



Article

Crown-Tulip Trigger Mechanisms to Improve Crashworthiness Design of Composite Tubular Structures

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Abstract

Background: This article presents the design development of a new crown-tulip trigger mechanism to initiate progressive failure and reduce initial collapse load in comparison with the existing trigger designs of bevel and tulip in tubular composite structures. **Objectives:** Through experimental impact testing, comparisons are drawn to the existing designs, such as the 45° bevel and 4T90° tulip trigger mechanism. **Methods:** This experimental testing design phase demonstrated a significant improvement in the crush force efficiency of crown-tulip trigger mechanisms compared to the previously established Tulip trigger design (4T90°). The experimental results were utilised to develop equivalent numerical models in LS-DYNA. **Results:** The validated models were employed for further design development, studying the influence of increased bevel angles (30°, 45°, and 60°), tulip angles (90°, 100°, 120°, 140°, and 160°), crown notch depth, crown notch angle, and number of tulip tips/crown notches on the crashworthiness and force response. **Conclusions:** This culminated in the numerical design development of the 4T160°-40°-2 mm crown-tulip trigger, which achieved 20% higher specific energy absorption, a 22% increase in crush force efficiency, and a 36% higher mean force compared with the 4T90° Tulip-triggered specimen. The outcomes of this research will be implemented in automotive, aerospace, and defence sub-structures.



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1. Introduction

A key aspect of lightweight structure design is considering impact and crash scenarios. These complex loadings require tailoring the structure's strength, stability, and integration. This enables ideal impact response, which is characterised by crashworthiness. This refers to the energy absorption capability and resistance to impact of a structure, defining the shape of the impact force–displacement response. By carefully regulating the impact force in terms of impulse and deformation, the crashworthiness of the design is improved. This is critical in regulating the occupant's accelerations, which can be fatal if they exceed the limits defined by the Head Injury Criteria (HIC). This design philosophy applies to the automotive sector, where improved minimum safety standards and a continuous increase in vehicle mass have further elevated the importance of crash structure efficiency, specifically the specific energy absorption (SEA). Carbon epoxy composite structures, due to their higher specific strength and stiffness, exhibit improved SEA compared to equivalent metal (aluminium/steel) or metal/composite (aluminium/carbon) hybrid structures. Furthermore, the use of composites enables the tailoring of impact crash behaviour based on the

crash structure geometry, manufacturing process, laminate design, and improvement of the inter-/intra-laminate interface strength and fracture toughness. These factors have been studied by several authors for the quasi-static crushing and impact of composite structures, concluding that tubular carbon epoxy structures have the highest SEA under dynamic impact loadings, for a controlled ratio of the thickness (t) to diameter (D), $0.015 < \frac{t}{d} < 0.25$, representing the limit of buckling. To achieve the highest energy absorption state in composite structures, the progressive failure mode of laminar bending or splaying with brittle fragmentation must occur. This mode comprises several energy absorption mechanisms, including mode-I delamination peeling, mode-II delamination originating from laminar bending and splaying, axial splitting of fronds, fracture of layers into debris wedges, inter-wall cracks, and frictional interactions. This desired progressive failure mode requires an initial weakening of the structure, serving as an initiation point for the desired progressive failure. This is defined as the trigger mechanisms and is the main study of this article [1].

The machining of the leading edge to form a bevel or chamfer has been established as the simplest trigger mechanism that achieves the desired mode-I progressive failure [2], with an initial peak dependent on the bevel angle, followed by progressive failure. Palanivelu [3,4] found that a leading 45° bevel exhibited progressive failure for the ideal t/d ratio, for both planar and tubular sections, in line with the findings of Farley [5,6]. De La Cuesta [7] found that shallower angles of 30° produced an increased SEA, CFE, and mean crush force than angles $\geq 60^\circ$, with a reduced crush distance. Several authors conclude that for bevel/chamfer triggers, the optimal bevel angle, in terms of SEA, CFE, and mean crush force, is 45° . Unique bevel/chamfer combinations, such as the double chamfer, have been studied under quasi-static crushing for square profiles, resulting in a 4% reduction in SEA, a 5% lower CFE, and a 10% increase in initial collapse load compared to a single 45° bevel trigger.

Similar in impact force response to bevel/chamfer triggers is the implementation of a shape memory alloy (SMA) wire trigger mechanism, used to initiate leading-edge matrix failure through a temperature phase change, as a local weakening mechanism. This is studied in the work of Huang [8] and evaluated against a ply drop of the mechanism (reduced leading-edge ply thickness), which creates an effective bevel angle. In comparison to an un-triggered specimen, the SMA trigger improved the SEA by 37.2% and the CFE by 28%. The SMA trigger was superior to the ply drop-off trigger, particularly in terms of SEA (22.8% higher) and CFE (4.3%). The SMA trigger, however, is more complex to manufacture due to the requirement of pre-straining the Ni-Ti wire and bonding it to the crash tube.

Huang [9] further studied the implementation of a crown trigger, defined by eight shallow leading-edge notches or scallops, with a combined leading 45° chamfer. The crown trigger was compared to a leading-edge 45° bevel, resulting in a similar SEA, with an 18.4% lower initial specific triggering stress and a 21.2% higher CFE.

The use of an external trigger, defined as a plug or cap, has been studied in the works of [10,11], resulting in either internal (higher SEA) or external front formation, based on the cap profile. The structure SEA and failure behaviour under quasi-static crushing were found to be linked to the plug radius, whereby smaller radii resulted in fragmentation (3 mm radius) versus outward splaying. Siromani [11] found that compared to a bevel-triggered tube, plug/cap triggers exhibited a higher initial crush force and reduced specific energy (SE).

Tulip-shaped trigger mechanisms have been studied by several authors for tubular and section profiles, achieving a unique, progressive quasi-static crushing and impact force response. In comparison to bevel-initiated triggers, this results in a significantly lower initial crush force and extended total crush distance. This mechanism was most recently presented in the works of De La Cuesta and Ghasemnejad [12] through experimental axial

impact of tubular composite structures. For crashworthiness, the tested 4T60° Tulip trigger, compared to the 30° bevel trigger, exhibited a 7.5% reduction in SEA, a 21% increase in CFE, and a 77% improvement in SE due to the increased crush distance and reduced maximum crush force. De La Cuesta concluded that a further increase in the tulip angle to 90° improved the SEA, with the rise in tulip angle being more effective in enhancing SEA compared to the increase in the number of tulip tips.

2. Preliminary Crown-Tulip Trigger Concept

Leading from the ideal, progressive crushing behaviour of tulip-triggered composite tubular structures, the following key observations are made:

1. The reduced initial contact area produces a reduced initial contact force. This is attributed to the reduction in localised structure stiffness at the trigger region, thus enabling the onset of progressive failure.
2. For tulip-based trigger mechanisms, the leading tulip angle is proportional to the initial linear crush force gradient and inversely proportional to the total crush distance, influencing the SE.

By increasing the energy absorption during the initial crush phase, as a result of the higher initial crush force, the total crush distance and, therefore, stroke efficiency (SE) are reduced. A steep force gradient can result in a sharp drop-off in force beyond the initial peak, reducing the sustained mean crush force. Therefore, to enable improved SE, higher mean crush force, and crush force efficiency (CFE), the introduction of secondary crown notches is proposed between the broad-angle tulip cutouts. This combination is defined as the crown-tulip trigger mechanism. The preliminary concept is presented in Figure 1, illustrating an increased leading tulip angle of 140° and initial crown notches of 20°-20 mm. The preliminary specimen concept includes Ø5 mm holes at the end of the crown notch to experimentally investigate the effect of changing stress concentration in this region. The preliminary specimen concept includes Ø5 mm holes at the end of the crown notch to experimentally investigate the impact of changing the crushing process in this region.

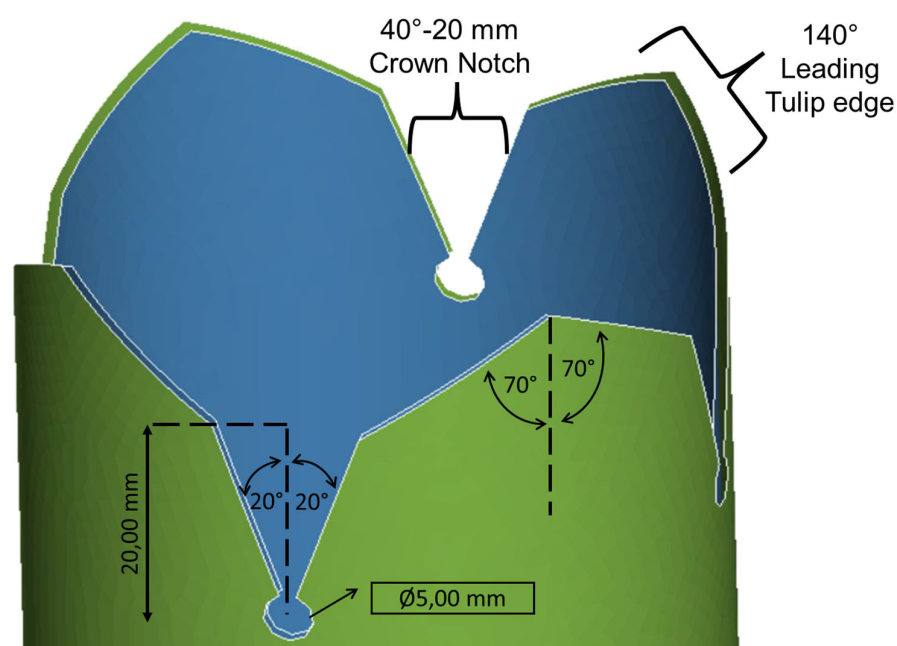


Figure 1. Preliminary crown-tulip trigger, illustrating the four 140° leading tulip tips with secondary 20°-20 mm crown notches, terminating in a Ø5 mm hole.

3. Experimental Studies

3.1. Manufacturing of Specimens

Following the development of the preliminary numerical model and design concept, representative coupons were manufactured at the Cranfield Enhanced Composites Centre. Specimens were formed around a $\varnothing 65$ mm aluminium mandrel using MTC 510-T700 UD to form a quasi-isotropic, $[45/-45/0/90]_s$ laminate, with a cured laminate thickness of 2.40 mm. The final trigger mechanism shape was achieved by hand machining utilising marked CAD (2023) templates, with the tube cut to 150 mm lengths.

3.2. Experimental Testing Process

A total of 5 crash tube specimens ($\varnothing OD$ 70 mm $\times$ 150 mm) were produced for impact testing with quasi-isotropic $[45/-45/0/90]_s$ laminate and nominal thickness (t) of 2.40 mm. This geometry results in an ideal crash tube $\frac{t}{\varnothing}$ of 0.034, which has been proven to allow stable crushing failure [13,14]. These cover the desired baseline trigger mechanism of the tulip trigger (QTY2: 4T90°), new crown-tulip trigger (QTY2: 4T140°-20°-20 mm $\varnothing 5$ mm), and a single 45° bevel trigger specimen B45° (see Figure 2). Only a single bevel trigger specimen was produced due to manufacturing limitations, and several researchers [15–19] have fully characterised the behaviour of such bevel triggers. The testing of the above-mentioned crash tubes was conducted at the Cranfield Impact Centre (CIC), utilising the vertical drop tower and flat plate impactor, as depicted in Figure 3. Tests were performed using a controlled mass of 80 kg and a target impact speed of 5 m/s, resulting in an input energy of 1000 J, consistent with the existing research on the development of the 4T90° tulip trigger mechanism. The impulse of the impact is recorded with a 400 kN load cell at a sampling rate of 20,000 Hz.

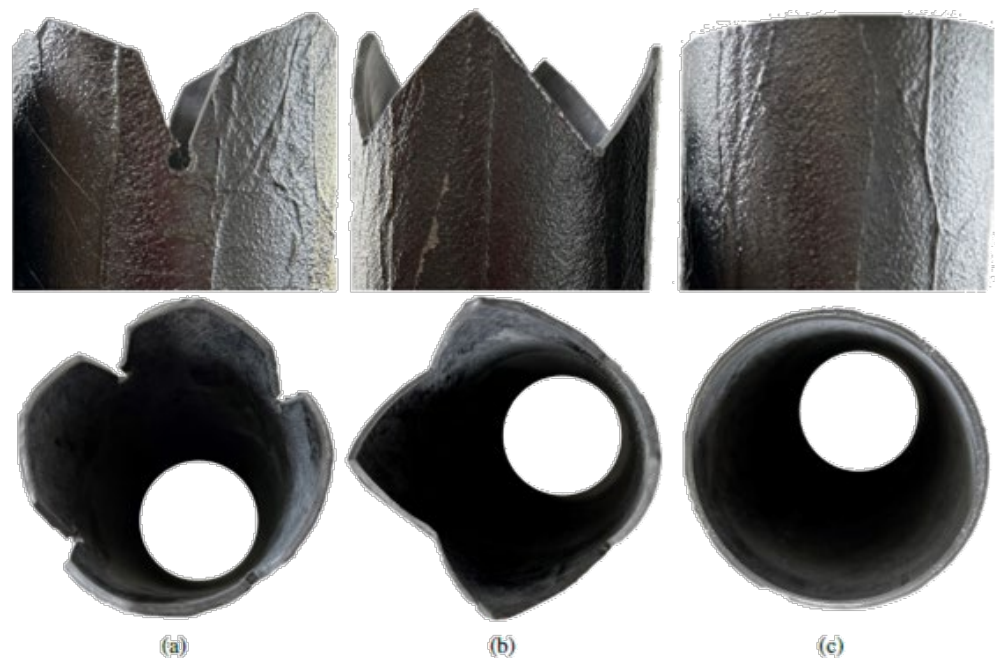


Figure 2. Tested triggered crash tubes compared to an un-triggered specimen (c), illustrating the geometrical differences between the crown-tulip trigger (a) and 4T90° tulip trigger (b).

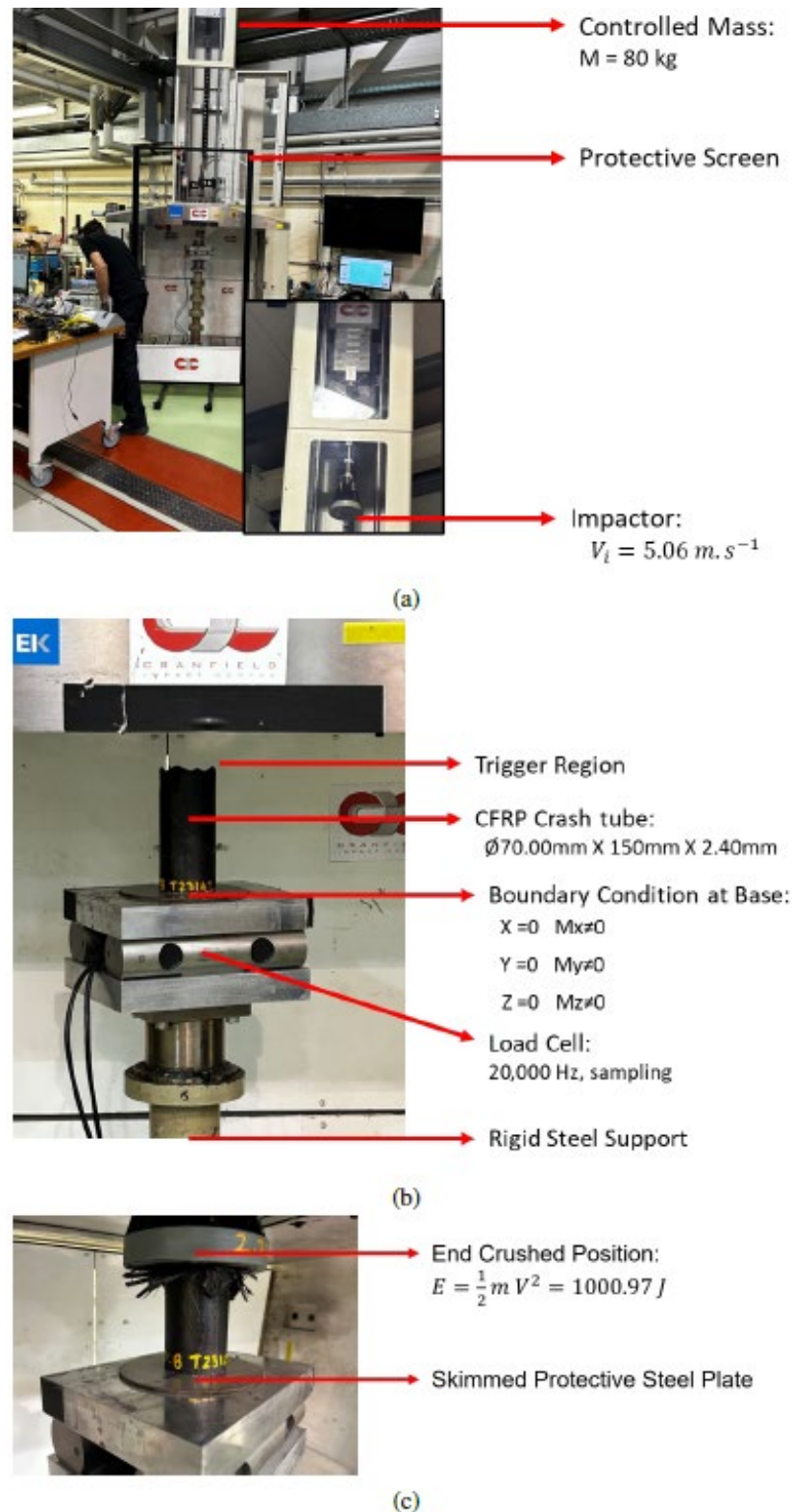


Figure 3. CIC vertical drop tower impact setup, illustrating the drop tower (a), specimen placement with defined boundary conditions (b), and end crush position for the crash tube within the drop tower (c).

3.3. The 45° Bevel Trigger Testing Results

The single 45° bevel trigger specimen exhibited symmetric progressive failure with the dominant mode-I delamination occurring between the outer 90°/0° ply interface. This does not occur at the exact laminate mid-plane due to the increased fracture toughness (G1c)

of the $90^\circ/90^\circ$ interface compared to the $90^\circ/0^\circ$ interface. There is clear debris buildup at the centre of the tube due to fragmented wedges forming from the contact between the inner 90° laminate and the impactor, as shown in Figure 4. Further fibre fractures are observed between the 0° outer fibres, which display laminar bending and splaying, resulting in longitudinal cracks. As the outer laminate layers are free to deform outwards, fronds/petals form as the tube structure progressively fails. At these fronds, mode-II delamination occurs due to the bending of the $0/-45^\circ$ layers. Due to the outer $\pm 45^\circ$ layup, a spiral-like splitting occurs at this surface.

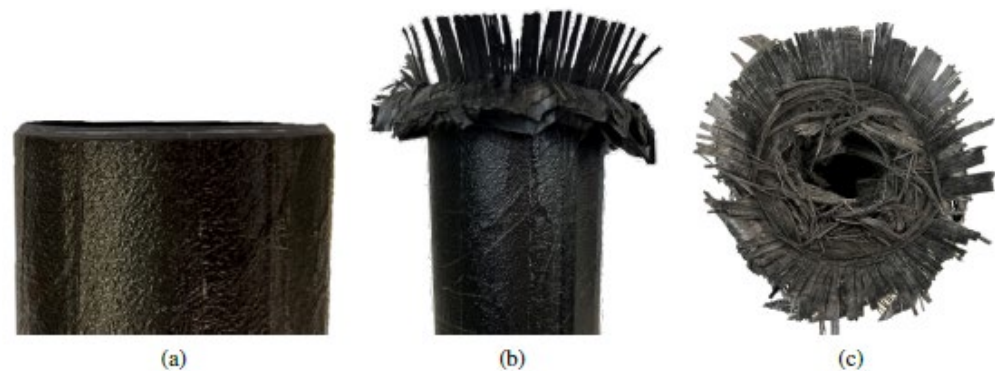


Figure 4. Illustration of the axial impact crushing behaviour of 45° bevel trigger (a), showing frond formation, laminar bending, and mode 2 delamination from the side view (b), and debris formation and mode 1 delamination (c).

As shown in Figure 5, the 45° bevel trigger produced an elevated initial peak force of 80 kN, corresponding to a peak acceleration of 100 g. Following the initial prominent peak, the force–displacement oscillates about a mean value of 30 kN for a total crush distance of 32 mm. This refers to the initial tube length of 150 mm, which corresponds to a 20-standard error (SE). Based on the initial tube mass of 124 g, the crushed mass is calculated as 24 g, resulting in an SEA of 42 kJ/kg and CFE of 39%.

3.4. The 4T90° Tulip Trigger Testing Results

In the 4T90° tulip trigger design, the crash tube exhibits a greater level of fragmentation and brittle fracture at the inner laminate interface in comparison to the 45° bevel trigger, as shown in Figure 6. The notching, which creates the tulip shape, results in petal-like frond formation due to the progressive peeling of the outer laminate layers at the $90^\circ/0^\circ$ interface, which has a lower fracture toughness (G_{Ic}) than the $90^\circ/90^\circ$ laminate midplane [15].

In comparison to the bevel trigger, there is reduced mode-II delamination, resulting from the bending of the 0° and -45° layers. The outer surface exhibits spiral fracture due to the progressive peeling, resulting in fracture of the 45° outer laminate layer. The analysis of the force–displacement curve, shown in Figure 6, exhibits a unique trend associated with the tulip trigger tip geometry. The contact points increase in stiffness as the tube structure collapses due to the 90° taper cutout. Therefore, the force response of the impactor progressively increases from a very low initial crush force. This eliminates the prominent peak during the initial crushing phase exhibited by the 45° bevel trigger design. It is confirmed from this behaviour that the tulip angle ($^\circ$) influences the gradient of this initial linear portion, with the caveat that the leading crash tube structure remains tapered to a near tip point.

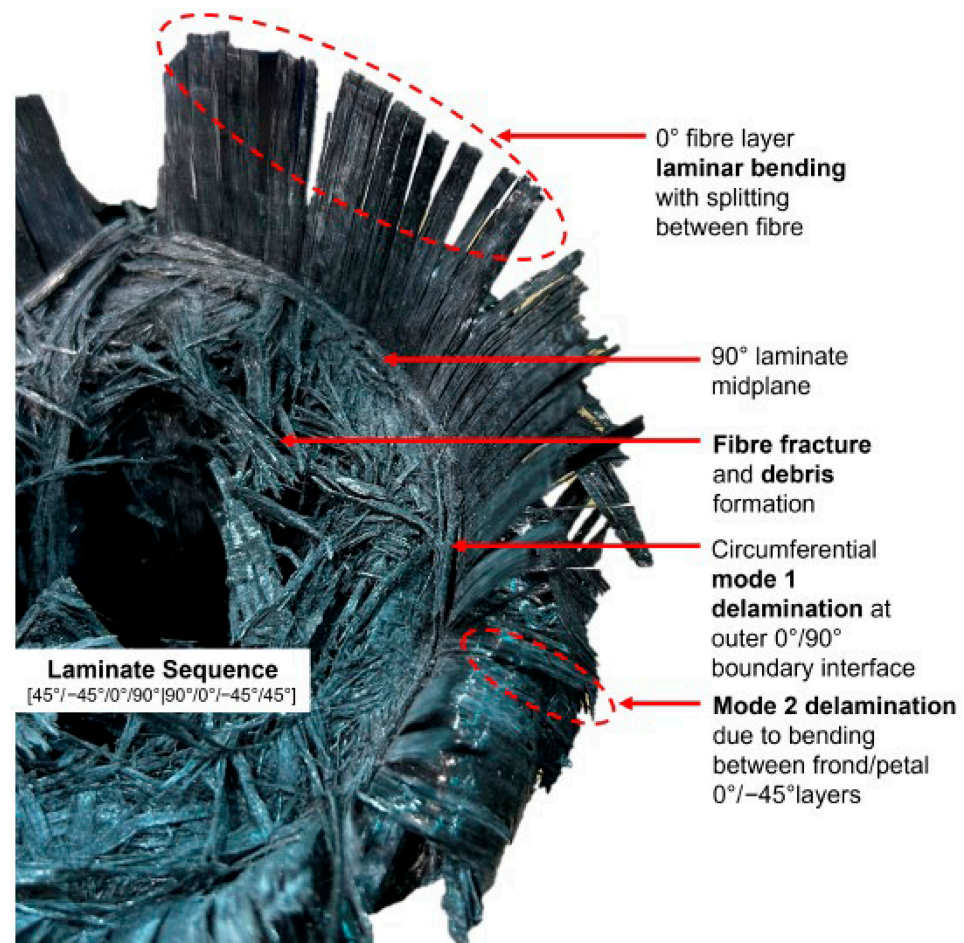


Figure 5. The 45° bevel-triggered tube, post impact deformation illustrating in detail the combined brittle fracture and laminar bending failure modes.

For both tests, the initial linear loading portion was identical, whereby the collapse of the tulip-notched region occurred until a crush distance of approximately 25 mm. Beyond this point, the entire circumferential area of the tube was exposed to the impactor, resulting in a change in stiffness and an increased force oscillation of approximately 30 kN. On average, the 4T90° tulip trigger tested exhibited a higher standard error of $39 \pm 1.1\%$ compared to the bevel trigger. The 4T90° tulip trigger had an SEA of 39 ± 2 kJ/kg, a CFE of $59 \pm 0.5\%$, and a mean force of 21.7 ± 0.8 kN. The overall 4T90° tulip trigger experimental behaviour is in line with the work of several authors [20–25], validating the experimental results.

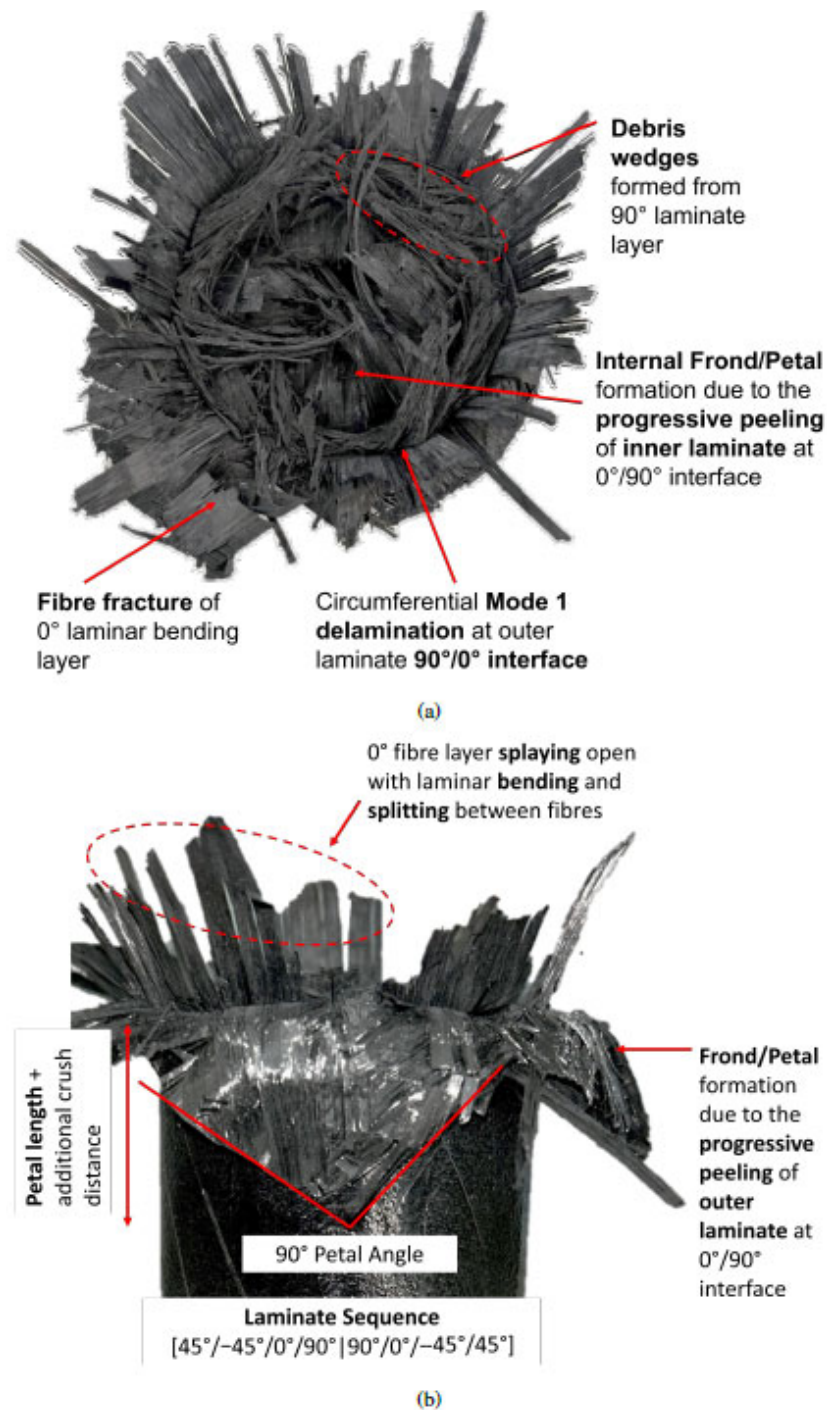


Figure 6. The 4T90° Tulip trigger post impact deformation, defining the combined brittle fracture and laminar bending failure modes, illustrated from top view (a) and side view (b).

3.5. The 4T140°-20°-20 mm-Ø5 mm Testing Results

In line with the tulip trigger tests, two samples were analysed, along with the average and standard deviation of the crashworthiness parameters, and the force–displacement response, as shown in Figure 7. The crown-tulip trigger design exhibited deformation in line with the 4T90° Tulip trigger. This details the progressive peeling due to mode-I delamination between the outer 90°/0° laminate layer and laminar bending of the inner 0° fibres. This progressive peeling exhibited broader fronds due to the wider 140° tulip leading angle.



Figure 7. Illustration of the crushing deformation of the tested crown-tulip trigger (a), showing the petal formation with progressive peeling due to mode 1 delamination (b) and top view (c) of the crushed tube with visible debris wedges and internal petal formation.

There was reduced splaying of the 0° fibre layer due to greater fragmentation, formed debris wedges, and overall increased brittle fracture, as illustrated in Figure 8. The formed, broader outer petals exhibited self-folding, which in turn resulted in mode-II delamination between the outer 0° and -45° layers. Due to the broader tulip angle, the crushing formation of internal fronds was noticeably greater than the level formed by both the tulip and bevel triggers. This increased the resistance to the impactor's vertical descent, as the progressive peeling of these internal fronds was restricted, and they would bind against each other. The outer tube surface exhibited spiral fractures due to progressive peeling, resulting in fractures of the $\pm 45^\circ$ outer laminate layers.

Analysing the impact force response in Figure 9, a similar linear trend to that of the tulip trigger occurred during the initial contact phase, identified up to an approximate crush distance of 6 mm. An intermediate region was determined, where the crush distance corresponded to the start of the secondary crown notch, and a force fluctuation around a local mean of 25 kN was noted. Once the impactor had crushed the tube past the end of the crown-notch hole, an increasing contact stiffness was seen due to the increased contact area of the tube. Both tests exhibited nearly identical crush responses, and the analysis of the

average testing critical crashworthiness parameters yielded an SEA of 41 ± 0.09 kJ/kg, a CFE of $67 \pm 3\%$, a mean force of 25.6 ± 0.06 kN, and an SE of $25 \pm 0.05\%$.

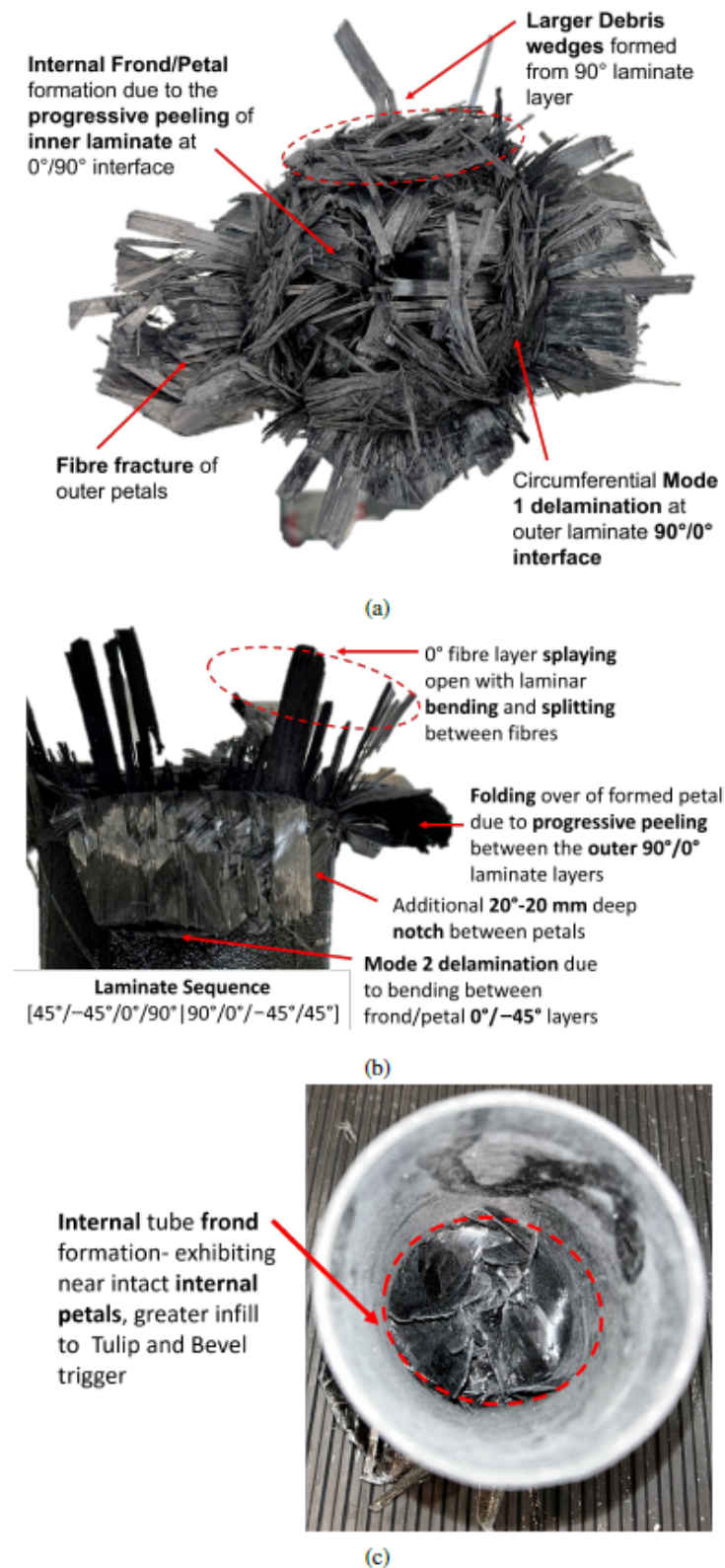


Figure 8. Crown-tulip trigger crash tube deformation, illustrating the frond formation and fragmentation from the top (a), reduced splaying and self-folding of external fronds (b), and internal crushing behaviour (c).

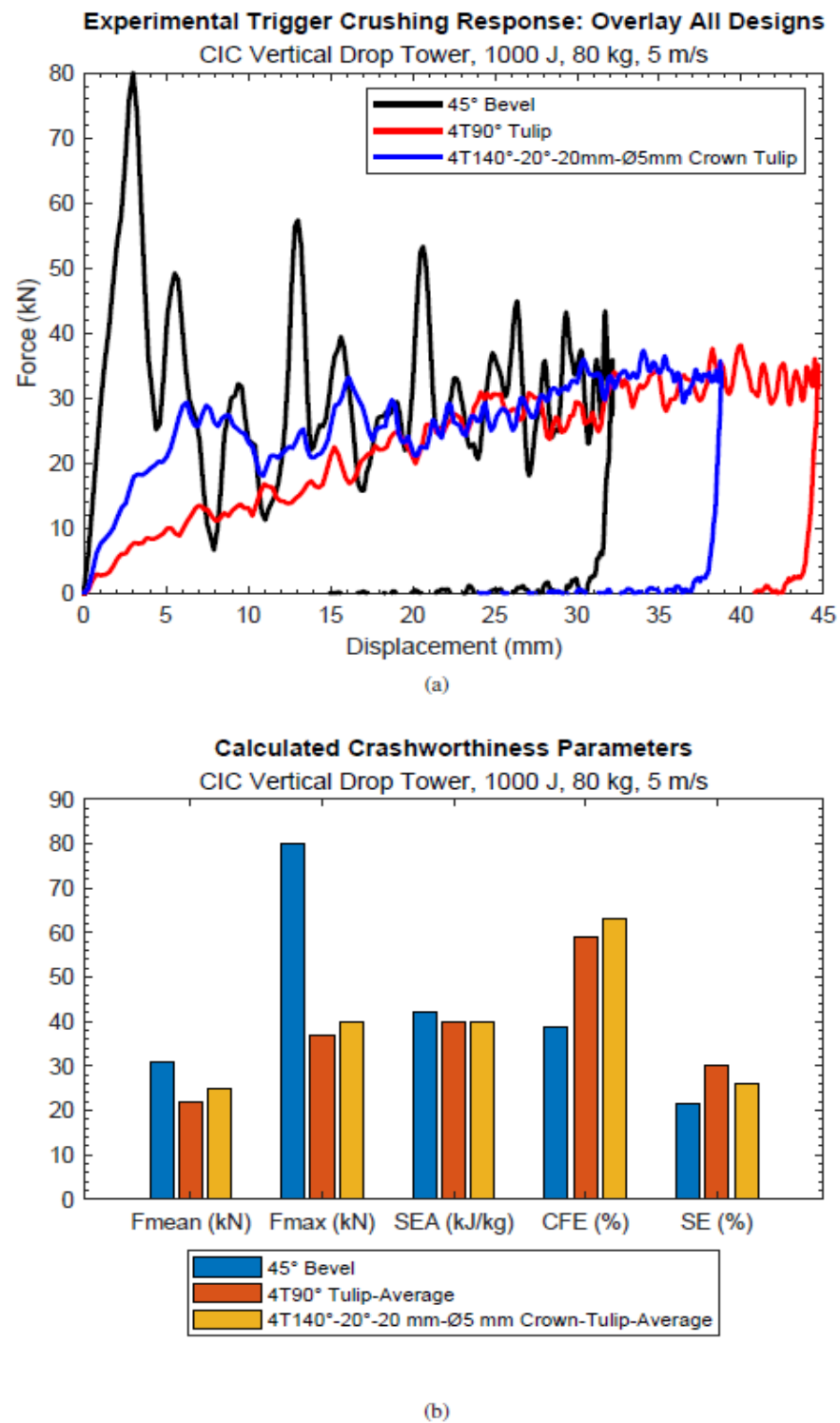


Figure 9. Comparison of the experimental testing results for the varied trigger designs: (a) illustrating the force–displacement response for test sample A and (b) calculated average test crashworthiness parameters (area under force–displacement diagram per mass indicates SEA).

3.6. Comparison of Experimental Testing Trigger Designs

The comparative analysis presented below focuses on the differences in the force–displacement and crashworthiness parameters for the axial impact of the tested trigger mechanism. The differentiation in performance is tied to both the base geometrical differences in the trigger shape and the dominant failure modes during progressive crushing. As previously stated, the tulip and crown-tulip triggers exhibited greater levels of brittle

fracture in comparison to the bevel trigger, whilst greater laminar bending and splaying of the outer 0° laminate layer was seen for the bevel trigger. However, the increased contact area to the impactor, for the 45° bevel trigger, produced an initial linear force–displacement curve, with a larger gradient, resulting in a high peak contact force. This is deemed undesirable due to the high acceleration associated with this peak force. By further tapering and reducing the initial contact area with the impactor, through the tulip trigger design, the produced force response resulted in a significantly lower initial contact force. This was a function of the reduced linear gradient, resulting from the depth (26 mm) and angle (90°) of the 4T 90° tulip cutouts. The modifications to the tulip trigger by increasing the leading-edge angle (to 140°) stiffened the structure, increasing the gradient of the initial linear crush phase and subsequent energy absorption. To ensure a sufficient total energy absorption of the crash tube, a larger total crush distance was required. This led to the crown notches between the tulip cutouts, defining the tested crown-tulip trigger combination.

The overlay of this behaviour is presented in the force–displacement curves shown in Figure 9. The tested crown-tulip trigger designs exhibited a 6% increase in CFE compared to the 4T 90° tulip trigger due to a 14% increase in mean crush force. In contrast to the 45° bevel trigger, the CFE was 33% higher. For the tested crown-tulip trigger, the improvement in SEA was minor, at 1%, with both the tulip and crown-tulip triggers, resulting in an SEA approximately 7% lower than the 45° bevel trigger, which achieved 43 kJ/kg. However, the 90% reduction in maximum crush force was drastic and a clear indication of the improvements made in the transferred acceleration.

4. Numerical Modelling

With the conclusion of physical testing and manufacturing, an equivalent numerical model was established. This was carried out using LS DYNA PrePost V4.8.29, utilising keyword entry, to develop a double-precision dynamic model. It has been established that, at a minimum, two shell layers are required to capture dominant mode-I delamination, which splits the geometry symmetrically about the laminate mid-plane. Belytschko–Tsay shell elements with a size of 2×2 are used, with a single integration point defined per layer [7,23]. The impactor was represented as a solid steel rigid body. Bevel triggers were achieved by a stagger of the leading-edge shell height to achieve an equivalent bevel angle. The tulip and crown-tulip triggers required no simplification of the trigger geometry. In the application of the Belytschko–Tsay shell elements to discretise the CFRP tube, the shear stress distribution was corrected using the LAMSHFT shell control. In this study, a stiffness-based hourglass control, specifically the IHQ = 4-Flanagan-Belytschko stiffness form (see Figure 10), was employed.

4.1. General Contact Definitions

To mitigate the penetration of the impactor (master) with the tube shells (slave), an automatic node-to-surface contact was used. The use of an automatic contact definition is appropriate as the deformation of the crash tube results in the non-normal orientation of shell elements. Friction between the impactor and the crash tube is defined with F_s and $F_d = 0.05$. The automatic surface self-sliding resistance of the shell layers is determined by a friction coefficient value of 0.230 (see Figure 11).

The contact definition between shell layers, representing the resistance to mode-I and II delamination, was defined by option 8, automatic, one-way, surface-to-surface tie-break contact. For the model, the master surface was defined as the outer shell layer, with the inner surface as the slave. The penalty condition of the tie-break contact represents the interface strength, established from DCB and ENF fracture tests of the MTC510-T700 UD. The contact failed, and nodes were released once the normal (σ_n) or shear (τ_n) stress at the

interface exceeded the defined interface normal (NFLS = 70 MPa) and shear strength limit (SFLS = 70 MPa).

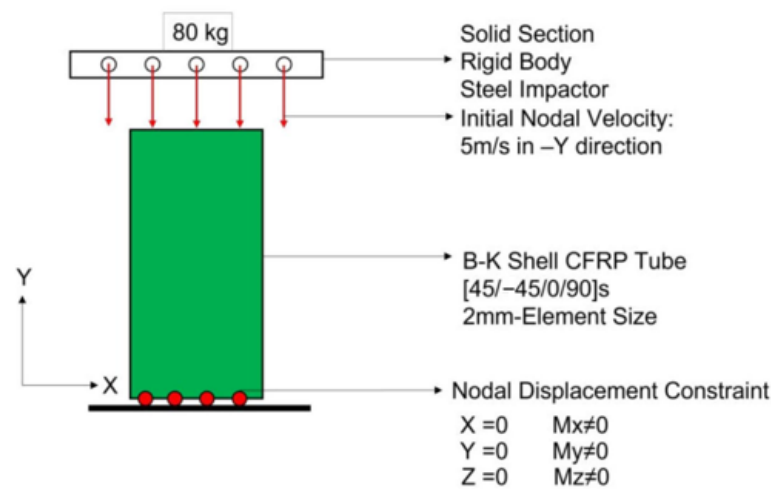


Figure 10. Illustration of FEM model defining the crash tube discretisation and boundary conditions, with the impactor initial position and velocity defined.

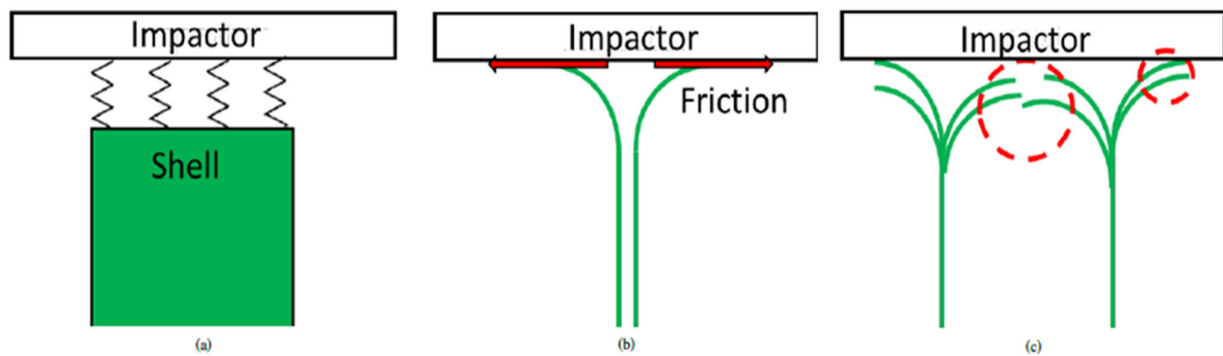


Figure 11. Numerical model, impactor to crash tube interactions, illustrating the normal contact between impactor and shell (a), tangential friction between impactor and shell (b), and shell layers self-contact (c).

4.2. Optimised, MAT 55: Enhanced Composites Damage Material Definition

This work studies the behaviour of the same quasi-isotropic laminate [45/−45/0/90]_s, MTC510-T700 UD prepreg fibres, tulip trigger design, and further design evolutions of the trigger mechanism, as per the published work in [22]. Using this as the preliminary baseline for the material definitions, an optimal fit to the testing force–displacement and crashworthiness parameters for each trigger was obtained through a 25% reduction in theoretical failure strengths. This difference in strength is attributed to the manual manufacturing process and defect generation, particularly the hand nesting of plies and low-pressure oven curing versus autoclave curing. Furthermore, it was found that the MAT: 55 failure definition, based on the Hashin Failure criteria, best captured the failure behaviour of the tested CFRP tubes, whereas the MAT 54: Chang failure criteria resulted in exaggerated brittle model behaviour (see Table 1).

Table 1. Optimised MAT55: enhanced composite damage material card properties.

MAT 55: Enhanced Composite Damage Card				
ρ (kg/m ³) 1500				
Elastic Properties				
Ea (GPa) 119.3	Eb (GPa) 8.2	GAB (GPa) 3.6	PRBA 0.01	
GBC (GPa) 2.7	GCA (GPa) 3.9	GCA (GPa) 3.9		
Failure Strength Properties				
Xc (MPa) 800	Xt (MPa) 1710	Yc (MPa) 150	Yt (MPa) 40.05	SC (MPa) 74.25
Failure Strain Properties				
DFAILM 0.0176	DFAILT 0.014	DFAILS 0.015	DFAILC −0.005	
Numerical Properties				
TFAIL 0.6	ALPHA 0	SOFT 0.7	YCFRAC 2	BETA 0
FBRT 0	SOFT2 1	EFS 0	CRIT 55	

5. Results and Discussions

5.1. The 4T90° Tulip Trigger

The initial linear crush phase exhibited a gradient equivalent to that of the model and test samples, as shown in Figure 12. This behaviour was noted till a crush distance of 26 mm, corresponding to the end of the tulip trigger notch depth. Past this point, the test samples exhibited a reduced local force oscillation due to the collapse of fronds in the test samples at this crush distance ($28 \text{ mm} < x < 33 \text{ mm}$). Thereafter, for the continued crush process, both the test specimens and the numerical model oscillated around a mean force of 32 kN. The numerical model exhibited a crush distance and maximum force value within the range of the two test samples. The numerical model exhibited a higher SEA and CFE than the testing average due to the reduced crushed mass of 2% and a fractional reduction in the maximum crush force. The individual errors relative to the testing average of the critical crashworthiness parameters were squared, and this total was shown to be less than 1% (see Table 2).

Table 2. The 4T90° tulip trigger testing vs. model crashworthiness parameters, including error calculation of the experimental testing results.

4T90° Tulip Crash-Worthiness Parameters							
	Fm (kN)	Fmax (kN)	SEA (kJ/kg)	CFE (%)	SE (%)	Mass (g)	$\sum (\text{Error})^2$
Simulation	22	37	40	59	30	25	-
Error	0%	0%	2%	0%	−2%	−2%	0.20%
Testing	22	37	39	59	31	25	-



Figure 12. Comparison of 4T90° tulip trigger deformation between physical test specimens T23146-Test A (a), T23145-Test B (b), and numerical model (c).

Furthermore, the deformation between the numerical model and test samples is compared in Figure 12. The simplified twin shell approach is shown to be sufficient in capturing the inner and outer front formation propagating from the progressive failure, initiated by the tulip petals. The top view of the numerical model exhibits outward peeling of the inner shell at the tube corners, forming a square shape. This feature is a numerical occurrence due to the deletion of elements within this region. The basic idealised deformation, with mode-I delamination between the shell layers, is captured sufficiently.

The simplified twin shell numerical approach does not produce the same laminar splaying and debris wedges as the test samples; however, the characterised force response still matches. Therefore, the numerical model of the 4T90° Tulip trigger progressive failure is validated with respect to the force response, calculated crashworthiness parameters, and basic deformation (see Figure 13).

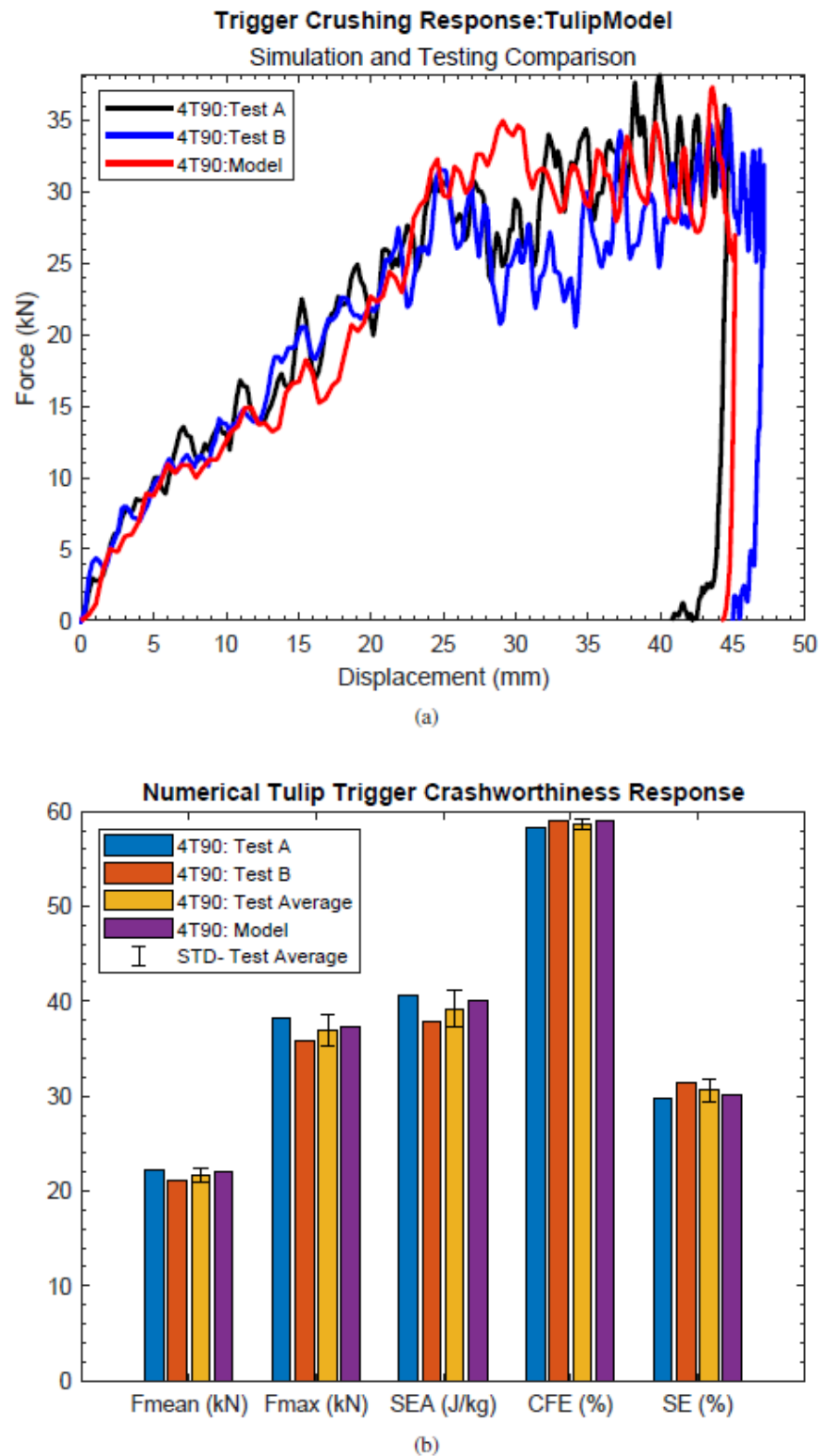


Figure 13. The 4T90° tulip trigger experimental vs. numerical model behaviour, illustrating the force–displacement behaviour (a) and calculated crashworthiness parameters (b).

5.2. Crown-Tulip Trigger

Figure 14a illustrates the force response for both experimental test specimens (A, B) and the simulation model. The initial linear crush phase, till a crush distance of 2 mm, is identical between the test specimens and the model. This gradient is dependent on the leading tulip angle, in this instance of 140°. Past the crush distance of 5 mm, both test specimens exhibit a force drop from 20 kN to 17 kN, after which they continue to increase to a local peak of 32 kN. Therefore, this local peak is 1 kN less than the value predicted by the model and occurs at a crush distance of 7 mm, compared to 5 mm for the model. The crush distance of 5 mm in the numerical model coincides with the end of the leading 140° tulip trigger edge and the start of the 20°-20 mm crown notch. At this point, a step in the model stiffness can be observed. Past the tulip crush phase, as the progressive failure of the crash tube reaches the crown notch, a reduction in stiffness is seen in the force–displacement response. This matches closely in the period between the experimental samples and the model. Both samples and the test model exhibit a similar total crush distance of 38.5 mm. The numerical model predicts a 4% higher end crush force of 40 kN, which is also the maximum crush force (see Table 3).

Table 3. Calculated crashworthiness parameters for the 4T140°-20°-20 mm-Ø5 mm crown-tulip trigger, comparing the predicted simulation behaviour to the calculated average experimental testing values.

4T140°-20°-20 mm-Ø5 mm Crown-Tulip Trigger Simulation and Experimental Testing Comparison							
	F _m (kN)	F _{max} (kN)	SEA (kJ/kg)	CFE (%)	SE (%)	Mass (g)	$\sum (Error)^2$
Simulation	26	40	41	63	26	24	0.40%
Error	0%	4%	−1%	−5%	0%	1%	
Test Average	26	39	41	67	26	24	

The influence of the afore-mentioned variances on the force response is presented in Figure 14b, highlighting their impact on the critical crashworthiness parameters. The numerical model predicts a higher maximum crush force than the testing. Average but lies within the upper bound of the testing standard deviation. Due to the increased prediction of maximum crush force, the CFE of the numerical model is 5% lower than the testing average; however, it falls within the lower bound of the standard deviation. The numerical model exhibits a 0.46% underprediction of the mean crush force and a 1% reduction in SEA, which falls outside the narrow testing standard deviation of 0.07 kN and 0.1 kJ/kg, respectively. However, this small tolerance for the small test sample size of two is still accepted. Therefore, by calculating the squared error for each crashworthiness factor, the total summation of this error is less than 0.5% relative to the experimental testing average.

The comparison of the numerical model deformation to the experimental samples is presented in Figure 15. The simplified numerical modelling approach with two shell layers captures the general progressive failure and front formation about the tie-break interface. The inner shell layer forms a square internal petal, which matches the internal laminate petal formed in the experimental samples. Artefacts such as the debris wedges and the 0° splayed layer are not captured in the model due to the simplified twin-shell layer discretisation of the laminate. Therefore, the equivalent crown-tulip numerical model is shown to be sufficiently accurate in predicting the impact force response, crash tube deformation, and calculated crashworthiness parameters. Further design development of the crown-tulip trigger can now occur, utilising the validated simulation approach.

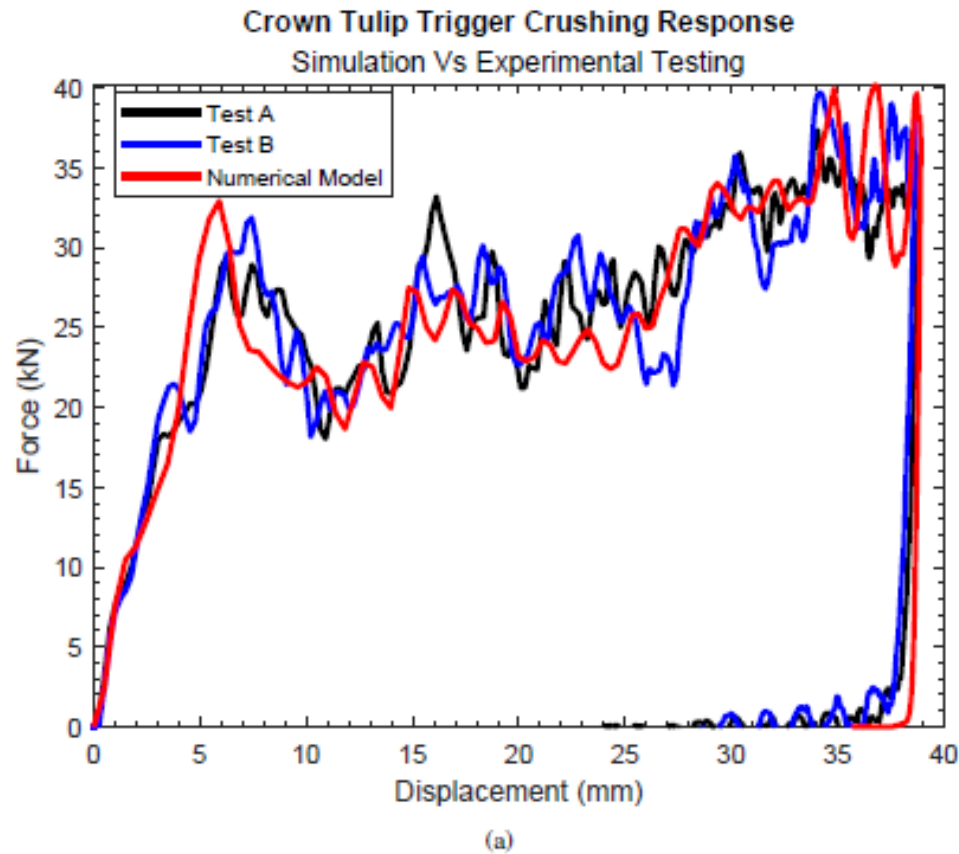
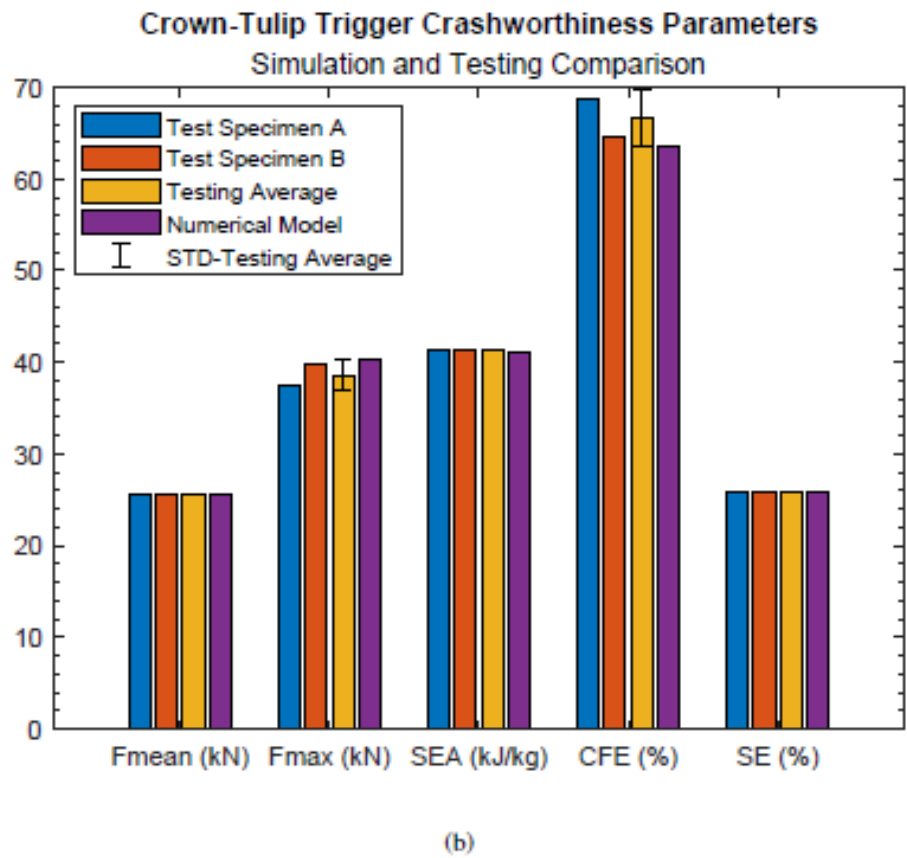


Figure 14. The 4T140°-20°-20 mm-Ø5 mm crown-tulip trigger comparison of experiment to simulation: force response (a) and calculated crashworthiness parameters (b) for each experimental test specimen, calculated test average, and standard deviation (STD).



Figure 15. Comparison of 4T140°-20°-20 mm-Ø5 mm crown-tulip trigger deformation between physical test specimens T23147-Test A (a), T23148-Test B (b), and numerical model (c).

6. Numerical Crown-Tulip Trigger Design Development

Following the experimental testing of various trigger mechanism designs and validation of equivalent numerical models, the crashworthiness and force response of the crown-tulip trigger are further developed. The crown-tulip trigger is now analysed concerning each component of the trigger, i.e., the leading tulip angle, number of tulip tips, notch depth, notch angle, and additional notch hole. Through this iterative process, where the geometrical components are varied in isolation, an optimal new developed design is formed.

As presented in Figure 16, the numerical discretisation of the crown-tulip trigger illustrates the variance from the tulip baseline by the addition of the crown notch. This is initially presented for a 5° notch angle and 20 mm notch depth. This angle serves as the lower bound limit to avoid significant distortion of the model mesh elements for the selected ideal 2 mm mesh size. A depth of 20 mm was chosen, as it was experimentally tested.

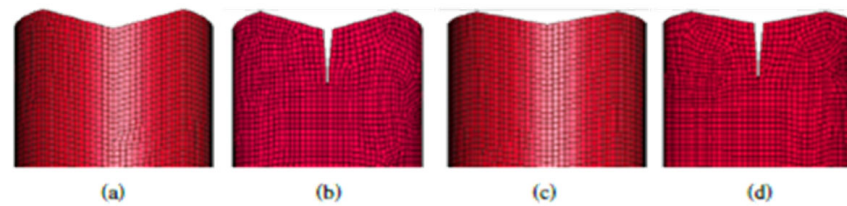


Figure 16. Discretisation of crown-tulip trigger, illustrating the modification from 4T150° tulip trigger (a) to 4T150-5°-20 mm crown-tulip (b) and 4T160° tulip (c) to 4T160-5°-20 mm crown-tulip (d).

The leading tulip trigger angles of 150° and 160° were selected based on the numerical investigation of varied pure tulip triggers. Whilst the tulip angle of 150° did result in the highest tulip trigger SEA and CFE, the leading angle of 160° exhibited the highest maximum and mean crush force, with force–displacement behaviour similar to the 45° bevel trigger.

For the 4T160° tulip trigger angle, the addition of the crown notch resulted in a 46% reduction in the maximum crush force and a 14% reduction for the 4T150° crown-tulip variant. By reducing the maximum initial crush force, the crush distance was extended 10% for the 150° variant and 12% for the 160° tulip leading notch. This is illustrated in Figure 17, where the improvement in CFE and SE resulting from the introduction of the crown notch is evident. The improvement in SE is due to the above-mentioned increase in total crush distance. A 10% reduction in the mean crush force was observed with the introduction of crown notches, resulting in improved CFE. A 9% reduction in SEA was observed for the implementation of the crown notches, due to the reduced crush mass for near-identical total energy absorption, compared to the base 150° and 160° tulip trigger angles.

6.1. Crown-Tulip Notch Angle—I

With an optimal leading tulip angle of 160° defined, further design development with respect to the ideal crown notch angle is presented for a notch depth of 5 mm. This crown notch depth is selected to allow wider notch angles to be investigated. For deeper notches, such as the experimentally tested 20 mm depth, the leading tulip edge is removed due to the shallow 160° tulip angle. The crown notch angle is varied in 10° increments from a maximum of 60° to 15°. For the selected notch depth of 5 mm, angles less than 15° are not investigated as the formed geometry results in a fine slit, no longer representing the desired crown trigger shape (see Figure 18).

Presented in Figure 19a is the force response for the investigated broader crown notch angles of 60°, 50°, and 40°. For the above-mentioned notch angles, the initial crush phase is identical, terminating at a crush distance of 1.50 mm. The 60° notch angle exhibits the highest maximum crush force of 57 kN, extending from the initial approx—linear crush phase. A trend is noted, whereby the maximum crush force is proportional to the crown notch angle. Referring to the initial crush phase, the 50° crown notch angle exhibits a step at a crush distance of 1.50 mm, exhibiting reduced stiffness in comparison to the 40° and 60° crown notch models, till a crush distance of 6 mm. Following this, the expected increased stiffness compared to the 40° crown notch model is observed.

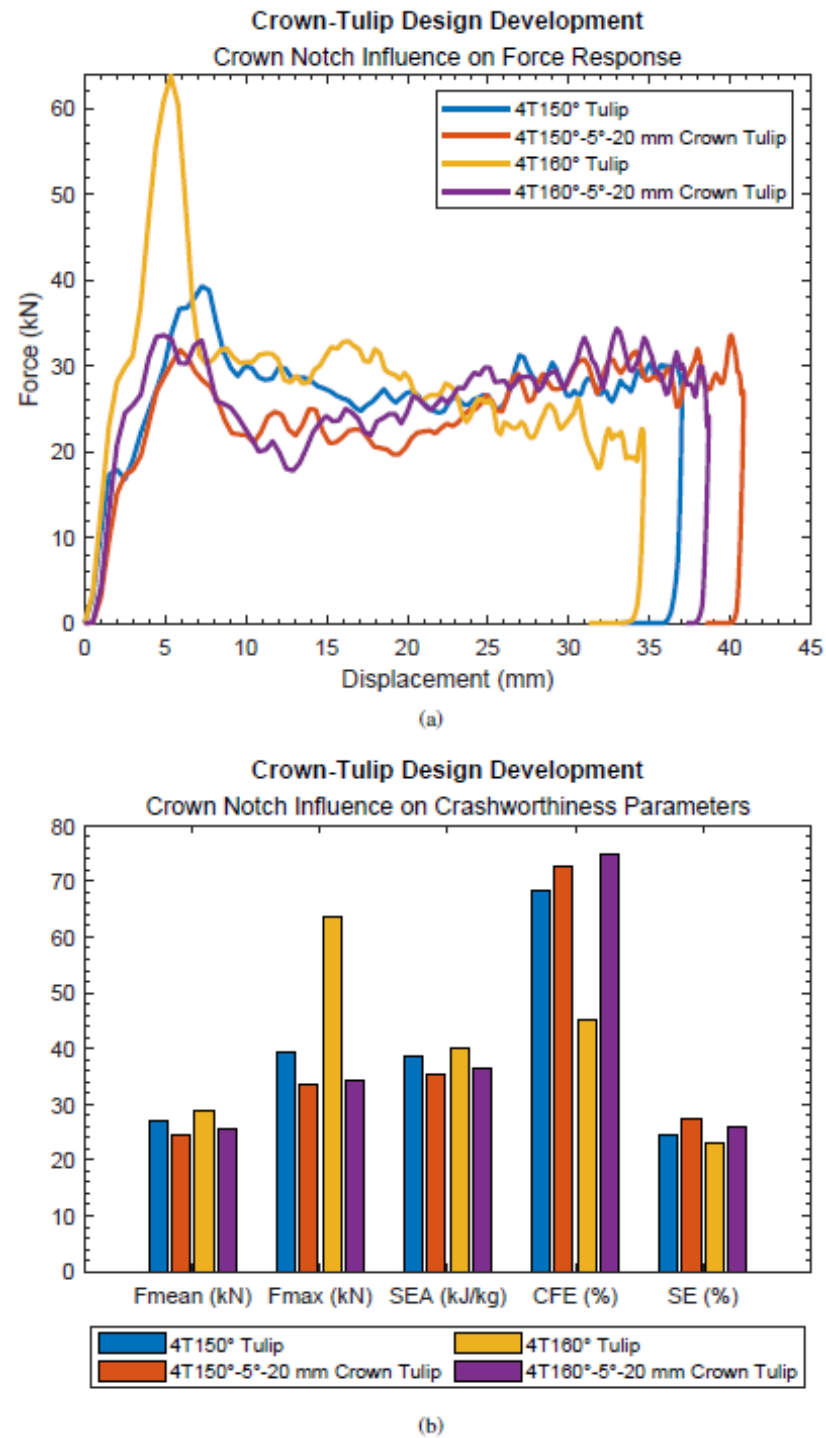


Figure 17. Numerical development of crown-tulip trigger, illustrating the variance in force response (a) and critical crashworthiness parameters (b) for the implementation of a 5°-20 mm crown notch for the 4T140° and 4T150° tulip triggers.

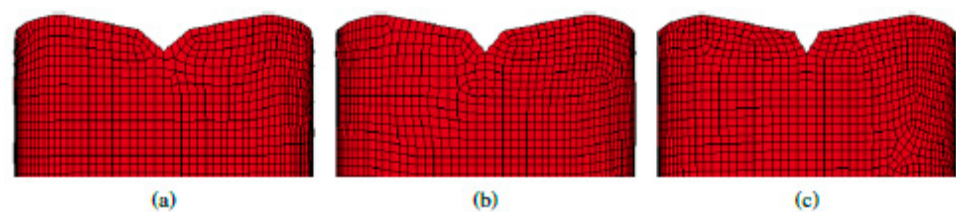


Figure 18. Discretisation of crown-tulip trigger, illustrating the geometrical change for the varied crown trigger notch angles for a select set, whereby (a): 50°-5 mm, (b): 40°-5 mm, and (c): 30°-5 mm.

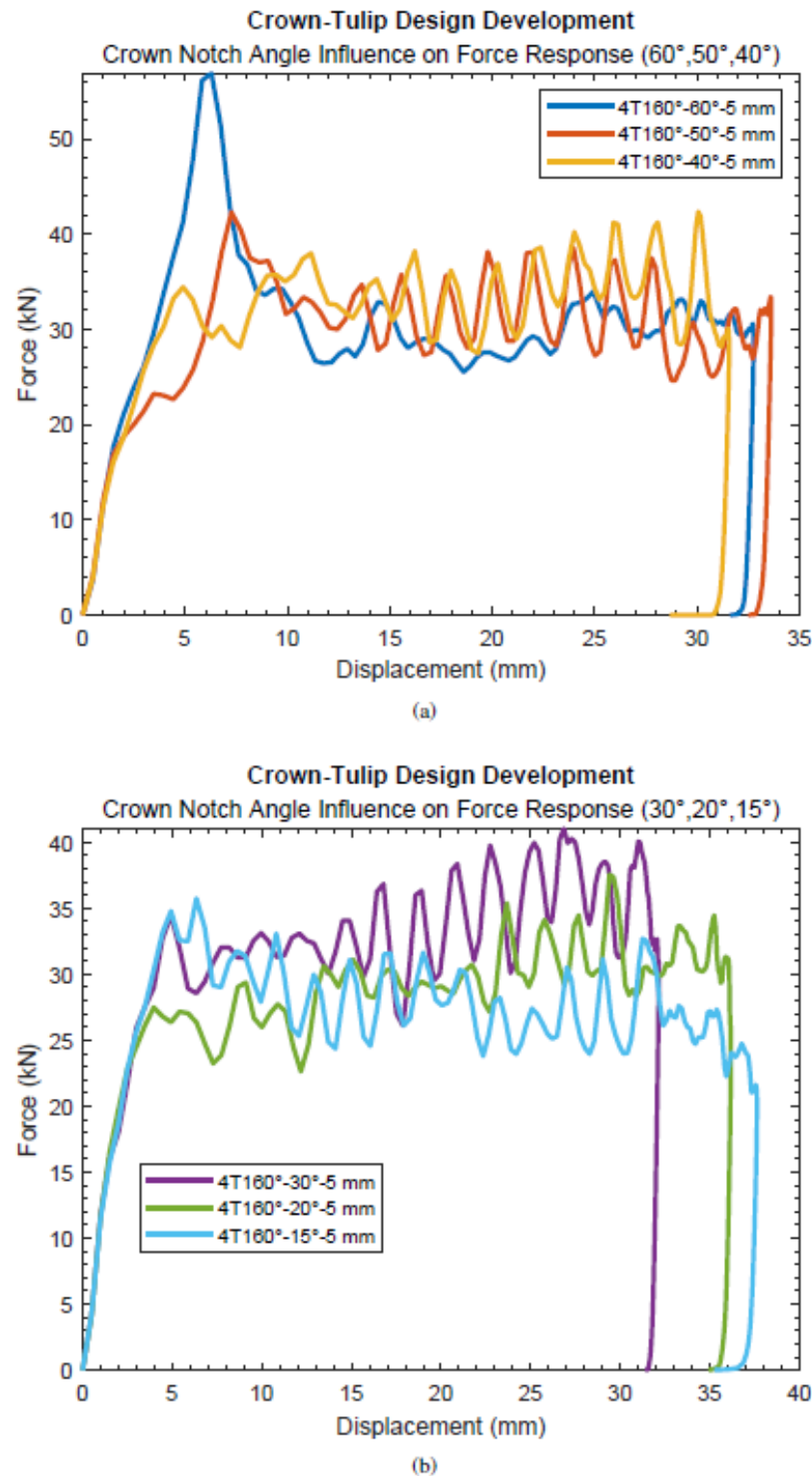


Figure 19. Numerical development of crown-tulip trigger, illustrating the variance in force response for broader (a) and narrower (b) crown notch angles for the fixed notch depth of 5 mm and leading tulip trigger angle of 160° with four tips (4T160°).

This behaviour is associated with the increased numerical deletion of leading-edge elements in the 50° model discretisation during the initial impact phase. Following the maximum crush force, the 50° and 40° crown notch angles oscillate around a similar local mean force of 34 kN, with a higher end crush force of 42 kN noted for the 40° crown notch angle. The end crush force is reported to exhibit an inversely proportional trend to the

crown notch angle. A consistent trend is not seen in the total crush distance relative to the crown notch angle. By investigating the crush distance associated with the maximum crush force, a clear trend is observed: the earlier the maximum crush force occurs in the impact period, the shorter the total crush distance.

For the analysis of the narrow crown notch angle set (30° , 20° , and 15°), presented in Figure 19b, an identical initial crush phase, terminating at a crush distance of 2.50 mm, is noted. Past this point, the 20° notch model deviates at a lower crush force of 28 kN, with an increasing force oscillation, till the end crush distance of 36 mm is reached. For the crown notch angles of 30° and 20° , a similar initial crush phase peak of 35 kN is noted. Following this, the 30° crown notch angle model exhibits identical behaviour to the 20° variant, with a higher end crush force of 40 kN. With respect to the mean crush behaviour, following the initial crush phase, the magnitude of the impact force oscillation is shown to have a proportional relationship to the crown notch angle. An inversely proportional trend is observed for the shallower notch angles with respect to the total crush distance. The influence of the above-mentioned changes in model force response, due to the crown notch angle, on the critical crashworthiness parameters is presented in Figure 19a for all the tested notch angles (see Table 4).

Table 4. Calculated critical crashworthiness parameters for the varied crown notch angles for a fixed crown notch depth of 5 mm and leading 4T160° tulip trigger edge, with relative improvement shown relative to the 15° notch angle.

Varied Crown-Notch Angles Calculated $\pm$						
	F _m (kN)	F _{max} (kN)	SEA (kJ/kg)	CFE (%)	SE (%)	Mass (g)
60°	30	57	43	53	22	23
Delta	13%	59%	16%	−29%	−13%	−15%
50°	30	42	42	70	22	24
Delta	10%	18%	12%	−7%	−11%	−12%
40°	31	42	45	74	21	22
Delta	17%	19%	20%	−1%	−16%	−18%
30°	31	41	45	77	21	23
Delta	17%	15%	19%	2%	−15%	−16%
20°	28	38	39	74	24	26
Delta	4%	5%	5%	−1%	−4%	−4%
15°	27	36	37	75	25	27

Overall, the 30° and 40° crown notch angles exhibit the best SEA, CFE, and mean crush force, with a 36% reduction in maximum crush force compared to the broader 60° crown notch angle. The approx. 20% improvement in SEA and 17% increase in mean crush force for the 30° and 40° crown notch angles. Further design development is carried out, utilising a crown notch angle of 40° due to these improvements in crashworthiness and reduced force oscillation compared to the 30° notch angle (see Figure 19).

6.2. Crown-Tulip Notch Angle—II

With the optimal crown trigger notch angle and leading tulip trigger angle established, the force response and crashworthiness are studied below for the implementation of a circular hole at the notch tip. This was implemented in the experimentally tested 4T140°-20°-20 mm-Ø5 mm crown-tulip trigger. The introduction of the hole altered the stress concentration at the notch tip, increasing the effective notch angle and depth. This behaviour was studied for a single case, implementing a Ø5 mm hole for the 40° -5 mm crown notch. This specific hole size was selected to minimise the element distortion due to

the optimised 2 mm mesh size and match the experimentally tested crown-tulip trigger's notch hole diameter (see Figure 20).

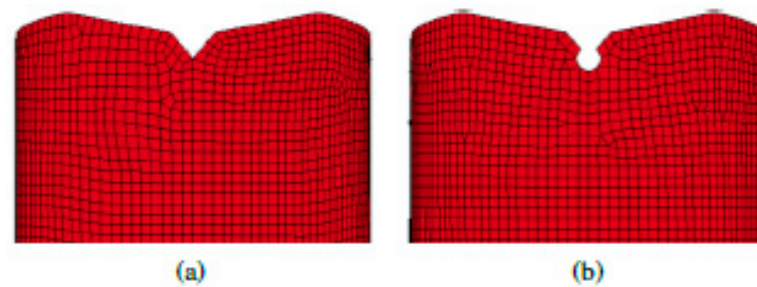


Figure 20. Discretisation of end notch hole for the crown-tulip trigger with crown notch angle of 40° and depth of 5 mm, comparing the base design (a) to the implemented $\varnothing 5$ mm hole (b).

The force–displacement for the introduction of an end notch hole of $\varnothing 5$ mm alters the initial crush behaviour, reducing the peak crush force and terminating the initial linear behaviour at a reduced crush distance of 5 mm, illustrated in Figure 21a. This is attributed to the adequate notch depth, which is increased for the addition of the $\varnothing 5$ mm hole, thereby extending the initial linear crush phase, which terminates at the end of the notch. Both models exhibit a similar mean crush force of approximately 35 kN, following the initial crush phase, with an increased crush distance of 33 mm for the hole notch model. The end crush force is increased by 26% for the introduction of the $\varnothing 5$ mm hole at the notch end.

These changes in force response on the critical crashworthiness parameters are presented in Figure 21b. The 5% increase in maximum crush force and 5% decrease in mean crush force for the introduction of the $\varnothing 5$ mm hole result in a 9% reduction in CFE. A 4% decrease in SEA is noted, due to reduced total energy absorption and increased total crush distance, resulting in a 4% increase in crushed mass. Therefore, in further design development, the implementation of a circular hole at the end of the crown notch is not studied due to the reduced SEA, CFE, mean crush force, and increased maximum crush force, which is undesirable with respect to optimal crashworthiness.

6.3. Crown-Tulip Notch Depth

With the established, ideal leading tulip trigger angle, crown notch angle, and crown notch tip feature, the influence of the crown notch depth was investigated. This design development varies the crown notch depth in discrete intervals from 1 mm to 20 mm for the 4T160°-40° crown-tulip trigger. The following sections present a summary of this study for notch depths ranging from 1 mm to a maximum of 10 mm. This is the critical range where an optimal solution was established. Figure 22a illustrates a similar initial linear crush force response corresponding to the crushing of the leading tulip trigger of 160°.

In comparison to the pure tulip 4T160° trigger design, this behaviour deviates past a crush distance of 1 mm. This corresponds to the start of the crown notch. Thereafter, linear crushing still occurs at a reduced gradient, implying lower structure stiffness. For the notch depths of 5, 2, and 1 mm, the maximum crush force occurs at a crush distance of approximately 5 mm, which matches the behaviour of the 4T160° pure tulip trigger. The magnitude of said maximum crush force exhibits an inverse proportional behaviour to the notch depth. This is associated with the reduced stiffness of the leading crash tube structure as the crown notch depth increases.

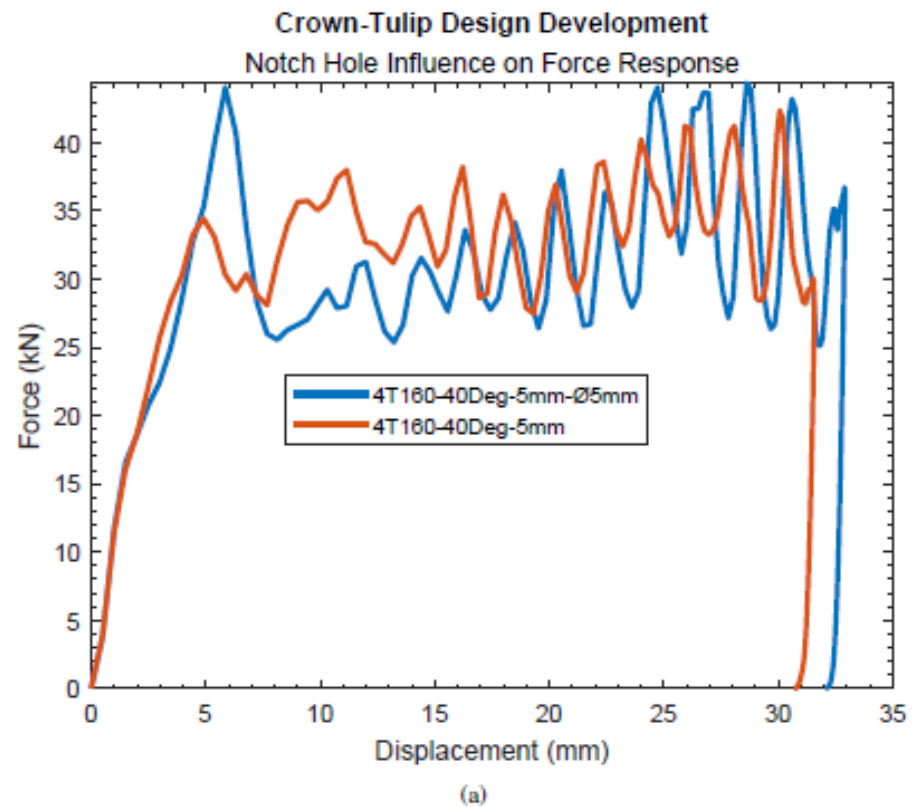
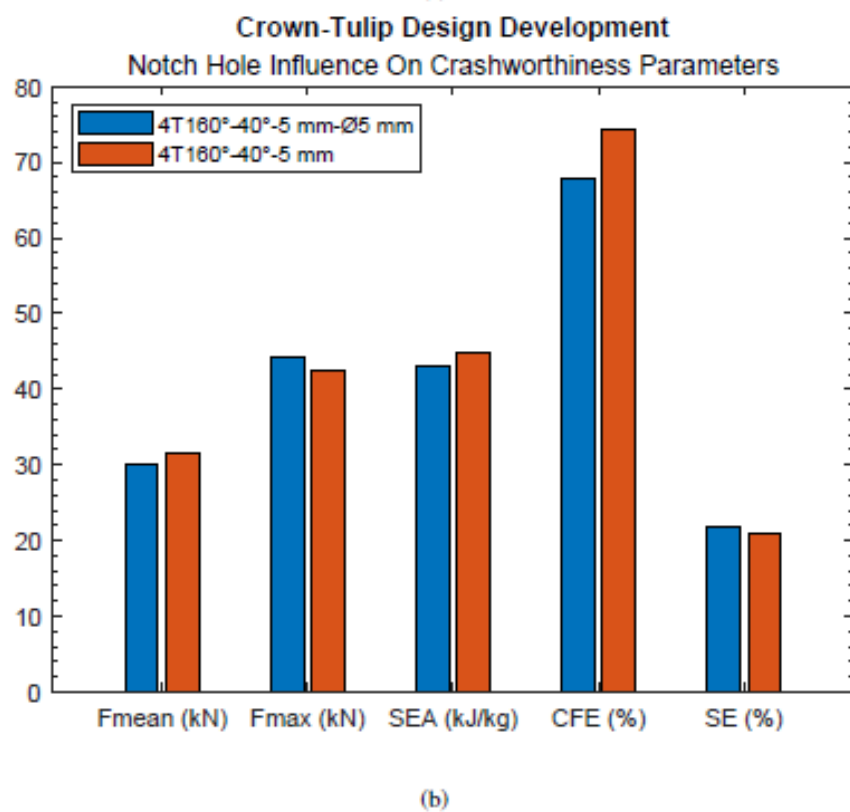


Figure 21. Numerical development of crown-tulip trigger: influence of an end notch hole of Ø5 mm on force response (a) and critical crashworthiness parameters (b) for the numerically designed 40°-5 mm crown notch with leading-edge 4T160° tulip geometry.

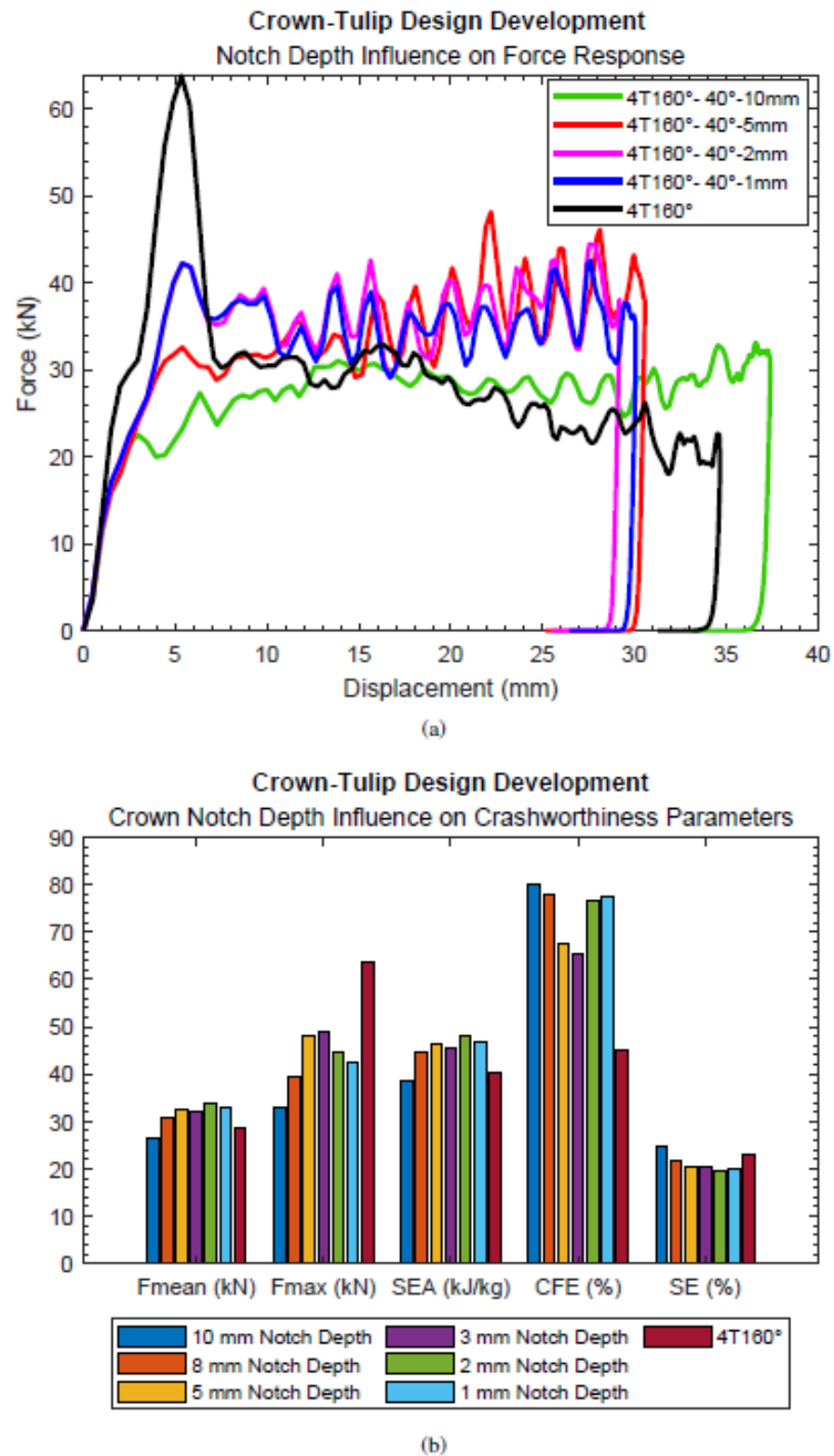


Figure 22. The 4T160°-40° crown-tulip trigger design development of the crown notch depth, illustrating the force response for select notch depths (10 mm, 5 mm, 2 mm, and 1 mm) (a) and critical crashworthiness parameters (b) for the varied notch depths from 10 mm to 1 mm, with comparison to the base 4T160° tulip trigger model response.

Both the 1 mm and 2 mm notch depths exhibit similar maximum crush forces of 43 kN and 44 kN, respectively. This minor deviation may be associated with the 2 mm mesh discretisation, which cannot accurately capture a notch depth below 2 mm. The trigger

discretisation is illustrated in the first row of Figure 23. For the notch depths of 5, 3, 2, and 1 mm, similar mean force oscillations are noted, with a standard deviation of 1.3 kN and an average of 32.5 kN. In general, as seen from the comparison of the 10 mm, 5 mm, and 2 mm notch depths, the crush distance exhibits a proportional relationship to the notch depth. Similarly to all previous studies, this is tied to the reduction in maximum crush force for the increased notch depth.

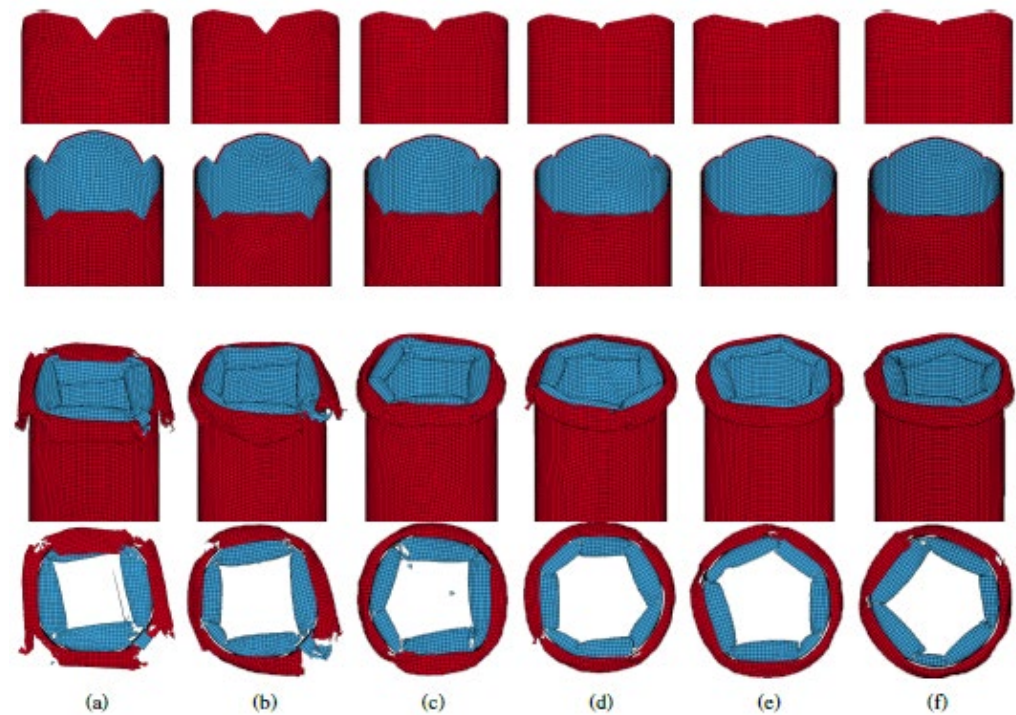


Figure 23. Model deformation comparison for tulip-crown trigger with varied notch depths at fixed tulip angle of 160° and notch angle of 40° , (a) 40° -10 mm Notch, (b) 40° -8 mm Notch, (c) 40° -5 mm Notch, (d) 40° -3 mm Notch, (e) 40° -2 mm Notch and (f) 40° -1 mm Notch.

The above-mentioned changes in force response on the critical crashworthiness parameters are presented in Figure 22b and summarised in Table 5, with comparison to the initial pure tulip trigger (4T160°). The inclusion of the crown notch improves the CFE, SEA, and mean crush force. It is noted that for the notch depths of 10, 8, 5, and 3 mm, the notch depth is proportional to the CFE. For further reduced notch depths of 2 mm and 1 mm, the CFE is increased to 77% and 78%, respectively. Excluding the behaviour of the 3 mm and 1 mm notch depth, the SEA and mean crush force are inversely proportional to the crown notch depth. The optimal notch depth, with respect to the crashworthiness behaviour, was identified as the 2 mm notch depth, as it exhibited the highest improvement in mean crush force of 18% in comparison to the equivalent pure tulip trigger (4T160°), with a 70% improvement in CFE, and 20% increase in SEA. This selection of the 1 mm notch depth is due to the 3% higher mean crush force for the 2 mm notch depth and 4% higher SEA.

Figure 23 illustrates the varied model deformations for the change in tulip crown notch depth for the 4T160°-40° Crown-Tulip trigger. For the notch depths of 10 mm and 8 mm, increased fragmentation and tearing of the outer petal fronds are seen. This produces a matching square deformation of the outer frond to the deformed inner shell layer. This propagates from the end of the crown notch due to the element deletion associated with the element deformation and subsequent failure. As the notch depth is reduced, the outer shell layers exhibit more circular frond formation, with reduced element deletion around the crown notch ends. Furthermore, the internal shell layers no longer form a square internal

peeling pattern as the notch depth is reduced. Overall, due to the numerical simplifications, all the models exhibit the inner and outer fronds, which progressively peel with failure around the tie-break interface, associated with the numerical modelling of mode 1 failure for CFRP crash tubes. Therefore, from the presented design development, the optimal crown notch depth of 2 mm is identified, and further analysis will be performed on the developed 4T160°-40°-2 mm crown-tulip trigger mechanism.

Table 5. Calculated critical crashworthiness parameters for varied crown notch depths for the 4T160°-40° crown-tulip trigger, with calculated delta relative to the baseline pure tulip 4T160° trigger.

Tulip-Crown Trigger Crashworthiness Parameters							
Notch Depth Influence, for 4T160°-40°							
	Energy (J)	Fm (kN)	Fmax (kN)	SEA (kJ/kg)	CFE (%)	SE (%)	Mass (g)
4T160°-40°-	994	27	33	38	80	25	26
10 mm	0%	−8%	−48%	−4%	78%	8%	4%
4T160°-40°-	994	31	39	44	78	22	22
8 mm	0%	7%	−38%	11%	73%	−7%	−10%
4T160°-40°-	993	33	48	46	68	20	21
5 mm	0%	13%	−25%	15%	50%	−12%	−14%
4T160°-40°-	992	32	49	45	65	21	22
3 mm	0%	12%	−23%	13%	45%	−11%	−12%
4T160°-40°-	992	34	44	48	77	19	21
2 mm	0%	18%	−30%	20%	70%	−16%	−17%
4T160°-40°-	994	33	43	47	78	20	21
1 mm	0%	15%	−33%	16%	72%	−13%	−14%
4T160	996	29	64	40	45	23	25

6.4. Crown-Tulip No. Tips

The final design development phase investigates the influence of the number of leading tulip tips on the combined crown-tulip trigger, with a leading crown angle of 160°, crown notch of 40°, and depth of 2 mm. For the increase in tulip tips, the no. of crown notches is also increased. A maximum of six tulip tips is compared to the developed 4T160°-40°-2 mm crown-tulip trigger and pure tulip triggers of 4T160° and 6T160° (see Figure 24).

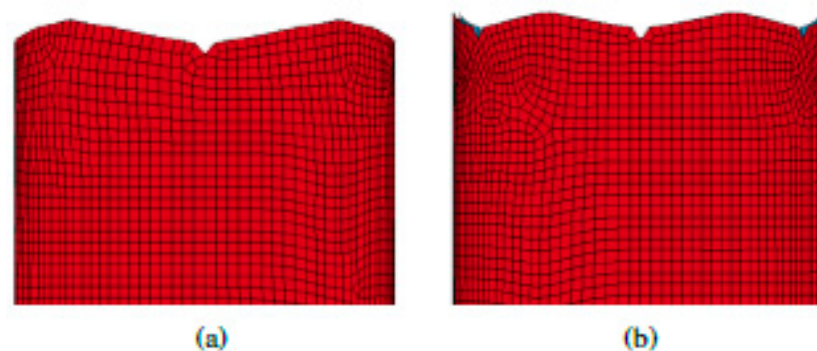


Figure 24. Discretisation of the developed crown-tulip trigger design, illustrating the geometrical difference between the 4-tip (a) and 6-tip (b) 160° leading tulip edge, 40°-2 mm crown notch trigger.

The force response exhibits similar initial crushing for the pure tulip and tulip crown combinations, for both four and six trigger tips, till a crush distance of approx. 1 mm as illustrated in Figure 25a. Beyond this point, the four-tip crown trigger model deviates due to a lower effective stiffness, as indicated by the gradient of the initial linear force response. Isolating the comparison between the crown-tulip triggers (NT160°-40°-2 mm),

by increasing the number of crown and tulip tips to six, the initial crushing peak (till crush distance of 5 mm) is reduced by 19% in comparison to the four-tip model variant.

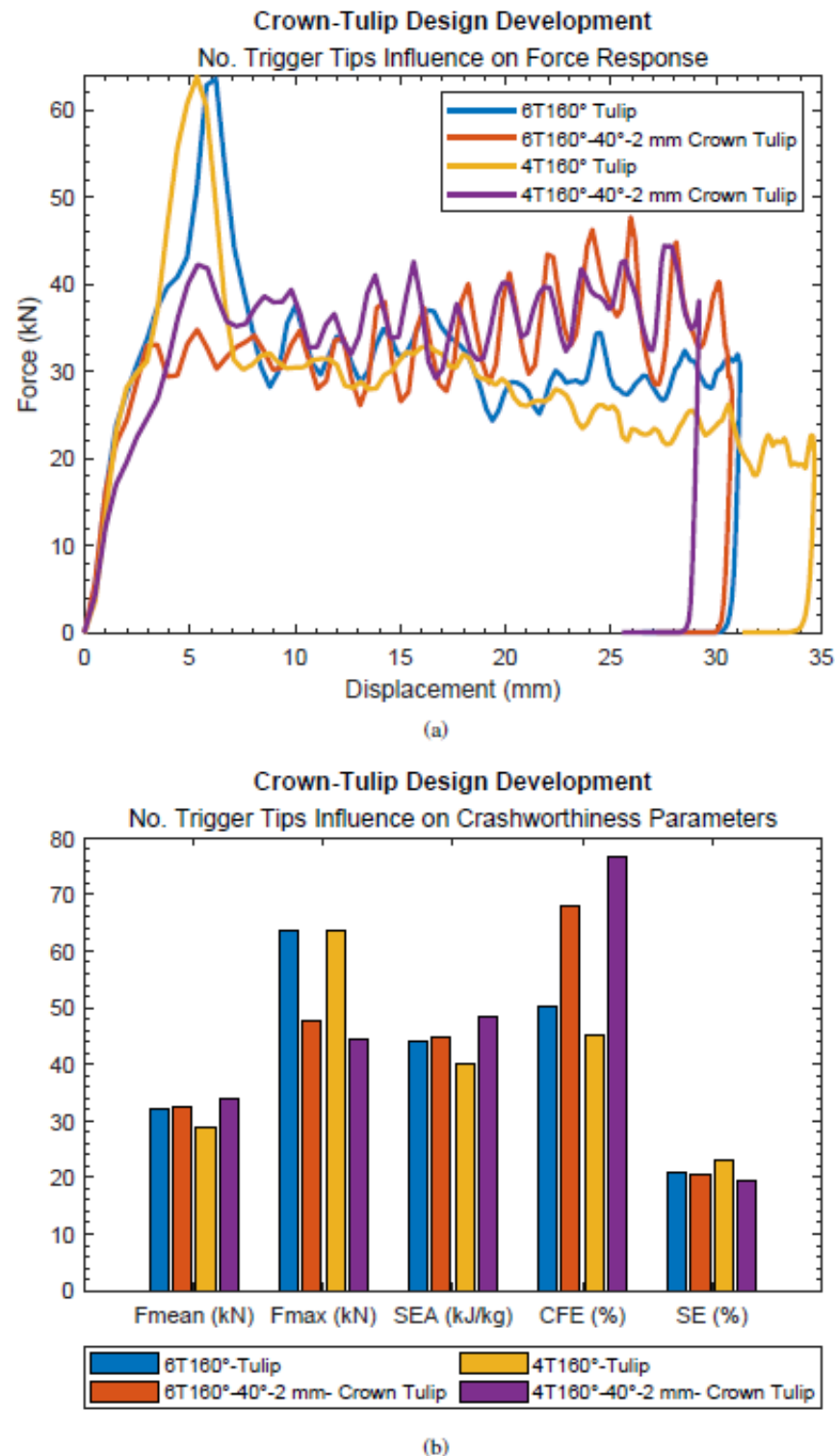


Figure 25. Crown-tulip trigger design development, illustrating the force response (a) and critical crashworthiness parameters (b) for the increase in the number of trigger leading edge and notch tips on the NT160°-40°-2 mm trigger design.

Following this phase, a similar local force oscillation is observed between the crown-tulip models for the varied tip numbers. This corresponds to the continued progressive peeling of the shell layers past the crown notch depth. The total crush distance increases by

approximately 11% due to the increased number of crown-tulip trigger tips. Increasing the no. of tulip tips and crown notches to six results in a 7% higher maximum crush force, in comparison to the 4T160°-40°-2 mm crown-tulip trigger. Compared to the pure tulip trigger behaviour, for the increase in the no. of tulip tips, a greater difference in the maximum crush force is noted with a reduced variance in the total crush distance.

Analysing the influence of the above-mentioned variance in the force response, for the increased no. of tulip tips and crown notches, on the critical crashworthiness parameters, is presented in Table 6. The calculated delta is shown, relative to the baseline of four trigger tips and crown notches. The mean crush force, SEA, and CFE exhibit an inversely proportional relationship to the no. of tulip tips and crown notches, as illustrated in Figure 25b. The 11% decrease in CFE, 7% decrease in SEA, and 5% decrease in mean crush force indicate that increasing the number of trigger tips and crown notches does not result in improved crashworthiness response. Therefore, with respect to the force response, crash tube deformation, and critical crashworthiness parameters, the optimal design develops trigger features, four tulip tips, and four crown notches.

Table 6. Calculated crashworthiness parameters for the increased number of trigger leading-edge tips for the developed crown-tulip trigger design, with a delta relative to the 4-tip model variant.

No. Crown-Notch Tips Influence, for T160°-40° Variant							
	Energy (J)	Fm (kN)	Fmax (kN)	SEA (kJ/kg)	CFE (%)	SE (%)	Mass (g)
6T160°-40°-2 mm	994	32	48	45	68	21	22
4T160°-40°-2 mm	992	34	44	48	77	19	21
Delta	0%	−5%	7%	−7%	−11%	5%	8%

7. Conclusions

The efficient numerical modelling approach enabled the design development of the tested critical 4T90° tulip and crown-tulip trigger mechanisms. It was established that the leading tulip trigger edge could be increased to 160°, increasing the initial linear crush gradient and subsequent energy absorption. This was found to be more effective with respect to SEA and mean crush force than increasing the number of tulip tips and subsequent crown notches. To achieve a sustained mean crush force and SE for the increased leading tulip angle, the incorporation of an optimal-sized crown notch was defined at an angle of 40° with a notch depth of 2 mm (4T160-40°-2 mm). The above-mentioned developed trigger design did not feature the experimentally tested end notch hole, as this was found to reduce the mean crush force, CFE, and SEA. The design developed a crown-tulip trigger that achieved an 8% higher mean force response than bevel-initiated triggers, without the high initial crush force (−39%) and, therefore, 12% higher SEA, which was not previously possible with pure tulip triggers. In conclusion, the developed crown-tulip trigger design (4T160°-40°-2 mm) outperforms the experimentally tested, preliminary crown-tulip concept (4T140°-20°-20 mm-Ø5 mm) and the optimal tulip trigger design (4T90°) in terms of crashworthiness response, as shown in Figure 26.

Although the configuration of significant triggers may enhance performance, it could potentially lead to an excessively long stroke and installation difficulties, which may limit their practical performance. This phenomenon is recommended to be studied in future work [26].

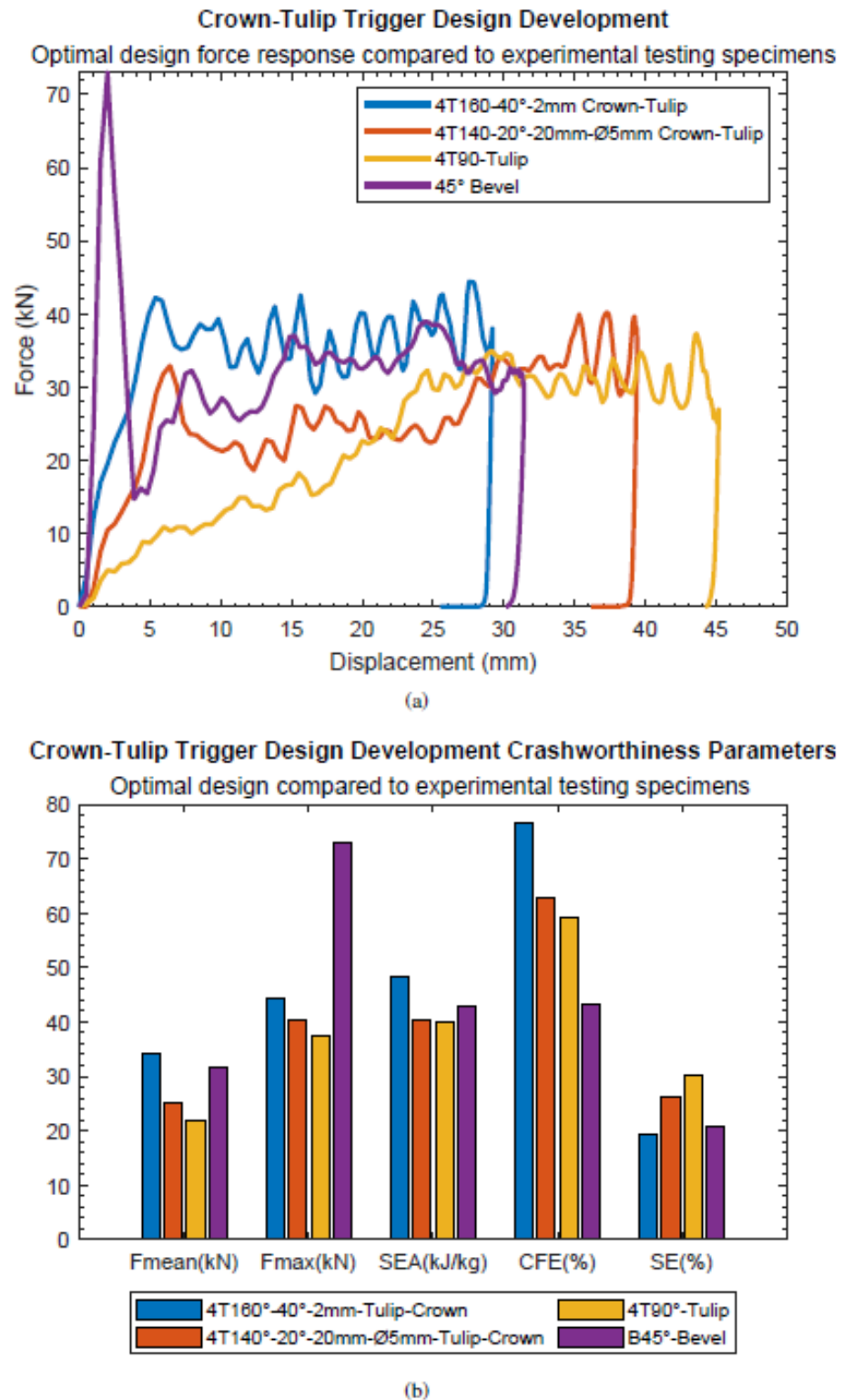


Figure 26. Crown-tulip trigger design development, illustrating the force response (a) and critical crashworthiness parameters (b) for the new optimal crown-tulip trigger design (4T160°-40°-2 mm), compared to the experimental testing specimens: 4T140° 20°-20 mm-Ø5 mm crown-tulip trigger, 4T90° tulip trigger, and 45° bevel trigger.

Author Contributions: Conceptualization, H.G.; Methodology, R.P.; Software, R.P.; Validation, R.P.; Formal analysis, R.P.; Writing—review & editing, H.G.; Visualization, H.G.; Supervision, H.G.; Project administration, H.G. All authors have read and agreed to the published version of the manuscript.

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Conflicts of Interest: The authors declare no conflict of interest.

Nomenclature

CFE	Crush force efficiency
SEA	Specific energy absorption
SE	Stroke efficiency
KE	Kinetic energy
ρ	Density
E	Young's modulus
F	Load
F _{max}	Initial maximum load
F _m	Mean load
G	Fracture toughness
G ₁₂	Shear modulus
G _{IC}	Mode-I interlaminar fracture toughness
G _{IIc}	Mode-II interlaminar fracture toughness
SEA	Specific energy absorption (kJ/kg)
t	Wall thickness
n	Number of axial splits
μ	Coefficient of friction
δ	Displacement
σ_n	Normal stresses
σ_s	Shear stresses
τ_s, S_2	Shear strength
NFLS	Normal failure stress
SFLS	Shear failure strength
SOFT	Softening reduction factor for material strength in crash front elements
TFAIL	Time step size criterion for element deletion
PARAM	Critical normal separation of the surface
4T90°	Trigger mechanism with 90-degree angle
4T140°-20°	Trigger mechanism with 140-degree, 20-degree, and 20-degree angles

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Crown-tulip trigger mechanisms to improve crashworthiness design of composite tubular structures

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