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Fracture behaviour of foam core sandwich structures with manufacturing defects using phase-field modelling

Xing-Yuan Miao\textsuperscript{a}, Renchao Lu\textsuperscript{b} and Xiao Chen\textsuperscript{c,}\textsuperscript{*}

\textsuperscript{a}Department of Energy Conversion and Storage, Technical University of Denmark, Risø Campus, Frederiksborgvej 399, 4000 Roskilde, Denmark
\textsuperscript{b}Helmholtz Centre for Environmental Research - UFZ, Leipzig, Germany
\textsuperscript{c}Department of Wind Energy, Technical University of Denmark, Risø Campus, Frederiksborgvej 399, 4000 Roskilde, Denmark

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\textbf{ABSTRACT}

This study investigates fracture behaviour of sandwich structures with foam core slits. These slits are typically machined in the foam core materials to improve manufacturability but inevitably lead to material discontinuities such as resin-starving regions or voids. Using the phase-field modelling method, which does not require the exact location of crack initiation or the crack path known as a prior, the complex fracture process of sandwich structures with different resin-filling slits in the foam core materials is numerically reproduced. We examine the effective stiffness, the peak force, the displacement at crack initiation, and the dissipated energy during fracture of the sandwich structure under shear loads following the ASTM C273 test standard. It is found that the sandwich structure with fully or partly resin filled slits in the foam core exhibits better fracture resistance than the ones with the intact foam and with unfilled slits. The sandwich structure with partly resin filled slits also shows good ductility due to the presence of voids. The effects of the number of slits, slit spacing and foam core density on the load-carrying capacity and fracture resistance are also examined, providing insights into fracture behaviour and damage tolerance of foam core sandwich structures with manufacturing defects.

\section{1. Introduction}

Foam core sandwich structures are extensively used in aerospace and wind energy industry such as in aircraft structures and wind turbine blades. The foam core materials in these structures are typically closed-cell and are machined with finishing features such as slits, grooves, or perforations due to manufacturing requirements. These foam core features can either facilitate resin flow or help the foam core materials better conform the curved tool surface during the resin infusion process. These surface finishing features lead to material discontinuities in the sandwich cores. Particularly, voids are formed if these features are not fully filled with resin, potentially resulting in crack initiations in the structures when loaded. Fig. 1 shows a foam core sandwich structure used in aerodynamic shells of a commercial wind turbine blade. Significant voids exist in the foam core slits which are partly resin filled despite one slit fully filled with resin. Fig. 2a shows the damaged sandwich structure of the same blade after static loading. It can bee seen that the crack propagates along the foam interface and migrates into the foam at the voids due to partly resin filling of the slits. Comparably, Fig. 2b shows a different fracture characteristic where the crack propagates along the interface of the sandwich structure whose slits are fully filled with resin.

The effects of these foam core slits on mechanical properties of sandwich structures have been previously studied. In terms of stiffness, the study from Rosemeier et al. (2018) suggested a potential material reduction in the blade design taking advantage of the resin uptake in the core grooves based on the improved homogenised material properties - which essentially treats the slits/grooves with resin filling as a mechanically favorable feature. The experimental investigation by Chen (2020) found that the partly resin-filled grooves, or slits, lead to large voids that cause crack migration of skin/core debonding. The adverse effects of the partly resin-filled foams are addressed and the associated challenges in the wind turbine blade design are highlighted. The studies from Massüger and Gätzi (2010); Fathi et al. (2013); Yokozeki and Iwamoto (2016) found the improvement of interfacial toughness due to the perforated and grooved cores. Truxel et al. (2006) found that sandwich specimens with grooved cores showed increased local fracture resistance and temporary crack arrest. The foam core features such as the resin starved grooves can also result in low face/core

\textsuperscript{*}Corresponding author at: Department of Wind Energy, Technical University of Denmark, Frederiksborgvej 399, 4000 Roskilde, Denmark.
E-mail address: xiac@dtu.dk (X. Chen).

ORCID(s): 0000-0001-6972-1541 (X. Miao); 0000-0002-5762-3036 (R. Lu); 0000-0001-6726-4068 (X. Chen)
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Figure 1: A cross sectional segment of a wind turbine blade with manufacturing defects in sandwich foam core materials. Most foam slits are partly filled with resin, creating prevailing voids in the core. Note that there is one foam slit that is fully filled with resin in this sandwich structure. This segment of the blade is not tested under load thus no structural damage is observed.

Figure 2: Observed fracture in sandwich foam core materials with the presence of manufacturing defects. Two specimens are cut from two blades which have been statically loaded to structural failure. (a) Crack propagation along the foam interface and crack migration into the foam material where partly resin filled slits exist. The specimen is from the same blade as the one presented in Fig. 1. (b) Crack propagation along the foam interface when the fully resin filled slits exist. Two specimens are cut from two blades which has been statically loaded to complete failure by Chen et al. (2014) and Chen (2020).
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| Greek symbols | Roman symbols |
|---------------|--------------|
| \( \gamma \)  | crack-surface density \([m^{-1}]\) | \( d \) | crack-phase indicator [-] |
| \( \Gamma \)  | crack surface | \( E \) | Young's modulus \([Nm^{-2}]\) |
| \( \varepsilon \) | small strain tensor [-] | dissipated energy during fracture \([J]\) |
| \( \varepsilon \) | length-scale parameter \([m]\) | \( F \) | load force \([m]\) |
| \( \mu \) | shear modulus \([Nm^{-2}]\) | \( F_{\text{peak}} \) | peak force \([N]\) |
| \( \nu \) | Poisson’s ratio [-] | \( g \) | material forces \([Nm^{-3}]\) |
| \( \sigma \) | stress \([Nm^{-2}]\) | \( G \) | strain energy release rate \([Nm^{-1}]\) |
| \( \Sigma \) | energy–momentum tensor or Eshelby tensor \([Nm^{-2}]\) | \( G_c \) | critical Griffith-type energy release rate \([Nm^{-1}]\) |
| \( \psi \) | Helmholtz free energy density \([Jm^{-3}]\) | \( I \) | second-order identity tensor [-] |
| \( \psi_{\text{me}} \) | mechanical part of Helmholtz free energy density \([Jm^{-3}]\) | \( K \) | bulk modulus \([Nm^{-2}]\) |
| \( \psi_{\text{surf}} \) | crack surface energy density \([Jm^{-3}]\) | \( K_{\text{eff}} \) | effective stiffness \([Nm^{-2}]\) |
| \( \Omega \) | spatial domain of a solid body | \( K_I \) | stress intensity factor of mode I \([Nm^{-3/2}]\) |
| \( \partial \Omega \) | Neumann boundaries of a solid body | \( n \) | outward unit normal vector [-] |
| \( \partial \Omega_u \) | Dirichlet boundaries of a solid body | \( u \) | displacement \([m]\) |
| (\( a \otimes b \)) | tensor product of \( a \) and \( b \) | \( u_C \) | displacement at crack initiation \([m]\) |
| (\( a \cdot b \)) | dot product of \( a \) and \( b \) | div() | divergence operator |
| (\( a \otimes b \)) | tensor product of \( a \) and \( b \) | grad() | gradient operator |
| (\( \langle \bullet \rangle \)) | signed Macauley brackets: \( \langle \bullet \rangle = [\bullet \mp |\bullet||/2 \right) | tr() | trace operator |

Fracture evolution in a composite structure can be complex due to the discontinuities of material properties and geometries, making the exact crack path unknown. Conventional fracture mechanics-based methods such as virtual crack closure techniques (VCCT) and cohesive zone models are thus not applicable to analysing a complex cracking process in which the crack path is not pre-defined as a prior. Because the simulations in this study are performed to simulate the cracking process with unknown crack paths, the chosen fracture model should be capable of simulating crack nucleation and propagation without prescribing discretisation-based directional energetic bias. In this regards, the phase-field model for fracture has shown promise in simulation of intricate crack growth topologies, e.g. Hofacker and Miehe (2013); Zhou et al. (2018); Yoshioka et al. (2019); Miao et al. (2019). A key highlight of this numerical approach is that it avoids the use of sophisticated numerical techniques to address the discontinuities. Newly initiated cracks are no longer treated as physical discontinuities in a discrete setting. Instead, a phase-indicator variable is introduced for numerically continuum approximation, realising a continuous and smooth transition between the undamaged and cracked material phases (Miehe et al., 2010b; Kuhn and Müller, 2010). Besides, phase-field approach does not require discretised elements conforming to the crack faces. Mesh induced nucleating/propagating bias can be greatly mitigated as no additional care at the discontinuities is needed. This is in particular important for accurate prediction of fracture behaviour in structures with irregular geometric features.

In this work, we numerically investigate how foam core finishing and resin filling affect the load-carrying capacity and fracture behaviour of sandwich structures under shear loading following the ASTM C273 test standard (ASTM, 2013). Using validated phase-field modelling techniques, we are able to simulate complex cracking processes of foam core materials with manufacturing defects due to finishing slits with different resin filling. This study is the first of its
kind to examine fracture behaviour of defected foam core sandwich structures using phase-field modelling - without the necessity to pre-define the exact location of crack initiation or the crack path along which the cracking process has to follow. The fracture characteristics, such as crack migration from the interface into the foam material when a crack front approaches a partly resin filled slit and the cracking along the interface with complete fracture of fully resin filled slits that have been observed from the post-mortem experimental investigation, are numerically reproduced. This work provides new insights into the entire fracture process of defected foam core sandwich structures, which are difficult to obtain using conventional fracture-mechanics based numerical approaches.

We model four representative structural configurations, i.e. the sandwich specimen with an intact foam core, a foam core featured with full slits, a foam core with fully or partly filled slits. Four mechanical performance indicators are used for comparison. The effects of the finishing features, the spacing between two neighbouring slits and material properties of foam cores on the fracture behaviour are examined. This paper is organised as follows. In Section 2, the basic concept of the phase-field approach to fracture is briefly described, as well as the model setup, assumptions and numerical settings. Simulation results of fractures in specimens with finishing slits are given and discussed in Section 3. Concluding remarks are made in Section 4.

2. Numerical modelling

2.1. Phase-field model for fracture

Phase-field model for fracture is originally formulated using variational methods for Griffith theory of brittle fracture based on energy minimisation (Francfort and Marigo, 1998; Bourdin et al., 2008; Miehe et al., 2010b). Crack evolution is determined by the Griffith energy-based criterion. The phase-field approach transforms the discontinuity at the discrete lower-dimensional crack interface into a continuous diffusive setting. Firstly, we define $\Omega \subset \mathbb{R}^D$ the reference configuration of a material body in dimension $D \in \{2, 3\}$, and $\partial \Omega \subset \mathbb{R}^{D-1}$ the external boundary including Dirichlet $\partial \Omega_u$ and Neumann $\partial \Omega_t$ boundaries in pure mechanical settings. The assigned external boundary constraints hold $\partial \Omega_u \cup \partial \Omega_t = \partial \Omega$ and $\partial \Omega_u \cap \partial \Omega_t = \emptyset$. Then, we let $\Gamma \subset \mathbb{R}^{D-1}$ be the embedded sharp crack surface as shown in Fig. 3a. Through introducing a phase-indicator variable $d$ which varies from 1 (intact medium) to 0 (fully damaged), and defining the width of the crack phase by a length-scale parameter $2\varepsilon$, we can regularise the sharp crack surface to a diffusive topology, creating a transition phase between the damaged and the intact part of material, as shown in Fig. 3b.

![Figure 3: A solid body with: (a) a sharp crack; (b) a phase-field approximation of the crack surface, intact-damaged phase transition over a length scale $2\varepsilon$.](image)

Phase-field fracture model can simulate crack growth without a prior knowledge of its cracking path, addressing crack initiation and branching problems (Zhou et al., 2018; Tanné et al., 2018). Besides, The numerical implementation of the phase-field model can be done with a standard finite element discretisation. The crack field is treated as an extra nodal degree of freedom.

The theoretical formulations of the presented phase-field model is written in Appendix A, along with two linear elastic fracture mechanics benchmarks for model validation given in Appendix B. A more detailed description of the
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phase-field formulation for brittle fracture can be found in Miao (2020).

2.2. Specimen geometry

Fig. 4 shows four configurations of the foam core sandwich structures which are investigated in this work. Fig. 4a shows a sandwich structure with a foam core sandwiched between two steel plates. The foam can be intact or have slits. Fig. 4b illustrates a slit placed in the centre of the foam upon the sandwich structure in Fig. 4a. The slit has the same height as the foam and a width of 2 mm. A resin material may be fully or partly infused into this slit, as shown in Fig. 4c and Fig. 4d. A 1.2 mm foam interface layer is added between the foam and the steel plate. The geometric sizes of components are annotated in Fig. 4a. The loading line passes the diagonal of the foam core material following the ASTM C273 test standard (ASTM, 2013). The material properties are listed in Table 1.

![Diagram of specimen geometry](image)

**Figure 4:** Foam core sandwich structures: (a) an intact foam core sandwiched between two steel plates; (b) the foam with a full slit; (c) the foam with resin filled in the slit; and (d) the foam with resin partly filled in the slit. The loads are applied on two steel plates in such a way that loading line aligns with the diagonal of the foam core material following the ASTM C273 test standard as shown in Fig. 4a.

| Materials         | Young’s modulus $E$ / GPa | Poisson’s ratio $v$ | Fracture toughness $G_c$ / Nm$^{-1}$ | References                  |
|-------------------|----------------------------|----------------------|---------------------------------------|-----------------------------|
| Steel plate       | 210                        | 0.28                 | 10                                    | -                           |
| PVC foam I (100 kg m$^{-3}$) | 0.12                      | 0.31                 | 0.89                                  | Saenz et al. (2011)         |
| PVC foam II (45 kg m$^{-3}$) | 0.05                      | 0.40                 | 0.32                                  | Poapongsakorn and Carlsson (2013) |
| Foam interface I$^a$ | 0.06                      | 0.31                 | 0.45                                  | -                           |
| Foam interface II | 0.03                       | 0.40                 | 0.18                                  | Berggreen et al. (2018)     |
| Resin             | 3                          | 0.30                 | 1.14$^b$                              | -                           |

**Table 1**
Material properties of components.

*Note:* $^a$ Assumed material parameters. $^b$ $G_c$ calculated from stress intensity factors and plane-strain Young’s modulus.

2.3. Model assumptions

Based on the material and the fracture characteristics of the investigated foam core materials in this work, we make the following assumptions:

1. Foam core materials are assumed to be isotropic. Foam core materials typically show some extent of anisotropy due to their intrinsic foaming process. The thickness direction, or the cell rise direction, usually has a large stiffness and strength properties compared to the in-plane direction. The anisotropy is more significant when the foam density is large. For low-density foam core materials, Gdoutos et al. (2002) showed that the anisotropy is insignificant and the low-density foam core materials can be treated as to be isotropic.
Experimental observations of the fracture surfaces of PVC foam materials reported in Poapongsakorn and Carlsson (2013) suggested that the fracture of low-density foam core materials exhibits a brittle manner. Thus, the foam core materials can be treated as brittle materials, and we model linear elastic material behaviour and brittle fracture for the foam cores.

The interface properties of the 100 kgm$^{-3}$ foam are assumed. The Young’s modulus and the fracture toughness are 0.50 times of those of the foam core material. This reduction factor is comparable to 0.56 as found in the 45 kgm$^{-3}$ foam. The Poisson’s ratio of the interface is the same as that of the foam core.

### 2.4. Numerical settings

We simulate the specimens in two dimensions. Displacement-controlled loads are used in the simulations. A discretisation of 67,748 elements is applied for a representative specimen shown in Fig. 5. The main matrix of computational domain is discretised by structured quadrilateral plane-strain elements. Unstructured quadrilateral elements are used for the filled resin as well as the surrounding domain. The mesh resolution of the foam layer and the steel plates is set to 0.6 mm, and the filled resin set to 0.015 mm. To avoid the likely mesh dependency problem, the element size is set to half of the material-specific regularisation length (Miehe et al., 2010b,a). Note that the regularisation lengths of the foam core material and the resin are set equal to their internal characteristic lengths. According to Bourdin et al. (2008), the fracture toughness is amplified by a factor $1 + \frac{h}{4\ell}$. Effective fracture toughness values of the materials used in simulations are calculated as $G_{\text{eff}}^c = G_c \left( 1 + \frac{h}{4\ell} \right)$.

The discretised linear momentum balance equation and phase-field equation constituting the whole coupled differential-algebraic equation system are solved in a staggered fashion. In setting the staggering error control of the equation system, a relative error tolerance of $10^{-6}$ and $10^{-3}$ is adopted respectively as the global convergence conditions of the displacement field and the phase field. In the staggered solution procedure, the Newton-Raphson method is employed for iteratively solving the algebraic system of the linear momentum balance equation and the phase-field equation, with a relative error tolerance of $10^{-6}$ and $10^{-3}$ for each field.

![Figure 5: Spatial discretisation of a representative specimen which consists of a partly resin filled slit. The meshes are made of unstructured quadrilateral elements for the filled resin as well as the surrounding domain, and of structured quadrilateral elements for the remaining part of the specimen.](image)

### 3. Results and discussions

In this section, we model specimens with prescribed structural configurations. The characteristics of the global force-displacement response of the specimen are obtained, and the fracture process in each specimen is visualised by phase field contours at different loading points.

#### 3.1. Specimen with an intact foam

The sandwich structure with an intact 100 kg m$^{-3}$ foam core shown in Fig. 4a is presented as the reference case. The global force-displacement response is characterised by four distinct stages as shown in Fig. 6. The phase-field contours of the specimen in each stage are shown in Fig. 7. After an initial stage of linear structural response, the specimen shows nonlinear behaviour up to its peak load at which a crack initiates at the bottom interface layer and propagates along. Due to the presence of the crack, the load-carrying capacity of the specimen is significantly decreased. The global force-displacement curve is featured with a sudden drop of the applied load. With the propagation of the crack, the global force-displacement curve shows a post-peak nonlinear behaviour that the previous sudden load drop is interrupted. This is due to the considerable change of load conditions as can be seen from the rotation deformation in the specimen when the crack propagates to the mid-span, as shown in Fig. 7c. Further loading leads to a complete fracture of the bottom interface layer which results in a sudden drop of the applied load.
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**Figure 6:** Global force-displacement response of the reference specimen with defined effective bulk modulus $K_{\text{eff}}$, marked peak force $F_{\text{peak}}$, critical displacement at the crack initiation $u_C$, and dissipated energy during the fracture $E_{\text{Disp}}$.

**Figure 7:** Fracture process of the reference specimen illustrated using phase field contours, showing crack initiation (a); states of crack evolution at the marked points in the force-displacement curve (b) and (c); final fracture of the specimen (d).

Particularly, to quantify the structural response to fracture of the modelled specimen, we define four mechanical performance indicators:

- $K_{\text{eff}}$ represents the effective stiffness of the specimen at the very early stage where the specimen responds linearly to the applied load.

- $F_{\text{peak}}$ is the peak force, which is used to determine the load-carrying capacity of the specimen.

- $u_C$ is the displacement at crack initiation, which is defined to assess the ductility of the specimen before cracking.

- $E_{\text{Disp}}$ represents the dissipated energy during fracture, which is defined to estimate the fracture resistance of the specimen.
3.2. Specimen with a full slit

For comparison, the global force-displacement response of the specimen with a fully resin starved region, or a full slit is plotted against the one of the reference specimen, as shown in Fig. 8. An obvious observation is that the two specimens respond to the applied load similarly except that the post-peak response curve after the sudden load drop is flatter in the full slit specimen than the one in the reference specimen. This observation is interesting and it is reasonable by examining the fracture process of the full slit specimen through phase-field contours as shown in Fig. 9. Particularly, after the crack has propagated through the entire interface layer at the left-hand side of the full slit, a new damage zone has to be formed before the crack can propagate into the interface layer at the right-hand side of the full slit. The formation of the new damage zone in the interface layer requires additional energy compared to the propagation of the existing crack in the interface – which is reflected by a flatter curve in the post-peak force-displacement response in the full slit specimen. After the formation of a new damage zone, the crack propagates along the interface layer at the right side of the slit, leading to the final failure of the entire specimen.

![Figure 8](image_url)

**Figure 8:** Comparisons of force-displacement responses of the reference specimen (blue solid line), the specimen with a full slit (green dash line), the specimen with full resin filling (yellow dot dash line), and the specimen with a partly resin filled slit (red dot line).

3.3. Specimen with full resin filling

The specimen with full resin filling, or a resin insert, shows a significantly higher peak load compared to the reference specimen as shown in Fig. 8. This indicates that the ultimate strength of the specimen is enhanced by resin filling. The resin insert is essentially a shear lock. The crack initiates at the interface just like the case in the reference specimen and propagates along the interface layer until it approaches the resin insert. Due to the fracture of the interface layer at the left-hand side of the specimen, the resin insert has to take more shear force and a new damage zone is formed at the root of the resin insert as seen in Fig. 10b. Due to much larger fracture toughness of the resin compared to the interface, the damage of the resin insert requires more energy than that of the interface layer. This results in an increase of the post-peak response curve right after the sudden load drop in the specimen. After complete fracture of the resin insert, the crack continues to propagate along the interface layer and the specimen loses structural integrity as shown in Fig. 10d.
3.4. Specimen with a partly resin filled slit

The specimen with a partly resin filled slit in the foam shows a multi-step post-peak behaviour comparing to the post-peak responses of the other three specimens, see Fig. 8. Each step is characterised by its distinct damage mechanism during the crack propagation. When the crack approaches the half resin insert, the crack starts to diverge from its original path along with the interface layer into the foam, as shown in Fig. 11b and Fig. 11c. The junction of the void and the resin becomes highly stressed, leading to a new damage zone which starts to progress into a crack.
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at the right-hand side interface layer even though the left-hand side interface layer is still not completely fractured, see Fig. 11b. This particular failure process is reflected by two-step post-peak response curve after the sudden load drop from the peak load. Continuing loading fractures the specimen along the interface layer and some foam materials remain attached to the resin after complete fracture of the specimen.

![Figure 11](image-url)

**Figure 11:** Fracture process of the specimen with a partly resin filled slit illustrated using phase field contours, showing crack initiation (a); state of crack propagation in the interface at the marked point in the graph (b); crack path diversion between the foam and the interface (c); final fracture of the specimen (d).

Table 2 summarises the mechanical performance indicators of the above four specimens. Comparing to the reference specimen in which the foam is intact, the specimen with a slit in the foam exhibits similar macroscopic fracture toughness and mechanical response to the loads. The decrease of the effective bulk modulus of the specimen is minor, this of course depends on how many slits are embedded in the specimen. The more slits are in the specimen, the lower the effective bulk modulus of the specimen will be. The specimen with a resin insert in the foam exhibits superior load-carrying capacity and fracture resistance due to the shear lock effect. However, the ductility of the specimen is reduced due to the much higher stiffness of the resin compared to the foam.

| Configurations     | Effective stiffness $K_{\text{eff}}$/MPa | Peak force $F_{\text{peak}}$/N | Displacement at crack initiation $u_c$/mm | Dissipated energy during fracture $E_{\text{Disp}}$/J |
|--------------------|----------------------------------------|-------------------------------|------------------------------------------|---------------------------------|
| Intact foam        | 116.6 (100.0%)                         | 222.9 (100.0%)                | 0.8498 (100.0%)                          | 0.0386 (100.0%)                |
| Full slit          | 115.8 (99.30%)                         | 223.1 (100.1%)                | 0.8677 (102.1%)                          | 0.0396 (102.6%)                |
| Fully filled slit  | 129.5 (111.1%)                         | 247.2 (110.9%)                | 0.8294 (97.60%)                          | 0.0826 (214.0%)                |
| Partly filled slit | 121.9 (104.5%)                         | 231.7 (103.9%)                | 0.8594 (101.1%)                          | 0.0475 (123.0%)                |

Comparing to the fully resin inserted case, the specimen with a partly resin filled slit insert in the foam exhibits moderate load-carrying capacity and fracture resistance, while maintains good ductility of the specimen due to the existence of the voids to absorb the strain energy and allow nonlinear deformation.

### 3.5. Specimen with two partly resin filled slits

From the comparisons of the above four structural configurations, we can see that the specimen with a partly resin filled slit insert in the foam exhibits superior mechanical performance in all aspects that we examine in this work.
Thus, we further investigate how the number of slits, the spacing between two neighbouring slits, as well as the material properties of the foam core material affect the fracture process in the specimen. We examine two cases: (a) a specimen with two partly resin filled slits insert in the foam where the second slit is located in 3/4 of the foam length and (b) the second slit located in 5/8 of the foam length. Namely, the interval between the two slits of case (b) is shorter than that of case (a). We investigate two foam core materials with different densities, respectively.

As can be seen in Fig. 12, the global force-displacement response of the specimen with two partly resin filled slits is plotted against the single slit case. Firstly, we can see that the double slits cases show similar post-peak behaviour as the single slit case. In case (a), the cracking path experiences four diversions, as shown in Fig. 13b. When the crack approaches the first slit insert, the original path along with the interface diverges into the foam and a new damage zone is formed at the highly stressed junctions of the void and the resin. After this damage zone progresses into a crack, the crack path turns back to the bottom interface layer until it approaches the second slit insert where the path diverges into the foam again. Comparing the post-peak dissipated energy of case (a) to that of the single slit case, an additional energy is needed for crack propagation from the first slit in the centre of the specimen to the second slit, see Fig. 12 and Table 3.

![Figure 12: Comparisons of force-displacement responses of the specimen with a single partly resin filled slit (blue solid line), the specimen with two partly resin filled slits with long spacing (green dash line), the specimen with two partly resin filled slits with short spacing (yellow dot dash line).](image)

| Configurations                          | Effective stiffness $K_{eff}$ / MPa | Peak force $F_{peak}$ / N | Displacement at crack initiation $u_c$ / mm | Dissipated energy during fracture $E_{Disp}$ / J |
|----------------------------------------|-------------------------------------|---------------------------|------------------------------------------|-----------------------------------------------|
| One slit                               | 121.9 (100.0%)                      | 231.7 (100.0%)            | 0.8594 (100.0%)                          | 0.0475 (100.0%)                              |
| Two slits with long spacing            | 119.2 (97.78%)                      | 222.5 (99.03%)            | 0.8618 (100.3%)                          | 0.0610 (128.4%)                              |
| Two slits with short spacing           | 118.2 (96.96%)                      | 221.3 (95.51%)            | 0.8642 (100.6%)                          | 0.0514 (108.2%)                              |

In case (b), i.e. the short spacing case, when the new damage zone at the first slit progresses into a crack, this crack propagates straightly through the foam due to the relative short interval between the two slits, see Fig. 13c. The post-peak force-displacement response in this case suggests that the energy needed to propagate the crack between the slits is lower than that needed in case (a), see Table 3. This is because the cracking path between the two slits is shorter than that in the long spacing case, compare Fig. 13b and Fig. 13c.

### 3.6. Specimen with a 45 kg m$^{-3}$ foam core

We investigate the impact of the material properties of foam cores on fracture behaviour by modelling the specimen with partly resin filled slits. Again, the global force-displacement response of the specimen with two partly resin filled
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(a) One slit
(b) Two slits with long spacing
(c) Two slits with short spacing

Figure 13: Comparisons of fracture processes of the 100 kg m\(^{-3}\) foam core specimen with partly resin filled slits illustrated using phase field contours (one slit (a), two slits (b) and (c)). The spacing between the two slits equals 1/4 and 1/8 of the foam length, respectively.

Figure 14: Force-displacement responses of specimens with a 45 kg m\(^{-3}\) foam core material with partly resin filled slits. The blue solid line is used for the specimen with a single slit, the green dash line for two slits with long spacing, and the yellow dot dash line for two slits with short spacing.

Different from the 100 kg m\(^{-3}\) foam case, the crack initiation occurs in the region around voids in all the three specimens with 45 kg m\(^{-3}\) foam as can be seen in Fig. 15. In the single slit case, the crack first propagates from the void to the left part of the specimen along with the upper interface layer. In the mean time, after the formation of the new damage zone at the right junction of the void and the resin, the crack migrates from the foam to the interface and propagate straightly through the bottom interface, finally fracturing the specimen.

For the two slits with long spacing, the crack always initiates in the interface between two voids and it propagates along the interface. In the short spacing case, the crack path is similar to the single slit case. Particularly, the post-peak dissipated energy of the two slits case is nearly twice the energy of the single slit case, cf. Table 4. This is because when the density of the foam core material is too low, the mechanical properties of the foam play the dominant role in the fracture behaviour of the specimen. Double damage zones are formed in the specimen with two slits, the energy
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Figure 15: Comparisons of fracture processes of the 45 kg m$^{-3}$ foam core specimen with partly resin filled slits illustrated using phase field contours (one slit (a), two slits (b) and (c)). The spacing between the two slits equals 1/4 and 1/8 of the foam length, respectively.

needed to form these damage zones is doubled when the interaction between the two slits can be negligible.

Table 4

| Configurations (partly filled, 45 kg m$^{-3}$) | Effective stiffness $K_{\text{ef}}$ / MPa | Peak force $F_{\text{peak}}$ / N | Displacement at crack initiation $u_c$ / mm | Dissipated energy during fracture $E_{\text{Disp}}$ / J |
|---------------------------------------------|------------------------------------------|----------------------------------|--------------------------------------------|-----------------------------------------------|
| One slit                                    | 58.92 (100.0%)                           | 97.83 (100.0%)                  | 0.8313 (100.0%)                            | 0.0038 (100.0%)                               |
| Two slits with long spacing                 | 58.69 (99.61%)                           | 92.19 (94.23%)                  | 0.7651 (92.04%)                            | 0.0076 (200.0%)                               |
| Two slits with short spacing                | 57.77 (98.05%)                           | 91.48 (93.51%)                  | 0.7795 (93.77%)                            | 0.0071 (186.8%)                               |

4. Concluding remarks

In this work, we explore the effects of finishing features of foam core materials on the fracture behaviour of foam core sandwich structures with manufacturing defects. Several representative structural configurations of practical interest are modelled and the fracture process in each structure as well as structural response to shear loads are obtained. We found:

(1) The sandwich structure with a foam core slit that is fully filled with resin shows superior fracture resistance to any other sandwich structures with different resin filling features. The superior fracture resistance is found to be due to the reinforcement of the resin but at a cost of ductility loss of the sandwich structure.

(2) The mechanical performance of the sandwich structure with one foam core slit that is unfilled with resin is not necessarily inferior than the one with an intact foam core. However, increasing the number of the unfilled slits decreases the mechanical performance of the sandwich structure as expected.

(3) The sandwich structure with one partly resin filled slit in the foam core exhibits good performance in both fracture resistance and ductility. The crack propagates along the interface and migrates into the foam material due to the presence of the resin, resulting in a larger dissipated energy during the fracture process. In the mean time, the existence of a void in the partly resin filled region allows relatively large deformation before crack initiation, which increases the ductility of the sandwich structure.
The sandwich structure with two partly resin filled slits in the foam core has higher dissipated energy in the fracture process than the one with a single partly resin filled slit in the foam core. Particularly, with two slits both partly filled with resin, the specimen with 45 kg m\(^{-3}\) foam core, dissipates nearly double energy compared with the specimen with 100 kg m\(^{-3}\) foam core regardless the spacing of two slits.

These findings have identified the possibility and the pathway to fracture mitigation through crack process control utilizing inevitable manufacturing-induced defects in foam core sandwich structures. It should be noted that more studies are needed to examine parametric effects on fracture behaviour and its implication to the structural design. The future work may include the investigation on the damage tolerant design by controlling crack paths using micro-scale features such as grooves, slits, or inclusions in the foam core.

Appendix A Theoretical formulations

A.1 Phase-field equations

We formulate the Helmholtz free energy density function for solid as the combination of mechanical and regularised surface-energy contributions:

\[
\psi (\varepsilon (u)) = \psi_{m}(\varepsilon (u)) + \psi_{\text{surf}}(\gamma) \quad \text{(A.1)}
\]

The elastic energy density is decomposed into volumetric and deviatoric contributions to identify the degradation of the elastic energy, i.e. only expansive volumetric and deviatoric strain energies are dissipated with the initiation of a crack (Miao et al., 2019).

\[
\psi_{m}(\varepsilon) = (d^2 + k) \psi^+_{m0}(\varepsilon) + \psi^-_{m0}(\varepsilon) \quad \text{(A.2)}
\]

with

\[
\psi^+_{m0}(\varepsilon) = \frac{1}{2} K (\text{tr} (\varepsilon))^2 + \mu (\varepsilon) : (\varepsilon)^{\text{Dev}}
\]

\[
\psi^-_{m0}(\varepsilon) = \frac{1}{2} K (\text{tr} (\varepsilon))^2
\]

where \(k\) represents a residual stiffness used in numerical calculations to ensure a residual elastic energy density in the fully damaged state. \(K\) is the bulk modulus, and \(\mu\) the second Lamé constant/shear modulus.

The regularised surface-energy density function reads (Bourdin et al., 2008; Miehe et al., 2010b; Miao, 2020)

\[
\psi_{\text{surf}}(\gamma) = G_c \gamma \quad \text{(A.4)}
\]

with

\[
\gamma (d, \text{grad} d) = \left[ \frac{1}{4\varepsilon} (1 - d)^2 + \varepsilon \text{grad} d \cdot \text{grad} d \right] \quad \text{(A.5)}
\]

where \(G_c\) is the critical Griffith-type energy release rate, \(\gamma\) the crack-surface density.

We then write an approximation of the Helmholtz free energy density of a fractured body as

\[
\psi (\varepsilon (u), d, \text{grad} d) = (d^2 + k) \psi^+_{m0}(\varepsilon (u)) + \psi^-_{m0}(\varepsilon (u)) + G_c \left[ \frac{1}{4\varepsilon} (1 - d)^2 + \varepsilon \text{grad} d \cdot \text{grad} d \right] \quad \text{(A.6)}
\]

The equilibrium equations associated with mechanical and phase-field arguments can be respectively derived from the variation of \(\psi (\varepsilon (u), d, \text{grad} d)\) with respect to the primary variables \((u, d)\) and applying the Gauss theorem (Miao, 2020). Then, the linear momentum equation without body forces writes

\[
\text{div} \left[ (d^2 + k) \sigma_{0}^{\text{+}} + \sigma_{0}^{\text{-}} \right] = 0 \quad \text{(A.7)}
\]

and the phase-field equation

\[
-2d \psi^+_{m0}(\varepsilon) + G_c \left[ \frac{1}{2\varepsilon} (1 - d) + 2\varepsilon \text{div} (\text{grad} d) \right] = 0 \quad \text{(A.8)}
\]

A detailed derivation of the partial differential equations and the respective Neumann-type boundary conditions can be found in our previous work (Miao et al., 2017, 2019; Miao, 2020).
A.2 Material forces and energy release rates

In our numerical simulations with OGS, the energy release rate can be quantified by the component of the material force vector pointing into the crack-growth direction (Mueller et al., 2002; Kuhn and Müller, 2010, 2016; Anderson, 2017). This approach is strongly related to the J-integral concept (Rice, 1968). To provide more context here, we briefly review the theory and the formulation of the material forces.

The theory of material forces, also called configurational forces, is a continuum mechanical approach to characterise the energetic impact of discontinuities (defects, inhomogeneities, phase transitions, etc.) which can move in material space. The pioneering study dates to Eshelby’s work on the driving forces that he introduced to describe the motion of material defects (Eshelby, 1951). The material forces link the potential configuration change of a discontinuity in the material to the associated energy change of the system. It is a versatile approach to study the discontinuities in various material models (elasticity, visco-elasticity, plasticity, etc.) (Maugin, 1995; Gross et al., 2003; Gurtin, 2008; Maugin, 2016).

For simplicity, we keep the medium homogeneous and without body forces. Thus, with \( \psi (\epsilon (u), d, \text{grad } d) = g(d)\psi_0 (\epsilon (u)) + G_c \gamma (d, \text{grad } d) \) and \( \sigma = g(d)\sigma_0 \):

\[
\text{grad } \psi = \sigma : \text{grad } \epsilon (u) + \frac{\partial \psi}{\partial d} \text{grad } d + \frac{\partial \psi}{\partial (\text{grad } d)} \text{grad } (d) \text{grad } d
\]

\[
0 = - \text{div } (\psi I - \text{grad }^T u \sigma) - \text{div } \sigma \text{grad } u + \frac{\partial \psi}{\partial d} \text{grad } d + \text{div } \left( \text{grad } d \otimes \frac{\partial \psi}{\partial (\text{grad } d)} \right) - \text{div } \left( \frac{\partial \psi}{\partial (\text{grad } d)} \right) \text{grad } d
\]

where several symmetries have been exploited. Using the equilibrium conditions in the absence of body forces, and the usual definition of the Eshelby stress we find:

\[
0 = \text{div } \Sigma - \left[ \frac{\partial \psi}{\partial d} - \text{div } \left( \frac{\partial \psi}{\partial (\text{grad } d)} \right) \right] \text{grad } d - \text{div } \left( \text{grad } d \otimes \frac{\partial \psi}{\partial (\text{grad } d)} \right) = 0, \text{ see Eq. (4) in Miao et al. (2019)}
\]

\[
= \text{div } \Sigma + g
\]

This shows that

\[
\Sigma = [g(d)\psi_0 (\epsilon (u)) + G_c \gamma (d, \text{grad } d)] I - g(d) \text{grad }^T u \sigma_0
\]

\[
g = - \text{div } \left( \text{grad } d \otimes \frac{\partial \psi}{\partial (\text{grad } d)} \right) = \text{div } \left( \frac{\partial \psi}{\partial (\text{grad } d)} \right) \text{grad } d + \frac{\partial \psi}{\partial (\text{grad } d)} \text{grad } (d) \text{grad } d
\]

with \( \frac{\partial \psi}{\partial (\text{grad } d)} = G_c \frac{\partial \gamma}{\partial (\text{grad } d)} = G_c 2 \epsilon \text{grad } d \)

In other words, the material forces are driven by the surface contribution \( \psi (\text{grad } d) \) of the energy functional, i.e. how the system topology evolves is dictated by how the energetic state depends on the phase boundary (designated by \( \text{grad } d \)).

Assuming quasi-static and body force free conditions, the J-integral can be formulated as

\[
J = \int_{\Omega} \frac{\partial \psi}{\partial x_t} dV - \int_{\partial \Omega} \sigma n \frac{\partial u}{\partial x_t} dA = \int_{\partial \Omega} \left( \psi 1 n - \sigma n \frac{\partial u}{\partial x_t} \right) dA
\]

(A.11)

With the material forces on the crack tip written as (Gross et al., 2003; Kuhn and Müller, 2010, 2016)

\[
G_{ct} = - \int_{\Omega} \text{div } \Sigma dV = - \int_{\partial \Omega} \Sigma n dA
\]

(A.12)

the scalar J-integral then equals the tangential component of \( G_{ct} \)

\[
J = -G_{ct} \cdot n_t
\]

(A.13)
Appendix B  Validation tests

Before performing the simulations of fracture processes in the foam core sandwich structures investigated in this work, the presented phase-field model is verified using two linear elastic fracture mechanics tests. The test objects are of simple geometry but have some similar structural features (i.e. voids and inclusions) with the target structures, such that we can also gain a preliminary insight into the fracture behaviour of a structure with defects.

B.1 Validation test 1: Symmetric cracks emanating from an elliptical hole

Crack evolution around an elliptical hole in an infinite plate under uni-axial tension is examined. As shown in Fig. 16a, two straight horizontal cracks are placed symmetrically at the apex of the major axis of an elliptical hole, with a length of 0.1 mm extending towards the two sides of the plate. Due to the symmetry of this setup, we employ quarter symmetry as plotted in Fig. 16b for the simulation. The material parameters used in the tests are summarised in Table 5. A displacement-controlled traction is imposed on the upper edge of the specimen to drive a stable propagation of the pre-existing crack until it reaches the edge of the model. The displacement increment $\delta u$ is set to 0.1 $\mu$m.

![Figure 16](image1)

**Figure 16**: Elliptical hole in an infinite plate under uniaxial tension: (a) elliptical hole with semi-axes $a = 0.2\, \text{mm}$ and $b = 0.1\, \text{mm}$ under vertical uni-axial tension with symmetrical cracks of length $c = 0.1\, \text{mm}$ normal to the loading direction; (b) computational model for the simulation.

| Materials       | Young's modulus $E$ / GPa | Poisson's ratio $\nu$ | Fracture toughness $G_c$ / N mm$^{-1}$ |
|-----------------|---------------------------|-----------------------|----------------------------------------|
| Matrix          | 210                       | 0.30                  | 2.70                                   |
| Soft inclusion  | 120                       | 0.22                  | 0.42                                   |
| Rigid inclusion | 340                       | 0.22                  | 4.24                                   |

![Figure 17](image2)

**Figure 17**: Crack propagation with vertical uni-axial tension loading.
The propagation path of the pre-existing crack driven by the tensile loading through the specimen is illustrated in Fig. 17. Stress intensity factors calculated from an analytical solution (Weißgraeber et al., 2016) and OpenGeoSys-6 (OGS) are compared. We write the analytical solution defined in Lukáš (1987) in Eq. B.1.

\[
K_1 = \frac{1.122 \left(1 + \frac{2a}{b}\right)}{\sqrt{1 + 4.5 \frac{ca}{b^2}}} \sqrt{\pi c \sigma} \quad (B.1)
\]

In the framework of linear elastic fracture mechanics, \(K_1\) can be related to the energy release rate \(G\) (\(G = J\)), see Eq. B.2.

\[
G = \frac{K_1^2}{E'} \quad (B.2)
\]

with \(E' = \frac{E}{(1-v^2)}\) in plane-strain case.

Under the vertical tensile stress of \(\sigma = 800\,\text{MPa}\), \(K_1^{\text{analytical}} = 795.5\,\text{MPa}\sqrt{\text{mm}}, K_1^{\text{OGS}} = 777.3\,\text{MPa}\sqrt{\text{mm}}\) with \(G = 2.618\,\text{N mm}^{-1}\).

### B.2 Validation test 2: A straight crack approaching an inclusion

Crack-inclusion interaction in a two-phase composite under uni-axial tension is studied. We investigate how the stiffness difference between the matrix and the inclusion, the size of the inclusion, and the distance between the crack tip and the inclusion affect crack evolution and stress intensity factors. A notched edge crack with a length of 0.1 mm is placed adjacent to a circular inclusion, see Fig. 18. The circular inclusion has an initial diameter of 0.2 mm, and the initial distance between the crack tip and the near edge of the inclusion is 0.1 mm. The same displacement traction and increment used in Section B.1 are applied on the upper edge of the model to drive the propagation of the crack until the matrix is totally split.

![Figure 18](image_url)

**Figure 18:** A notched crack and a circular inclusion in a two phase plate under uniaxial tension: a straight crack normal to the loading direction with length \(c = 0.1\,\text{mm}\) placed a distance \(l_0 = 0.1\,\text{mm}\) in front of a circular inclusion of diameter \(D = 0.2\,\text{mm}\) under vertical uni-axial tension.

The propagation path of the pre-existing crack driven by the tensile loading through the two phase specimen is plotted in Fig. 19. It can be seen from Fig. 19a, the crack propagates through the matrix body and bypasses the rigid inclusion. When it encounters a relatively soft inclusion, Fig. 19b, i.e. with a lower fracture toughness comparing to the matrix body, the crack propagates right through the inclusion, because the inclusion becomes the preferential path for cracking.
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Figure 19: Crack-inclusion interactions in a two phase composite under uniaxial tension: (a) crack propagation interacts with a rigid inclusion with high Young's modulus and fracture toughness ($E_{\text{inclusion}} > E_{\text{matrix}}$, $G_{\text{inclusion}} > G_{\text{matrix}}$); (b) crack propagation interacts with a soft inclusion with low fracture toughness ($E_{\text{inclusion}} < E_{\text{matrix}}$, $G_{\text{inclusion}} < G_{\text{matrix}}$).

Stress intensity factors calculated from an analytical solution (Li and Chen, 2002) (see Eq. B.3) and OpenGeoSys (OGS) are compared.

$K_I = K_{I}^c + \frac{K_{I}^c}{2\pi} \int_{l_0}^{l_0+2r} dx \int_0^{\sqrt{r^2-(x-l_0-r)^2}} \frac{C_1 \left(x^2 - y^2 + x\sqrt{x^2 + y^2}\right)}{(x^2 + y^2)^2} + \frac{2C_2 xy^2}{(x^2 + y^2)^{2.5}} \right] \right) \] \] \] \] \] (B.3)

with

$C_1 = \frac{(1-\alpha)(1-2\nu)}{(1+\alpha-2\nu)}$ \] \] \] \] \] (B.4)

$C_2 = \frac{3(1-\alpha)}{2(1+3\alpha-4\alpha)}$

and

$\alpha = \frac{E_{\text{inclusion}}}{E_{\text{matrix}}}$ \] \] \] \] \] (B.5)

where $K_{I}^c$ represents the stress intensity factor of the crack tip without inclusion in the matrix.

Note that for the current analytical solution, the Poisson’s ratios of the matrix and the inclusion are assumed the same constant (Li and Chen, 2002). Under the vertical tensile stress of $\sigma = 160$ MPa, $K_{I}^{\text{analytical}} = 100.5$ MPa $\sqrt{\text{mm}}$, $K_{I}^{\text{OGS}} = 99.1$ MPa $\sqrt{\text{mm}}$.

CRediT authorship contribution statement

Xing-Yuan Miao: Conceptualisation, Methodology, Validation, Software, Visualisation, Writing - original draft.
Renchao Lu: Software, Visualisation, Writing - review & editing. Xiao Chen: Conceptualisation, Methodology, Supervision, Writing - review & revision.

Declaration of competing interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.
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