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Modelling and experimental validation on drilling delamination of aramid fiber reinforced plastic composites

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Abstract

Aramid fiber reinforced plastic (AFRP) composites have been widely used in aerospace, military, and automotive industries. The common drilling process deployed for AFRP manufacturing can induce delamination that drastically deteriorate the mechanical performance and fatigue lives of the drilled AFRP components, therefore, establishing an accurate delamination model is desirable for delamination suppression and hole quality optimization. However, existing delamination models sum up all loads of the chisel edge and cutting lips act on the uncut plies under the chisel edge algebraically, which does not represent the true contact conditions. In this study, a new delamination regime is proposed where delamination caused by thrust forces exerted by both the chisel edge and cutting lips have been considered. On this basis, a novel analytical model in the context of AFRP drilling is proposed for the critical thrust force (CTF) prediction. Double cantilever beam (DCB) and delamination tests have been performed to validate the new model and results show that our proposed model agrees

Nomenclature

a_C	mode A delamination radius
a_L	mode B delamination radius
b	inner radius of APVT (mm)
CTF^A	total CTF of mode A delamination (N)
CTF_{ce}^A	critical thrust force component exerted by the chisel edge (N)
CTF^B	total CTF of mode B delamination (N)
CTF_{cl}^B	critical thrust force component exerted by the cutting lips (N)
d	Euclidean distance
D	bending stiffness of UPCE (N·mm)
D'	bending stiffness of APVT (N·mm)
D''	bending stiffness of the APVT inner edge (N·mm)
D_{ij}	coefficients of the bending stiffness matrix (N·mm)
E_{ij}	Young's modulus (MPa)
F	total thrust force (N)
F_{ce}	thrust force component exerted by the chisel edge (N)
F_{cl}	thrust force component exerted by the cutting lips (N)
G_{IC}	mode I interlaminar fracture toughness (J/mm ²)
G_{ij}	shear modulus (MPa)
h	thickness of UPCE (mm)
h_0	thickness of the APVT inner edge (mm)
h_1	thickness of the APVT outer edge (mm)
M	bending moment (N·mm)
M_{L1}	bending moment along the APET inner edge (N·mm)
M_{L2}	bending moment along the APET outer edge (N·mm)
q	load per unit area (N/mm)
R	outer radius of APVT (mm)
U	stored strain energy (N·mm)
U_C	stored strain energy of UPCE (N·mm)
U_d	strain energy absorbed due to delamination (N·mm)
U_{dC}	strain energy absorbed due to mode A delamination (N·mm)
U_{dL}	strain energy absorbed due to mode B delamination (N·mm)
U_L	total stored strain energy of UPCL (N·mm)
U_{L1}	stored strain energy of APVT (N·mm)
U_{L2}	stored strain energy of APET (N·mm)
ν_{ij}	Poisson's ratio
W	work done (N·mm)
W_C	work done by CTF^A (N·mm)
W_L	work done by q (N·mm)
γ	chisel edge ratio
ϕ_{L1}	slope of the deflection surface of APVT
ϕ_{L2}	slope of the deflection surface of APET
ω_C	deflection of UPCE (mm)
ω_{L1}	deflection of APVT (mm)

highly with the experimental results where the thrust force exerted by the chisel edge accounts for 24% of the total load during drilling of AFRP.

Keywords

AFRP composites, Drilling, Delamination, Critical thrust force, Analytical model

1. Introduction

With their excellent specific strength and toughness, aramid fiber reinforced plastic (AFRP) composites have been widely used in aerospace, military, and automotive industries [1]. To date, drilling is still considered as the most pivotal process in manufacturing/assembly of AFRP components due to its high efficiency [2]. However, the multiphase / anisotropic structure of AFRP often leads to undesirable manufacturing defects such as fuzzing, tearing, delamination and thermal degradation [3]. Delamination is one of the most critical defects which can drastically deteriorate the mechanical performance of the drilled components, particularly their fatigue lives, hence limiting their further applications.

With the aim of minimizing drilling damage in AFRP, some researchers have studied AFRP drilling parameters optimization and hole quality control. For instance, Won and Dharan [4] compared the hole quality of AFRP and carbon fiber reinforced plastic (CFRP) where the holes were produced under their respective optimal cutting parameters using the same twist drills. They found that while the machined surface of CFRP laminates was relatively smooth, the AFRP workpieces exhibited fuzzy machined surfaces with a large degree of fiber pull-out and crushed aramid fiber bundles. Bhattacharyya and Horrigan [5] investigated the influence of temperature on

the drilling thrust force and found that greater thrust force is required under cryogenic condition as compared to room temperatures. Shuaib et al. [6] analysed the influences of laminates preparation parameters and drilling conditions on the specific cutting energy, thrust force, and torque during drilling of plain weave AFRP when coated twist drills were used. They concluded that the thrust force and torque increase with increasing cutting speed, feed rate, and laminated thickness. In addition, researchers have investigated the influence of drill bit geometry and drilling parameters (e.g., feed rate f , spindle speed S) on the optimization of thrust force and hole quality. Table 1 provides a further summary on recent research in drilling of AFRP.

While the research into AFRP drilling delamination (especially the development of theoretical delamination models) is still in its infancy, delamination mechanisms of other fiber reinforced plastic (FRP) systems are reasonably well established in the field. Hocheng et al. [14] investigated two different delamination mechanisms in the drilling process of CFRP. It is found that when the tool drills into a laminate, delamination at the entrance plane is induced by the peeling action of the cutting tool. Under the continuous cutting motion at the hole exit, delamination occurs when the thrust force exceeds the interlaminar bonding strength of the laminate. On this basis, Capello [15] analyzed the difference in delamination mechanisms between drilling glass fiber reinforced plastic (GFRP) with and without a supporting plate. The study suggests that the cutting action of the drill point turns into punching action when the feed speed reaches a threshold value. Therefore, delamination is a consequence of overload on the peripheral parts of the cutting edge. The location of delamination defects has been also

investigated [16-19]. Wang et al., through C-scanning and SEM analysis, [18] suggests that the presence of inter-laminates delamination in drilling of CFRP depends on the drilling parameters and the cutting capability of the tools. In order to analyze the origin of crack initiation, Girot et al. [19] compared the machining morphologies and reported that delamination crack initiates at the drill corner as the drill bit exits the work piece.

To restrain delamination damage, it is important to predict the thrust force at the onset of delamination and control the thrust force during the drilling process [20, 21]. For this purpose, some workers have developed analytical models for delamination in drilling of CFRP. The first critical thrust force (CTF) model was presented by Ho-cheng and Dharan [14]. By assuming isotropy of the material and application of point load, this model gives a conservative prediction of the CTF. On this basis, Zhang et al. [22] took into account the material anisotropy and the coupling between tension and bending during drilling of CFRP, and established the delamination model for unidirectional laminates with an elliptical crack. Lachaud et al. [23] made the hypotheses that the thrust force exerted by a twist drill can be described by the uniformly distributed load and the point load at the center of the laminate, and established analytical models for both loads. In addition, CTF prediction have also been carried out for new modes of loading (such as triangular loading, combined loading) [19, 24], as well as the mixed mode energy release rate G_C [19, 25], and the thermo-mechanical formulation [26]. Table 2 summarizes the exiting CTF models at onset of delamination in drilling of CFRP.

Existing literature on delamination mechanism analysis and modelling are based on twist drill. Despite its simple geometry, the analytical research on twist drill delamination mechanism can help to inform the design of special drill geometries for better drilling quality. However, most existing models considered the thrust force exerted by the cutting lips as uniformly distributed load along the X-Y plane under the chisel edge, and neglected the effect of the uncut material in contact with the cutting lips during delamination. In addition, few studies have considered the role of thrust force on the actual tool-material interaction mechanisms in the context of AFRP drilling and their associated delamination model. In view of the limitations in the existing research, two modes of delamination (caused by thrust forces exerted by the chisel edge and cutting lips, respectively) have been investigated in this work, and a novel analytical model for the CTF prediction based on both the chisel edge delamination and the drill corner delamination is proposed. The proposed model has also been verified by the experimental results from double cantilever beam (DCB) and delamination tests in AFRP drilling.

2 Proposal of new analytical model

2.1 Traditional delamination regime

Fig. 1 illustrates the traditional delamination regime that can be generalized for several existing FRP drilling studies [14, 19-28]. Because the thickness of uncut plies decreases during cutting, the underneath uncut plies may experience deflection under the thrust force. When the thrust force exceeds the interlaminar bonding strength, delamination damage with radius a will occur. In the existing CTF prediction

models, the interaction between the drill and the uncut materials are mainly modeled by a point load F_1 at the center of the uncut material [14, 21, 22], uniformly distributed load F_2 along the drill radius R [20, 23], and/or the combination of the two [19, 24, 29]. However, the traditional delamination regime sums up all loads (exerted by the chisel edge and the cutting lips) act on the uncut plies under the chisel edge (UPCE) algebraically, which does not represent the true contact conditions.

2.2 New proposed delamination regime

2.2.1 The actual drill/material contact

Fig. 2 shows the actual drill/laminate contact in drilling of AFRP. Based on the law of superposition of the linear elastic regime, the total thrust force F can be described by Eq. (1):

$$F = F_{ce} + F_{cl} \quad (1)$$

where F_{ce} and F_{cl} are the force components of the thrust force exerted by the chisel edge and the cutting lips, respectively. Two different interaction regions and mechanisms of thrust force can be observed. In region 1, the UPCE is subject to F_{ce} . In region 2, the uncut material under the cutting lips (UPCL) is subject to F_{cl} . The different geometrical characteristics of the two regions can result in significantly different machining mechanisms and modes of delamination, which have not been discussed by previous studies.

2.2.2 Mode A delamination

In this section, a new delamination regime is proposed to analyze the two modes of delamination in their respective interaction region. Based on the classic plate bending theory [30], UPCE and UPCL can be considered as circular plates constrained within

the laminate. Considering the tool geometry of the twist drill, the thrust force exerted by the chisel edge is assumed as a point load applied at the center of the UPCE, whereas the thrust force exerted by the cutting lips can be described as a uniformly distributed load on the UPCL. When the thrust force reaches a critical value, interfacial debonding occurs between the plies due to the low interfacial bonding strength. As a result, the delamination cracks will be initiated under two modes of delamination respectively, and they will propagate in their respective weakest planes between the plies. The planes of the crack propagation under two modes of delamination are parallel to each other. Therefore, it is assumed that the initiation and propagation of the two modes of delamination are independent.

Fig. 3 shows the proposed mechanism for mode A delamination caused by F_{ce} . The UPCE can be modeled as a circular plate. In order to define the contribution of the thrust force component, the chisel edge ratio γ is defined as the ratio of F_{ce} and F :

$$\gamma = \frac{F_{ce}}{F}, (0 < \gamma < 1) \quad (2)$$

The bending stiffness of the UPCE decreases as the cutting progresses, which makes the uncut material more prone to delamination. When F_{ce} exceeds the critical force component of the thrust force exerted by the chisel edge (CTF_{ce}^A), mode A delamination will occur and propagate with a radius a_c . Hence the total CTF at the onset of mode A delamination (CTF^A) can be described as:

$$CTF^A = \frac{CTF_{ce}^A}{\gamma} \quad (3)$$

2.2.3 Mode B delamination

On the other hand, the proposed mechanism for mode B delamination (caused by

F_{cl}) can be described by Fig. 4. The UPCL can be modeled as an annular plate with varying thickness. The material is mainly removed by the drill cutting lips and the support from the UPCE gradually decreases with their reducing thickness (hence reduced bending stiffness). Mode B delamination under action of F_{cl} , depends on the interlaminar bonding strength in regions adjacent to the drill corner. When F_{cl} exceeds the critical force component of the thrust force exerted by the cutting lips (CTF_{cl}^B), mode B delamination will initiate and propagate with radius a_L . The total CTF at the onset of mode B delamination, CTF^B , can be described as follows:

$$CTF^B = \frac{CTF_{cl}^B}{1 - \gamma}$$

(4)

2.3 Total CTF of mode A delamination, CTF^A

Based on linear elastic fracture mechanics (LEFM) and the virtual work theorem [14], the energy conservation follows:

$$\delta U_d = \delta W - \delta U \quad (5)$$

where δU_d is the energy absorbed due to delamination propagation, δW is the virtual work done by the thrust force, δU is the infinitesimal strain energy.

As illustrated in Fig. 5, when subjected to CTF_{ce}^A , the deflection of UPCE in the case of mode A delamination is given by [30]:

$$w_c = \frac{CTF_{ce}^A}{16\pi D} \left[2r^2 \ln\left(\frac{r}{a_c}\right) + (a_c^2 - r^2) \right] \quad (6)$$

where

$$D = \frac{1}{8} (3D_{11} + 2D_{12} + 4D_{66} + 3D_{22}) \quad (7)$$

Coefficients D_{ij} of the bending stiffness matrix can be described as follows [31]:

$$D_{11} = \frac{E_{11}h^3}{12(1-\nu_{12}\nu_{21})} \quad D_{22} = \frac{E_{22}h^3}{12(1-\nu_{12}\nu_{21})} \quad D_{12} = \frac{\nu_{12}E_{22}h^3}{12(1-\nu_{12}\nu_{21})} \quad D_{66} = \frac{G_{12}h^3}{12} \quad (8)$$

where E_{ij} and G_{ij} are the elastic modulus of the AFRP laminate, ν_{ij} is the Poisson ratio, h is the thickness of UPCE.

The strain energy under CTF_{ce}^A follows [20]:

$$U_C = \pi D \int_0^{a_c} \left[\left(\frac{d^2 \omega_C}{dr^2} + \frac{1}{r} \frac{d \omega_C}{dr} \right)^2 - 2(1-\nu) \left(\frac{d^2 \omega_C}{dr^2} \times \frac{1}{r} \frac{d \omega_C}{dr} \right) \right] r dr \Rightarrow \delta U_C = \frac{(CTF_{ce}^A)^2 a_c}{16\pi D} \delta a_c \quad (9)$$

The work done by CTF_{ce}^A is:

$$W_C = \frac{(CTF_{ce}^A)^2 a_c^2}{16\pi D} \Rightarrow \delta W_C = \frac{(CTF_{ce}^A)^2 a_c}{8\pi D} \delta a_c \quad (10)$$

The strain energy absorbed during the crack propagation follows:

$$U_{dC} = G_{IC} \pi a_c^2 \Rightarrow \delta U_{dC} = G_{IC} 2\pi a_c \delta a_c \quad (11)$$

where G_{IC} is the mode I interlaminar fracture toughness. Substituting Eqs. (9)-(11)

into Eq. (5), CTF_{ce}^A can be obtained:

$$CTF_{ce}^A = 4\pi \sqrt{2G_{IC}D} \quad (12)$$

The total CTF for mode A delamination can be described as:

$$CTF^A = \frac{4\pi \sqrt{2G_{IC}D}}{\gamma} \quad (13)$$

2.4 Total CTF of mode B delamination, CTF^B

As illustrated in Fig. 6, the UPCL can be further decomposed into an annular plate with linearly varying thickness (APVT) and an annular plate with equal thickness (APET).

2.4.1 Annular plate with linearly varying thickness (APVT)

The analytical model of the APVT is shown in Fig. 7(a). When the plate is subject to F_{cl} , load per unit area, q , is uniformly distributed along the contact surface between the cutting lips and uncut material. Fig. 7(b) illustrates the geometry of the twist drill used in this study. h_0 is defined as the height of the chisel edge, and b is the chisel edge radius. Because the uncut material is machined by the cutting lips during drilling, it is assumed that the dimensions of the top surface of APVT complies with the geometry of the twist drill. Therefore, b and h_0 equal to the inner radius and the thickness of APVT, respectively (see green dashed lines in Fig. 7(a)), and R and h_1 equal to the outer radius and the thickness of APVT, respectively.

The equilibrium equation for an element of APVT can be described as:

$$D' \frac{d}{dr} \left(\frac{d\varphi}{dr} + \frac{\varphi}{r} \right) + \frac{dD'}{dr} \left(\frac{d\varphi}{dr} + \nu \frac{\varphi}{r} \right) = -\frac{1}{r} \int_b^r q r dr \quad (14)$$

where φ is the slope of the deflection surface of the APVT. Due to the varying thickness, the bending stiffness D' of APVT varies with radical distance r and it can be simplified as [30]:

$$D' = \frac{D'' r^3}{b^3}, \quad b \leq r \leq R \quad (15)$$

The bending stiffness of the APVT inner edge, D'' can be expressed as:

$$D'' = \frac{1}{8} (3D''_{11} + 2D''_{12} + 4D''_{66} + 3D''_{22}) \quad (16)$$

where D''_{ij} are the coefficients of the bending stiffness matrix at $r = b$.

Substituting Eq. (15) into Eq. (14), the following differential equation can be obtained:

$$\frac{D'' r^3}{b^3} \frac{d}{dr} \left(\frac{d\varphi}{dr} + \frac{\varphi}{r} \right) + \frac{3D'' r^2}{b^3} \left(\frac{d\varphi}{dr} + \nu \frac{\varphi}{r} \right) = -\frac{1}{r} \int_b^r q r dr \quad (17)$$

By using the Euler equation, the slope of the deflection surface of APVT can be obtained as the general solution of the equation:

$$\varphi_{L1} = Ar^{K_1} + Br^{K_2} + qK_3 - \frac{qK_4}{r^2} \quad (18)$$

where

$$\begin{cases} K_1 = \sqrt{3.25 - 3\nu} - 1.5 \\ K_2 = -\sqrt{3.25 - 3\nu} - 1.5 \\ K_3 = \frac{b^3}{2D'(1 - 3\nu)} \\ K_4 = \frac{b^5}{6D'(1 - \nu)} \end{cases} \quad (19)$$

By integrating Eq. (18), the deflection of the APVT subject to q can be described as:

$$\omega_{L1} = -\frac{Ar^{K_1+1}}{K_1+1} - \frac{Br^{K_2+1}}{K_2+1} - K_3qr - \frac{K_4q}{r} + C \quad (20)$$

where A , B , C are the integration constants. For the APVT, the strain energy under q can be obtained:

$$U_{L1} = \pi \frac{D''}{b^3} \int_b^R \left[\left(\frac{d^2\omega_{L1}}{dr^2} + \frac{1}{r} \frac{d\omega_{L1}}{dr} \right)^2 - 2(1-\nu) \left(\frac{d^2\omega_{L1}}{dr^2} \times \frac{1}{r} \frac{d\omega_{L1}}{dr} \right) \right] r^4 dr \quad (21)$$

2.4.2 Annular plate with equal thickness (APET)

As shown in Fig. 8, the APET is bent due to the uniformly distributed moment M_{L1} originated from q . Furthermore, since APET is constrained, it will be subject to the uniform bending moments M_{L2} along the outer edge.

In this case, the bending moments acting on the circumferential sections of the UPCL can be expressed as [30]:

$$M = D' \left(\frac{\partial \varphi}{\partial r} + \frac{\nu}{r} \varphi \right) \quad (22)$$

Under such a condition, the slope of the deflection surface of APET can be written as:

$$\varphi_{L2} = \frac{A'r}{2} + \frac{B'}{r}, \quad R \leq r \leq a_L \quad (23)$$

where R and a_L can be described by the following relationship:

$$s = R / a_L \quad (24)$$

By integrating Eq. (23), the deflection of the APET can be obtained:

$$\omega_{L2} = -\frac{A'r^2}{4} - B' \ln \frac{r}{a_L} + C' \quad (25)$$

where A' , B' , C' are integration constants. For the APET, the strain energy can be calculated as follows:

$$U_{L2} = \pi \frac{D'R^3}{b^3} \int_R^{a_L} \left[\left(\frac{d^2 \omega_{L2}}{dr^2} + \frac{1}{r} \frac{d\omega_{L2}}{dr} \right)^2 - 2(1-\nu) \left(\frac{d^2 \omega_{L2}}{dr^2} \times \frac{1}{r} \frac{d\omega_{L2}}{dr} \right) \right] r dr \quad (26)$$

2.4.3 Determination of integration constants

Conditions for solving the six integration constants (A , B , C , A' , B' , C') at the boundaries ($r = b$ and $r = a_L$), and the interface ($r = R$) are shown in Fig. 9.

The slope and the deflection of APET at $r = a_L$ is zero. Moreover, considering the constraint at the contact point between the chisel edge and the cutting lips, $r = b$, it can be assumed that the deflection of the APVT ω_{L1} equals that of the deflection of the circular plate ω_C . Therefore, the conditions at $r = a_L$ and $r = b$ can be described as:

$$\begin{cases} r = a_L : \varphi_{L2} = 0 & \omega_{L2} = 0 \\ r = b : \omega_{L1} - \omega_C = 0 \end{cases} \quad (27)$$

where

$$\varphi_{L2}|_{r=a_L} = \frac{A'a_L}{2} + \frac{B'}{a_L} = 0 \quad (28)$$

$$\omega_{L2}|_{r=a_L} = -\frac{A'a_L^2}{4} + C' \quad (29)$$

$$\begin{aligned} \omega_{L1} - \omega_C|_{r=b} = & -\frac{Ab^{K_1+1}}{K_1+1} - \frac{Bb^{K_2+1}}{K_2+1} - K_3qb - \frac{K_4q}{b} + C \\ & - \frac{\gamma}{1-\gamma} \cdot \frac{q(R^2 - b^2)}{16D''} \left[2b^2 \ln\left(\frac{b}{R}\right) + (R^2 - b^2) \right] \end{aligned} \quad (30)$$

Furthermore, APVT and APET have the same moment, slope, and deflection when $r = R$. Therefore, the conditions at $r = R$ can be written as:

$$r = R : M_{L1} - M_{L2} = 0 \quad \varphi_{L1} - \varphi_{L2} = 0 \quad \omega_{L1} - \omega_{L2} = 0 \quad (31)$$

where

$$\begin{aligned} M_{L1} - M_{L2}|_{r=R} = & AK_1R^{K_1-1} + BK_2R^{K_2-1} + \frac{2qK_4}{R^3} + \frac{\nu}{R} \left(AR^{K_1} + BR^{K_2} + qK_3 - \frac{qK_4}{R^2} \right) \\ & - \frac{A'}{2}(1+\nu) + \frac{B'}{R^2}(1-\nu) = 0 \end{aligned} \quad (32)$$

$$\varphi_{L1} - \varphi_{L2}|_{r=R} = AR^{K_1} + BR^{K_2} + qK_3 - \frac{qK_4}{R^2} - \left(\frac{A'R}{2} + \frac{B'}{R} \right) = 0 \quad (33)$$

$$\omega_{L1} - \omega_{L2}|_{r=R} = -\frac{AR^{K_1+1}}{K_1+1} - \frac{BR^{K_2+1}}{K_2+1} - K_3qR - \frac{K_4q}{R} + C + \frac{A'R^2}{4} + B' \ln \frac{R}{a} - C' = 0 \quad (34)$$

2.3.4 Calculation of CTF^B

Combining Eqs. (28)-(30), and (32)-(34), six integration constants can be obtained. In this work, Matlab was used for the calculation. The total strain energy U_L of the UPCL can be obtained using the superposition principle of the linear elastic regime:

$$\delta U_L = \frac{\partial (U_{L1} + U_{L2})}{\partial a_L} \delta a_L \quad (35)$$

The work done by q is:

$$W_L = \int_0^{2\pi} \int_b^R q \omega_{L1} r dr d\theta \Rightarrow \delta W_L = \frac{\partial W_L}{\partial a_L} \delta a_L \quad (36)$$

The strain energy absorbed by the crack propagation during mode B delamination can be described as:

$$U_{dL} = G_{IC} \pi (a_L^2 - R^2) \Rightarrow \delta U_{dL} = G_{IC} 2\pi a_L \delta a_L \quad (37)$$

Substituting Eq. (35)-(37) into Eq. (5), a detailed q value can be obtained through Matlab calculation.

Therefore, the CTF_{cl}^B can be obtained:

$$CTF_{cl}^B = q\pi(R^2 - b^2) \quad (38)$$

The total CTF of mode B delamination CTF^B is as follows:

$$CTF^B = \frac{q\pi(R^2 - b^2)}{1 - \gamma} \quad (39)$$

3 Experimental setup

3.1 Fabrication of AFRP laminates

AFRP laminates were made of K-29 high strength aramid fibers (supplied by DuPont) impregnated with epoxy resin having 60% fiber volume fractions. K-29 has been used for structural aircraft components, personal body armors, and ballistic head protections. Laminates with two different thicknesses (15 and 8 plies, respectively), were used for the delamination test and DCB test, respectively. The thickness of each ply of the laminate is 0.27 mm. All laminates were first cured in an autoclave at 80°C for 30 min, and then at 120°C for 120 min. The mechanical properties of the woven AFRP composites are listed in Table 3.

The workpiece used in this study were produced by water jet cutting to achieve the

desired dimensional accuracy and machining quality [32-33]. The influence of water transport on the AFRP laminate surface has been studied in detail in the literature [34]. In this study, the traverse speed during the water jet cutting was 400 mm/min and corresponding area for moisture absorption during the water jet cutting is negligible. The surface of the cut workpiece was wiped dry after cutting, and the workpiece were further dried under the room temperature for 48 h before further testing.

3.2 Mode I interlaminar fracture toughness, G_{IC}

In order to obtain G_{IC} of the AFRP, DCB tests were conducted in accordance with ASTM D5528. The experimental setup and the dimensions of the specimens are shown in Fig. 10. The test was carried out on a universal testing machine (AGS-X-50KND, Shimadzu) at a constant loading speed of 1 mm/min. The initial crack is prefabricated in the middle of the laminate end using a 20 μ m thick, and 50 mm long Teflon film. To observe the progresses of the crack growth during testing, the specimen was marked at 5 mm intervals from the tip of the Teflon film, see Fig 10. The average G_{IC} value (based on five tests) is 295.95 ± 32.71 J/m².

3.3 Drilling experiments

Fig. 11 shows the drilling experimental setup. The experiments were carried out on a XK714D computer numerical control (CNC) machine tool under dry condition. Blind holes were drilled on $160 \times 100 \times 4.1$ mm AFRP laminates with back support at the feed rate of 20 mm/min and spindle speed of 1000 rpm, and the number of the uncut plies remain under the chisel edge varies from 1 to 3. Uncoated solid carbide twist drills were used in this study. The geometric parameters of the tool are summarized in Table 4. In the preliminary research, no edge chipping occurred after drilling 3 holes.

However, after drilling 5 holes, edge chipping and severe adhesive wear have been found on the cutting lip and the flank face, respectively. In order to minimize the effects of tool wear on the thrust force, tool bit was replaced after drilling every three holes in this study.

The delamination tests were performed after drilling, at a feed rate of 5 mm/min on the same experiment platform without back support. The relatively low feed velocity was chosen as it allows the dynamometer to better capture the dynamic variation of the thrust force during the tests and hence provides a more accurate data acquisition. Each experiment with the same number of UPCE was repeated three times. During the delamination tests, the dynamic thrust forces were measured by a dynamometer (9257B, Kistler), which was connected to a 5070A charge amplifier and a data acquisition card. The data were processed by the program Dynoware.

4 Model validation and discussion

4.1 Experimental analysis of CTFs

Fig. 12 illustrates a typical thrust force curve recorded during a delamination test with 3 UPCE. It can be seen that with the application of the thrust force, the curve shows a linear increase up to point A, which represents the total CTF of mode A delamination [29]. When mode A delamination propagates, there is no material to support the UPCE to restrain the delamination propagation, hence the thrust force gradually decreases towards the end of the test.

In addition, several sudden drops of the thrust force (such as point B) occurred during the linear increase in the thrust force curve. Drill corner is the outermost point of the

cutting lips, which is in contact and shapes the machined hole wall during drilling. Past literature suggests that the delamination crack originates from the drill corner, not at the web (chisel edge) diameter of the drill [19, 35]. Fig. 13 gives the schematic illustration of the drill / material contact and stress states of model B delamination. As is shown in Fig. 13(a), under action of load q (generated by the cutting edge of drill corner), the aramid fiber of UPCL experiences elastic deformation. The constraint of the fiber to resist deformation is mainly dependent on the interfacial bonding strength between the fiber and the unmachined region adjacent to the drill corner. Once the thrust force exceeds the critical value, interfacial fracture will occur and crack will immediately propagate to the unmachined region, leading to the onset of delamination. Therefore, the first drop on the thrust force curve caused by the sudden loss of strength represents the total CTF of mode B delamination. As shown in Fig. 13(b), there are subsequent drops in the thrust force after the first drop. This phenomenon can be attributed by the resin rich areas in the AFRP laminates (Fig. 13(c)). Due to the weaker mechanical properties of the resin, the delamination crack is usually initiated and propagates in a resin rich area. Therefore, the subsequent drops on the thrust force curve can be related to the occurrence and propagation of the cracks in the resin rich areas in UPCL. The experimental results of the total CTFs are listed in Table 5. In the case of 1 layer of UPCE, only the total CTF of the mode A delamination is considered as the thrust forces linearly increase up to point A without sudden drops.

4.2 Determination of the chisel edge ratio

The chisel edge ratio (γ) is an important factor in establishing analytical model for

delamination. However, to date there has been no quantitative index defined in the literature to determine γ . In this paper, in order to accurately determine γ in drilling of AFRP, a quantitative index is proposed based on the degree of similarity between the experimental and theoretical values. As for mode A delamination with different number of UPCE ($i=1, 2, 3$), Euclidean distance is used to evaluate similarity between the average experimental data $CTF_i^{A(\text{exp})}$ and $CTF_i^{A(\text{pred})}$ with their corresponding theoretical values as a function of γ . Euclidean distance can be expressed as:

$$d = \sqrt{\sum_{i=1}^3 (CTF_i^{A(\text{exp})} - CTF_i^{A(\text{pred})})^2} \quad (40)$$

The smaller the Euclidean distance, the better the similarity, hence the greater accuracy of the model. As shown in Fig. 14, the best fitting is when $\gamma = 0.24$. Since the maximum relative deviation between modeling and experimental data is around 20% ($d = 245.1$) for $0.2 \leq \gamma \leq 0.3$, a reasonable level of fitting can also be achieved in this range. Therefore, we focused on this range ($0.2 \leq \gamma \leq 0.3$) in the subsequent analysis.

4.3 Delamination model analysis

Fig. 15 compares the predictive CTF component values in two different delamination scenarios. Based on Eq. (13), Fig. 15(a) shows that CTF_{ce}^A increases with increasing i as a result of the increasing bending stiffness of the UPCE. In particular, CTF_{ce}^A ($i=3$) is more than four times greater than that of CTF_{ce}^A ($i=1$). According to Eq. (39), the influence of γ on CTF_{cl}^B ($i=1, 2, 3$) is shown in Fig. 15(b). It can be seen that CTF_{cl}^B decreases with increasing γ . According to Eq. (2), the component F_{ce} will increase with γ under constant thrust force, and the deflection of the UPCE will also increase. In this situation, the supporting effect of the UPCE to the UPCL is

weakened, which leads to decreased CTF_{cl}^B . Moreover, it can be observed that the CTF_{cl}^B decreases with decreasing i due to the reduction in the thickness of UPCE and the reduction of its supporting ability. When $\gamma = 0.24$ (best point of fit in Fig. 14), the predictive value of CTF_{cl}^B dropped as i decreased from 3 to 1 (328.16 N to 309.85 N then drastically decreases to 198.85 N).

In order to analyze the occurrence of mode A and mode B delamination, the comparison of the total CTFs for the two different modes of delamination under the same drilling process is illustrated in Fig. 16. For selected values of γ (0.2, 0.24, 0.3), the following relationships apply:

$$\begin{cases} CTF^A < CTF^B, (i = 1) \\ CTF^A > CTF^B, (i = 2, 3) \end{cases} \quad (41)$$

It can be concluded that mode A delamination is more likely to occur as compared to mode B delamination for UPCE ($i=1$). When i exceeds 1, mode B delamination is more likely to occur. Models proposed in the past literatures usually attribute the hole-wall delamination to mode A delamination with large number of UPCE, hence they mostly underestimate the chance of the hole-wall delamination. The present model is more representative of the actual machining process where the hole-wall delamination is more likely to occur as mode B delamination.

4.4 Model validation and comparison

Fig. 17 shows the comparison of the experimental results and the predictive total CTF from mode A and mode B delamination as a function γ . It can be seen that the best fit is found for $\gamma = 0.24$, i.e., thrust force exerted by the chisel edge accounts for 24% of the total load. The contributions of the chisel edge and cutting lips have been investigated by several researchers in the context of CFRP drilling in the past. For

instance, the contribution of the chisel edge suggested by Zitoune and Collombet ranges from 40% to 50% [36]. In contrast, the chisel edge contribution in our study is significantly lower. The difference might be due to the different mechanical properties of the composites used (hence different γ). In addition, the twist drill used in this study has an inclining chisel edge, hence reducing the thrust force component of the chisel edge to a certain extent.

The comparison of different delamination models proposed in the literatures is shown in Fig. 18. As the existing models are based on the assumption that all the loads are exerted on the UPCE, only mode A delamination in the present work is used for comparison. It can be seen that the most conservative CTF prediction is by Ho-Cheng and Dharan's model where the assumptions were based on isotropic material and point load [14]. Although the anisotropy of the workpiece has been considered in Zhang's model [22], the predictive values of the CTF is still too low comparing to the experimental results. Lachaud's model [23] consists of two hypothetical loads, i.e., uniformly distributed load and point load. Although the distributed load model is better than the point load model, it still shows significant deviation from the experimental results. The comparison confirms that our proposed model best agrees with the experimental values.

5 Conclusions

Considering the role of thrust force on the actual drill-material interaction mechanisms, a novel analytical model is proposed in this study for more accurate CTF prediction in the context of AFRP drilling. New delamination regime was first proposed based on the actual chisel edge-material interaction mechanism and the actual cutting lips-material interaction mechanism and two modes of delamination (chisel edge

delamination and drill corner delamination) were both considered for establishing the new model. DCB and delamination tests have been carried out to validate the proposed analytical model. A quantitative index for the determination of the chisel edge ratio has been obtained by evaluating the degree of similarity between the experimental and the theoretical values. The results show that thrust force exerted by the chisel edge accounts for 24% of the total load in AFRP drilling. The comparison with existing delamination models confirms the proposed model agrees well with the experimental data. The mode of delamination during drilling of AFRP depends on the number of uncut plies under the chisel edge: mode A delamination is more likely to occur than the B delamination when there is 1 uncut ply under the chisel edge; In the case of 2 and 3 uncut plies, mode B delamination is more likely to be induced.

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Author statement

We certify that all authors have seen and approved the final version of the manuscript being submitted. We warrant that the article is the authors' original work, hasn't received prior publication and isn't under consideration for publication elsewhere.

37.

Conflict of interest

The authors declared that they have no conflicts of interest to this work.

38.

Figure Captions

Fig. 1. Schematic diagram of traditional delamination regime.

Fig. 2. Schematic diagram of the actual drill/material contact.

Fig. 3. Schematic diagram of mechanism describing mode A delamination.

Fig. 4. Schematic diagram of mechanism describing mode B delamination.

Fig. 5. Model of the uncut material under the chisel edge.

Fig. 6. Schematic diagram of the material in the region of mode B delamination.

Fig. 7. Schematic diagram of the APVT: (a) Model analysis based on the theory of classic plate bending, (b) Tool geometry of the drill bit used in this study.

Fig. 8. Schematic diagram of the APET.

Fig. 9. Schematic diagram of conditions at the boundaries and the interface.

Fig. 10. Experimental setup for DCB test.

Fig. 11. Experimental devices for delamination test.

Fig. 12. The thrust force curve of delamination test with 3 uncut plies under the chisel edge.

Fig. 13. Schematic diagram showing drill / material contact and stress states under model B delamination: (a) initiation of mode B delamination, (b) thrust force curve during delamination test, (c) delamination at the resin rich area.

Fig. 14. Determination of the chisel edge ratio.

Fig. 15. Comparison of the components of CTF under different conditions: (a) CTF components (exerted by the chisel edge) of mode A delamination, (b) CTF components (exerted by the cutting lips) of mode B delamination.

Fig. 16. Comparison of the total CTF of mode A and mode B delamination.

Fig. 17. Comparison of the CTFs between the predictive values and the experimental results: (a) The CTF of mode A delamination, (b) The CTF of mode B delamination.

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Table 1

Recent research on AFRP drilling.

Reference	Tool geometry	Drilling parameters	Main findings
Gao et al. [7]	Sawing drill and sawing-grinding drill ($d=30$ mm)	$f=5$ mm/min $S=10000$ rpm	Hole making process based on sawing and grinding
Zheng et al. [8]	Core drill (sintering diamond, $d=24$ mm)	f =Manual step feed $S=1270\text{--}3200$ rpm	Ceramics/AFRP stack composite drilling
Díaz-Álvarez et al. [9]	Twist drills and Brad & Spur drill (uncoated HSS, $d=6$ mm)	$f=30.5\text{--}584.0$ mm/min $S=1220\text{--}5840$ rpm	Prediction of damage extension
Bao et al. [10]	Twist drill (solid carbide, $d=6.5$ mm)	$f=2$ mm/min $S=1000\text{--}3000$ rpm	Model for the temperature distribution
Anarghya et al. [11]	Twist drill (solid carbide, $d=6\text{--}10$ mm)	$f=50\text{--}100$ mm/min $S=600\text{--}1200$ rpm	Model of the Neural Network optimization
Liu et al. [12]	Twist drill, burr tool, brad drill (uncoated solid carbide, $d=6$ mm)	$f=20\text{--}60$ mm/min $S=2000\text{--}6000$ rpm	Cutting mechanisms and hole quality optimization
Soepangkat et al. [13]	Type S and type X twist drill (HSS and HSS-Cobalt, $d=10$ mm)	$f=50\text{--}180$ mm/min $S=1500\text{--}2500$ rpm	Optimization of multi-performance characteristics

Table 2

Existing models of CTF at onset of delamination for CFRP drilling.

Reference	Material	Factors considered in the model	Drill /laminate interaction	Model
Ho-cheng and Dharan [14]	CFRP	Isotropic circular plate	Point load	$F_{HD} = \pi \sqrt{\frac{8G_{IC}Eh^3}{3(1-\nu^2)}}$
Jain and Yang [27]	Unidirectional CFRP	Anisotropic elliptical plate	Point load	$F_{JY} = \frac{3\pi b}{a} \sqrt{2G_{IC}D^*}$
Zhang et al. [22]	Multidirectional CFRP	Orthotropic elliptical plate	Point load	$F_Z = \sqrt{\frac{\pi G_{IC}}{\xi(C_3 - K)}}$
Gururaja and Ramulu [28]	Multidirectional CFRP	Orthotropic elliptical plate	Uniformly distributed load	$F_{GR} = \sqrt{\frac{\pi G_{IC}}{\xi(c_3/3 - k)}}$
Lachaud et al. [23]	Multidirectional CFRP	Orthotropic circular plate	i) Uniformly distributed load ii) Point load	$\text{i) } F_{LD} = 8\pi \sqrt{\frac{G_{IC}D}{1/3 - (D'/8D)}}$ $\text{ii) } F_{LP} = \pi \sqrt{\frac{8G_{IC}Eh^3}{3(1-\nu^2)}}$
Rahmé et al. [24]	Multidirectional CFRP	Orthotropic circular plate	Uniformly distributed load with additional point load	$F_R = 16\pi \sqrt{\frac{6G_{IC}D}{13}}$
Girod et al. [19]	Multidirectional CFRP	Orthotropic circular plate; Mixed mode energy release rate	Triangular load with additional point load	$F_G = \frac{22/21\pi\alpha Aa^3 \pm \sqrt{\Delta}}{2[12(1-\alpha^2) + \alpha^2]}$
Ismail et al. [26]	Cross-ply CFRP	Orthotropic elliptical plate; Thermo-mechanical formulation	Uniformly distributed load with additional point load	$F_I = \frac{12\pi}{\eta} \sqrt{\frac{2D^*(D' + G_{IC})}{(16\gamma^2 + 2(1-\gamma)^2)}}$
Karimi et al. [20]	Multidirectional CFRP	Orthotropic circular plate; Critical feed rate	Uniformly distributed loads on cutting lips and chisel edge	$F_K = \frac{\pi\sqrt{32G_{IC}D'}}{\sqrt{J^2(1-0.5S^2)^2 + (1-J)^2(1-0.5\tau^2s^2)^2}}$

Table 3

Properties of AFRP composite laminates [9].

E_{11} (MPa)	E_{22} (MPa)	G_{12} (MPa)	ν_{12}
18000	18000	770	0.25

Table 4

Geometric parameters of the twist drill.

R (mm)	3
2ϕ	135°	
h_0 (mm)	0.15	
b (mm)	0.54	

Table 5

CTFs under different conditions.

Number of UPCE	Mode A delamination (Point A)			Mode B delamination (Point B)	
	1	2	3	2	3
Test 1 (N)	197.63	569.18	1038.57	371.70	410.00
Test 2 (N)	208.95	519.41	1122.10	447.96	446.44
Test 3 (N)	235.38	662.35	1059.51	377.77	436.31
Average values (N)	213.99	583.65	1073.39	399.14	430.92
Standard deviation	19.37	72.56	43.46	42.38	18.81