similar, hence, Point 1 showed more tensile behavior. However, by increasing the cutting speed, which in turn cause high thermal gradients and thermal stresses during cutting, in a constant feed rate in conditions D2 and D3, (See Figure 5.18 and Figure 5.19), the residual stress profile of Point 2 exposes more tensile behavior. This is attributed to the fact that since the heat gradients reaches to a constant regime along the hole the thermal effect is appeared more effectively. Whereas, Point1 located at the entrance of the hole and the heat generation cannot be considered effective. Consequently, in order to examine the cutting effect of cutting conditions on residual stresses, the profiles of Point2 were considered in comparisons. Figure 5.24 shows the residual stress profile of all cutting conditions exposed at Point 2.

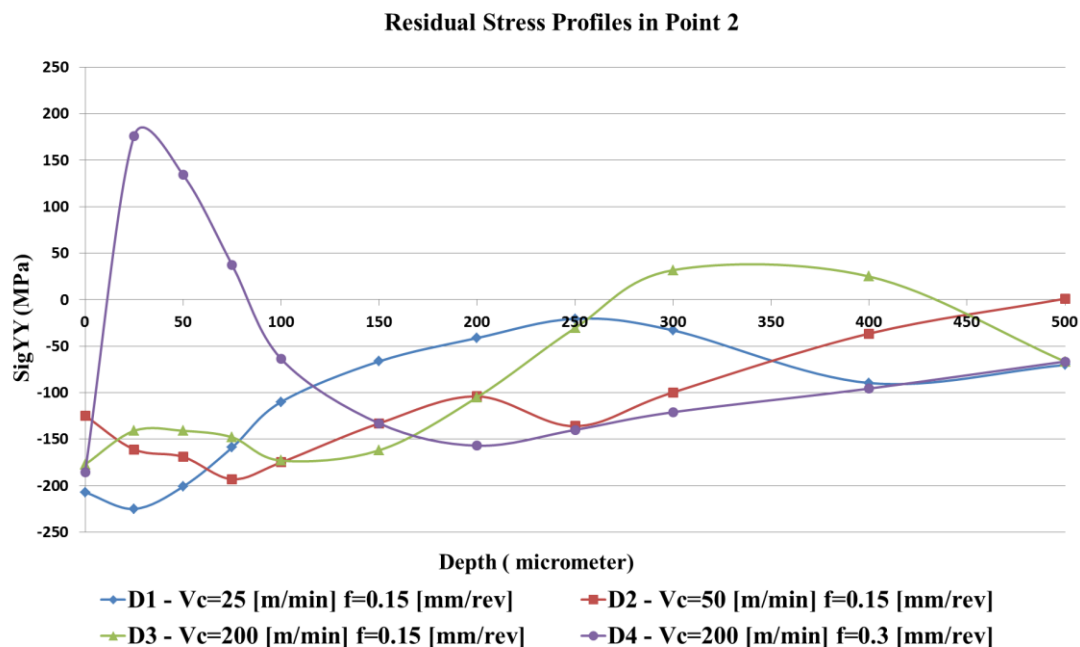


Figure 5.24 : Residual stress profiles of all condition at Point 2.

According to the results, it can be deduced that increased cutting velocity has influenced the residual stress profile at near-surface layer. Increasing cutting velocity

at conditions D1, D2, and D3 leads the residual stress to be more tensile. This is due to the increase in plastic deformation rate which causes high thermal gradients in near-surface layer. However, the generated heat gradients is not sufficient to shift the residual stresses to completely tensile manner. According to the measured temperature results measure in Figure 5.24 the temperature values is not sufficient to affect the material thermally. Also, Kuzu et. al. [98] has modelled the drilling temperatures at CGI. It is reported that the maximum heat gradients appears at chisel edge, where has been cut and carried away by chip. The temperature seen at drilled hole surface is near 120°C, which in turn cannot affect the residual stresses significantly. However, this temperature will increase by increasing the cutting speed and the resulted thermal stresses can affect the compressive residual stresses slightly. Moreover, due to high thermal conductivity of CGI and high heat evacuation by chip at high cutting speeds, the generated heat gradient cannot expand the material or affect the microstructure to alter the residual stress profiles significantly [97]. Comparing the conditions D3 and D4, it can be deduced that due to the high plastic deformation caused by high material removal rate (MRR), increases the compressive stresses and consequently leads to a high elastic deformation at layers below the surface, which in turn cause tensile residual stresses in this condition [99].

5.8 Conclusion

Using the combination of XRD and incremental hole-drilling method with electronic speckle pattern interferometry sounds as an accurate technique for measuring residual stress. Residual stresses in axial direction have been evaluated as a function of depth from the machined surface. These profiles are critical important for selection of cutting parameters to avoid or minimize tensile residual stresses which are detrimental to the fatigue performance of machined part, especially in diesel engine blocks. These profiles indicate that drilling operation induce compressive stresses at surface and subsurface layers of CGI. This is attributed to high rake angle of drill bit at periphery where engage with material and cause relatively high shear angles and low cutting forces. Moreover, high thermal conductivity of compacted graphite iron is also leads to low thermal expansion of material which in turn cause tensile residual stresses. According to the result, by increasing cutting speed residual stress profiles show a tendency to be more tensile in axial direction due to heat

gradient generation resulted from high plastic deformation. Whereas, high feed rate influenced the residual stresses in detrimental manner. As a result, it can be deduced that in drilling operation, mechanical loadings are more dominant than thermal stresses in cause compressive residual stress.

6. CONCLUSION AND RECOMMENDATION FOR FUTURE WORK

6.1 Conclusion

This thesis presents the modification of mechanistic modeling of drilling process for split-point twist drills. The material used is compacted graphite iron (CGI), which is focus of interest in automotive and locomotive industries. The proposed model encompasses the twist drill geometrical parameters, tool-workpiece material pair properties, and cutting condition to predict the cutting forces in drilling operation. This model allows prediction of drilling forces using basic information about workpiece material and twist drill geometry. However, in order to derive the workpiece material properties, orthogonal tube cutting test have been carried out. Using the cutting mechanics approach and data obtained from orthogonal test, orthogonal to oblique transformation has been applied to model the oblique cutting edges of drill bit. Having transformed the oblique forces to drill coordinate, total thrust force and torque values were derived. The predicted results validated with experimental drilling force measurement results. Due to some reasons, cutting mechanics approach over predicts the cutting forces. Accurate geometrical modeling of cutting lips of twist drill can eliminate this error. Moreover, the regression model for evaluating orthogonal cutting parameters must be enhanced by performing more orthogonal test. However, the prediction has been presented by an acceptable error.

Residual stress measurements have been performed in depth from machined surface in order to obtain the residual stress profile in different cutting conditions. The XRD technique have been employed to measure the residual stresses within surface. The incremental hole-drilling method with the electronic speckle pattern interferometry (ESPI) technique has been used to derive the residual stress profile in depth. Due to geometrical constraints, the residuals tresses in axial direction were considered. Drilling operation induce compressive residual stresses in either of surface and subsurface regions. This shows that mechanical loadings are more dominant than

thermal loading and normal tensile loading behind the tool during cutting exceed the compressive loads resulted from thermal expansion. As a result, compressive residual stresses are induced in the workpiece and the hole are beneficial for fatigue life of the component. Moreover, it can be seen that increasing cutting velocity affects the residual stress profiles. Increasing cutting velocity, rises thermal gradients at tool-chip-workpiece interfaces, hence, residual stresses presents tendency to be more tensile. However, the resulted residual stress profile is the combination of the thermal and mechanical effect, simultaneously. However, mechanical loading effect dominates the thermal gradient's influence.

6.2 Recommendation for Future Work

This work presents the force modeling for the drilling operation of CGI. Prediction of cutting mechanics and resulted residual stresses are the next significant challenges which is an ongoing research in our group. The whirling, hole profile errors and tool deflection resulted from tool geometry asymmetry, which in turn affect the cutting forces can be discussed in future. Tool deflection, mechanical failure, and induced residual stresses in high alloy steels can be challengeable tasks in this area. Low-diameter drilling tools are being considered more than before, especially in electronic and medical sciences. Residual stress investigations, as well as mechanistic modeling, in especial alloys in medical and electronic devices need to deliberate on it meticulously. Addition of different cooling media such as; minimum quantity lubrication (MQL), cryogenic and high-jet cooling presents various difficulties in interpretation of residual stress results.

Furthermore, measuring steps in incremental hole drilling technique can be improved with even more accuracy. The present study gives the brief insight regarding drilling insight residual stress in compacted graphite iron.

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APPENDICES

APPENDIX A: Regression Results

APPENDIX A

- Regression Results for uncut chip thickness of $h=0.1\text{ mm}$

$$\tau_{sh=0.1} = 404.9 + 0.2053\alpha_n + 0.0312V_c$$

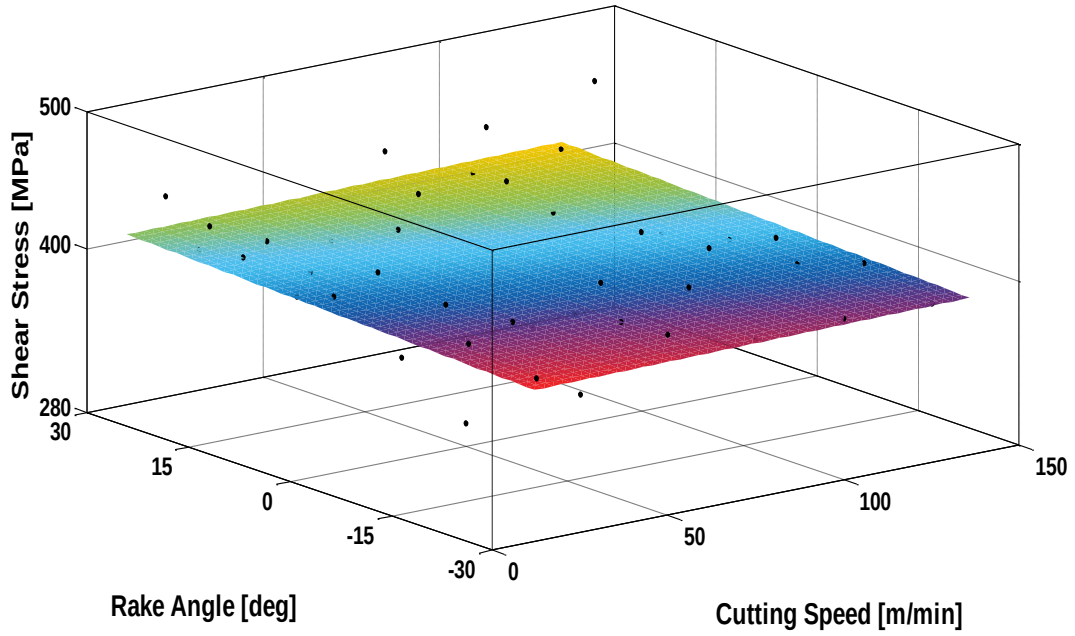


Figure A.1 : Regression diagram of shear stress for $h=0.1\text{mm}$

$$\phi_{nh=0.1} = 20.73 + 0.2379\alpha_n + 0.02216V_c$$

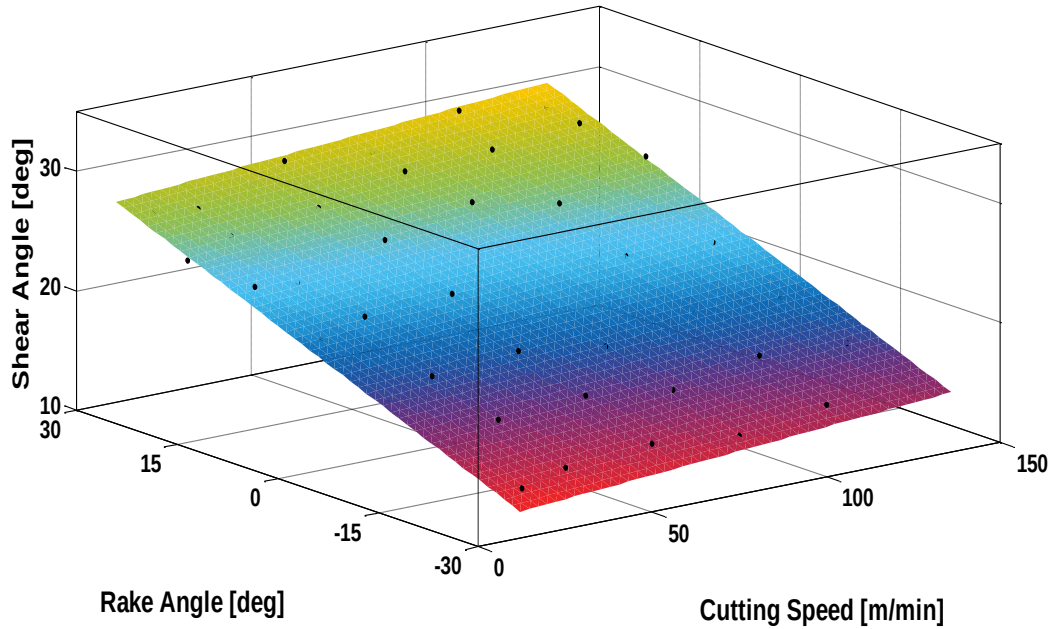


Figure A.2 : Regression diagram of shear angle for $h=0.1\text{mm}$

$$\beta_{n_{h=0.1}} = 20.85 + 0.4414\alpha_n + 0.3155V_c - 0.002V_c^2 + 0.0003854\alpha_n V_c$$

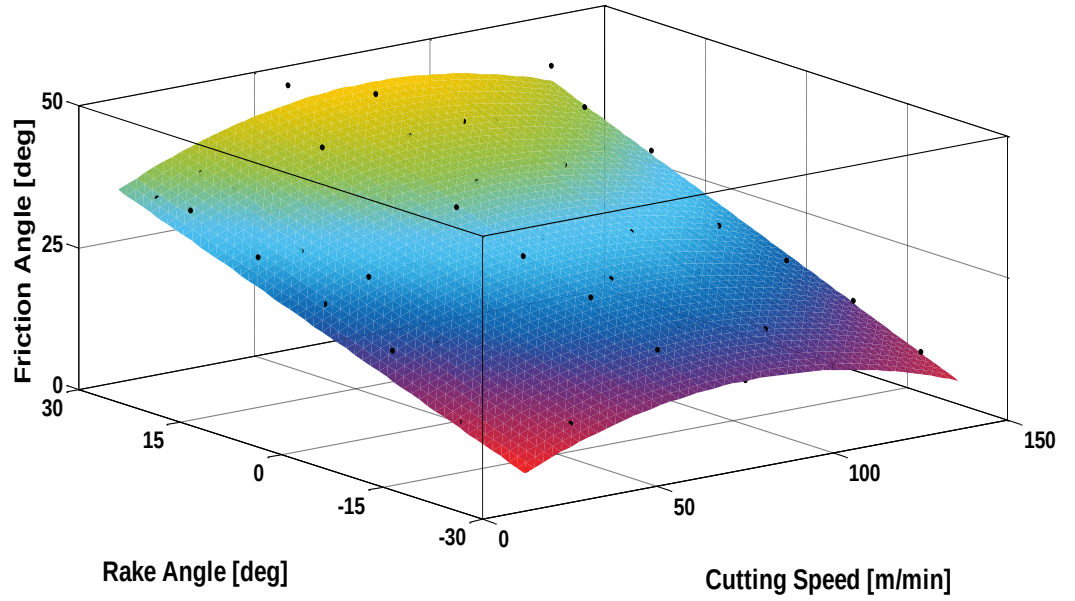


Figure A.3 : Regression diagram of friction angle for $h=0.1$ mm

- **Regression Results for uncut chip thickness of $h=0.15$ mm**

$$\tau_{sh=0.15} = 415 + 0.2833\alpha_n + 0.09298V_c$$

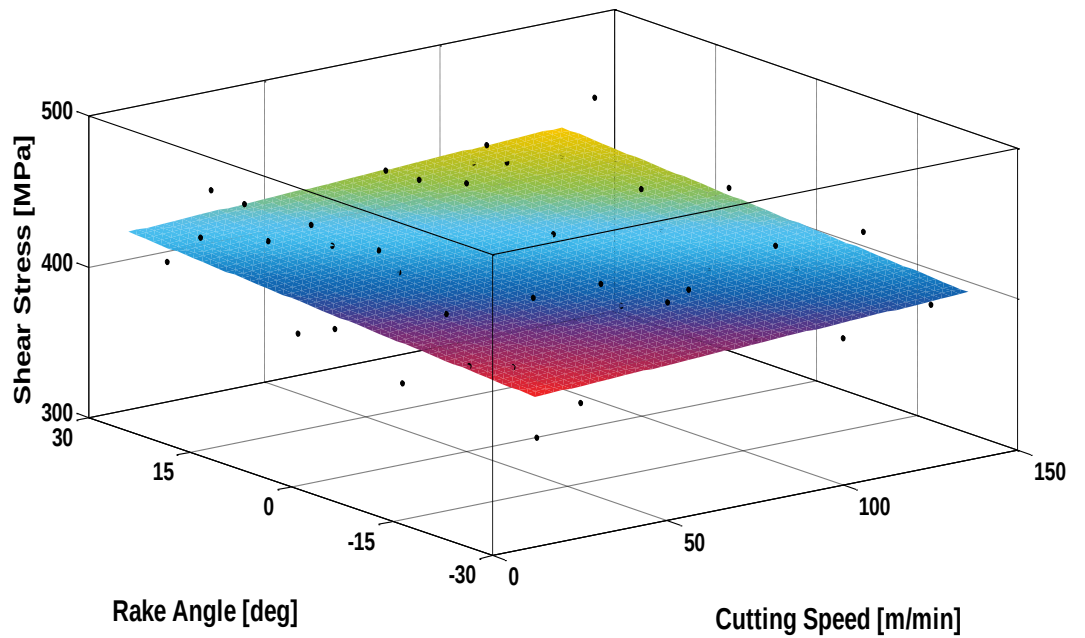


Figure A.4 : Regression diagram of shear stress for $h=0.15$ mm

$$\phi_{n_{h=0.15}} = 21.89 + 0.2603\alpha_n + 0.03454V_c$$

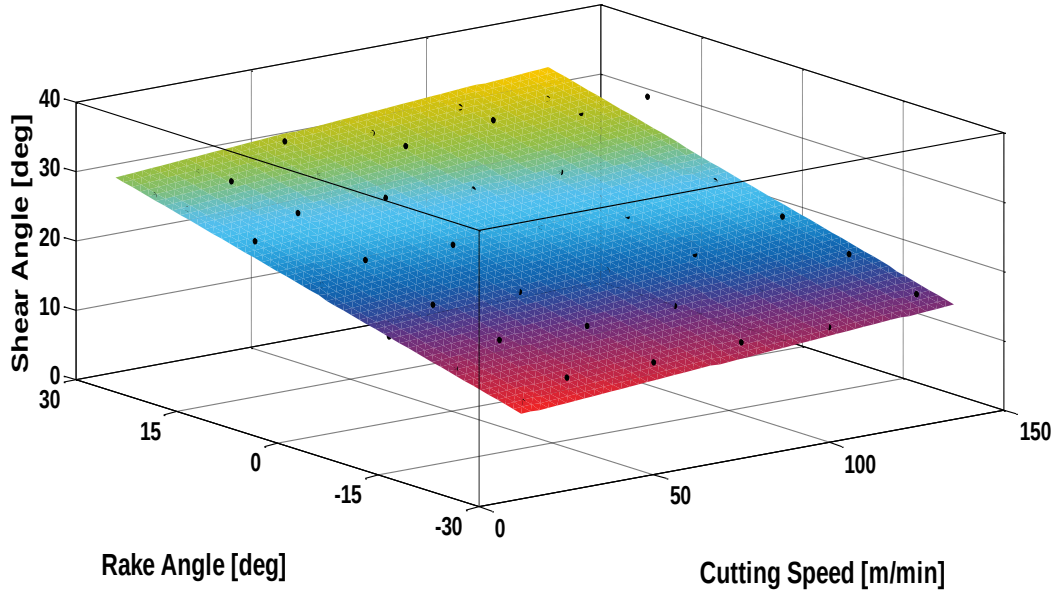


Figure A.5 : Regression diagram of shear angle for $h=0.15$ mm

$$\beta_{n_{h=0.1}} = 21.9 + 0.2591\alpha_n + 0.4826V_c - 0.001711\alpha_n^2 + 0.0001553\alpha_n V_c$$

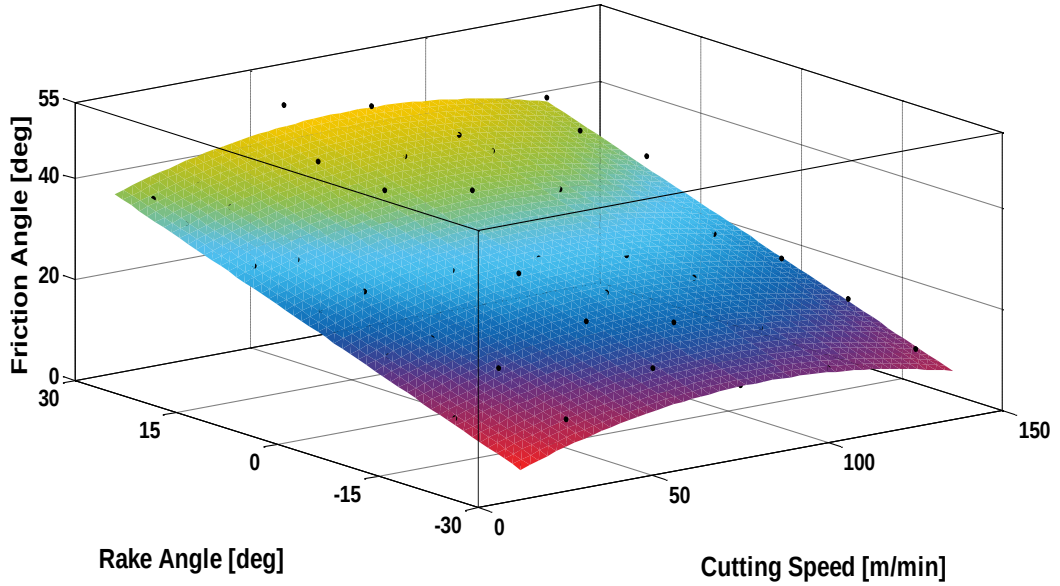


Figure A.6 : Regression diagram for friction angle of $h=0.15$ mm

- **Regression Results for uncut chip thickness of $h=0.2\text{ mm}$**

$$\tau_{sh=0.2} = 418.7 - 0.2221\alpha_n + 0.007788V_c$$

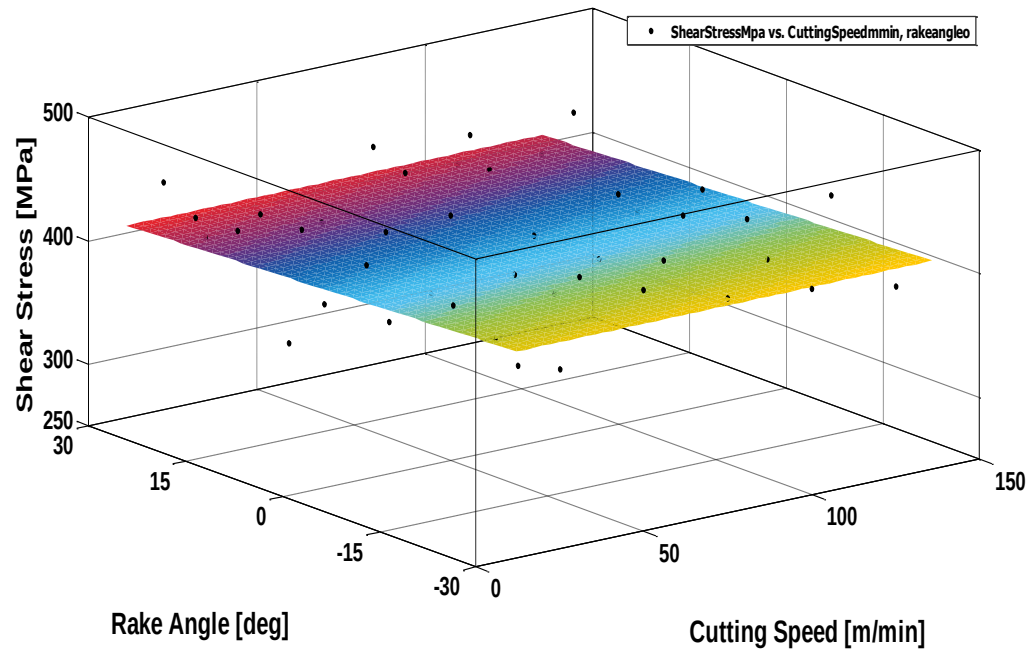


Figure A.7 : Regression diagram for shear stress of $h=0.2\text{ mm}$

$$\phi_{nh=0.2} = 24.23 + 0.25733\alpha_n + 0.0284$$

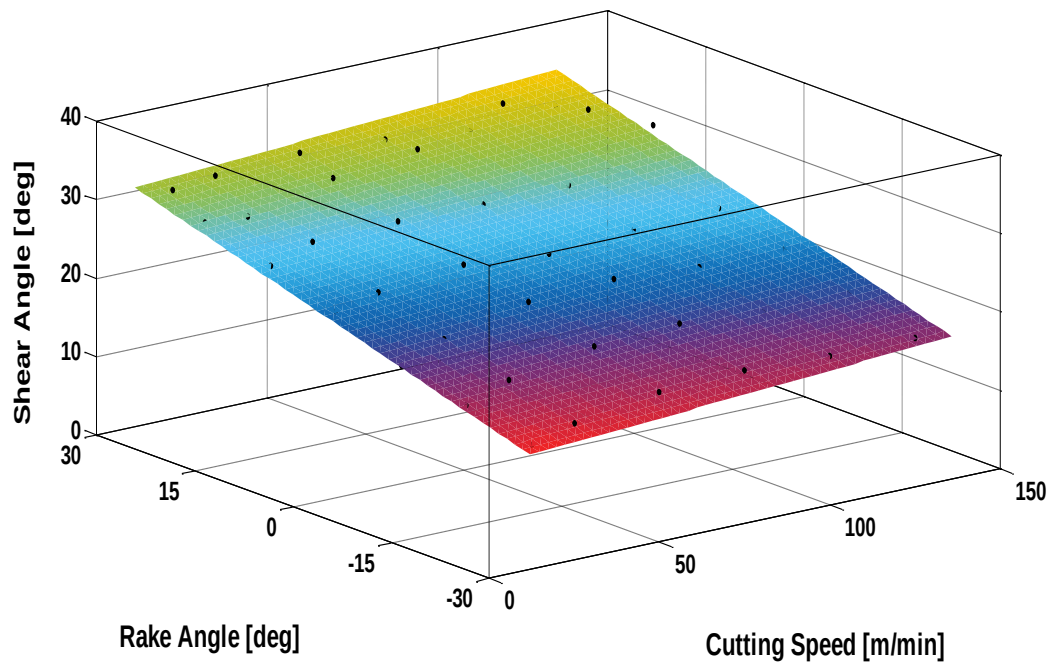


Figure A.8 : Regression diagram for shear angle of $h=0.2\text{ mm}$

$$\beta_{n_{h=0.2}} = 28.08 + 0.4777\alpha_n - 0.0092V_c$$

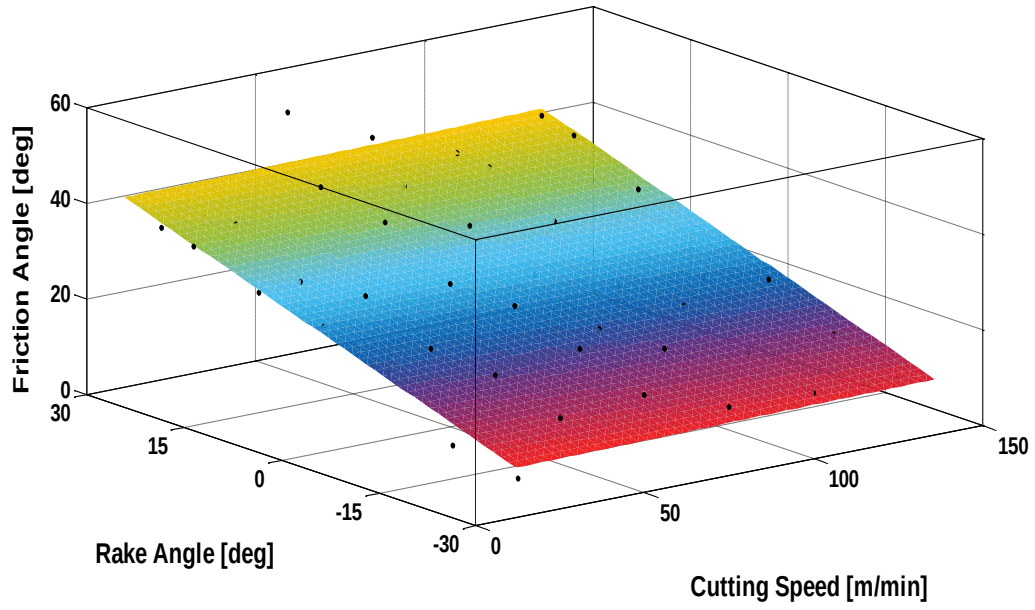


Figure A.9 : Regression diagram for friction angle of $h=0.2$ mm

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