ID 1452
Buckling Response of Sandwich Composites: Effect of Core Density and Implanted Interface Cracks

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Abstract: Buckling response of foam core sandwich composites with various core densities and implanted delaminations have been investigated. Klegcell foams with densities ranging from 75-300 kg/m$^3$ were used in the analysis. Various lengths of delaminations were introduced between the face sheets by implanting non-porous Teflon during the construction of the sandwich. The idea was to determine the influence of a/L (crack length over the length of specimen) on the critical strain. The study further investigated the effect of interface crack in controlling/accelerating the core shear, and local buckling of the face sheet. Buckling tests were carried out with simple support end conditions on a newly designed test fixture. Both in- and out-of-plane displacements were recorded, and were utilized in determining the critical buckling loads. Both FEM and analytical calculations were carried out to determine the initial and post buckling responses. They were later compared with the experimental data. Failure modes with various core densities have also been identified, and their relationship with interface crack propagation has been determined. Details of the experimental, finite element and analytical work are presented in this paper.

Introduction: Sandwich composites have numerous potential applications in automotives, construction, aerospace and navy industries. They are also used in small boats and yachts, die molds, recreational, and residential construction materials. High bending rigidity and energy absorbing characteristics, and low specific weight are some of the main attractive features of sandwich composites. A sandwich panel consists of two thin high strength face sheets and a thick core of lightweight material. As the face sheets are placed further away from the natural axis, they become capable of sustaining higher flexural load during the bending of a sandwich panel. Core materials can withstand high in-plane shear stress developed around the central zone of a panel during the bending action.

Buckling behavior of sandwich panels has drawn interest of many researchers. Brush and Almroth [1] mentioned that in sandwich, rotations and curvatures were caused not only by the moment intensities but also by the transverse shearing force intensities. They also explained that the critical load for a plate, unlike that for a column, did not represent its ultimate strength. Hoff and Mautner [2] tested some rectangular sandwich panels in edgewise compression using papreg faces and cellular acetate core. They found that the buckled shape consisted of a ripple of short wavelength across the panel. It was either symmetric, the two faces bulging out symmetrically according to sine curves, or skew, the two faces deflecting in the same sense according to sine curves having a phase angle of 90°. Pearce and Webber [3] found that for all CFRP panels, except for the unbalanced cross-ply, the experimental failure load was higher than the experimental buckling load and quickly followed it. Furthermore, the experimental failure loads for the CFRP panels designed for wrinkling were all higher than the theoretical wrinkling loads. Ko [4] established the combined load (mechanical or thermal) buckling equations for orthotropic rectangular honeycomb core sandwich panels under four types of end conditions through the use of the Raleigh-Ritz method. Kardomeas [5] on the other hand, conducted compression tests on delaminated Kevlar/epoxy specimens and investigated the deformation behavior related with post
buckling condition. He also investigated the load-displacement behavior, and the corresponding growth of the delamination. Several other researchers [6-10] also studied the delamination growth and the corresponding stability analysis. It was observed that the buckling load decreased as the delamination size increased and the buckling modes of the delaminated plates changed from a global type to a mixed type. It was also found that the global buckling load reduction due to an edge delamination was insignificant. The buckling load decreased proportionally to the width of the delamination. The global instability load of the plate with the same size delamination became smallest when the delamination was located on the midplane. The local instability was observed at the delaminated portion when the delamination was located near the surface and its size was relatively large. It was found that shear deformation effect lowered the buckling load and increased the energy release rate. The effect became larger in short and thick delamination. Hann et al. and others [11-13] developed a test fixture for evaluating the pre and post-buckling response of simply supported, nearly flat, rectangular corrugated board panels subjected to edge compression. It was seen that collapse occurred at loads larger than those required to initiate localized and elastic overall buckling. When delamination existed primarily in the areas where the mode shape displayed high curvature, little degradation took place. Hansen [14] investigated the parameters controlling the buckling instability of the FRP sandwich specimens with clamped edges and interface debonds. He found that the effective buckling length of debond was considerably larger than the physical debond length. Carlsson [15] presented a design analysis of the cracked sandwich beam (CSB) specimen, based on shear deformation theory. He concluded that crack propagation occurred prior to core crush or core shear failure. Large core thickness and high facing modulus promote the desired propagation failure mode.

The above discussions illustrate how rigorously the investigators have studied the buckling behavior of the sandwich composites. The effects of local delamination and debonding have been related only to the local buckling. However their effects on global buckling have been avoided. Moreover, literature review does not reveal any study relating core density with buckling responses. This paper takes a systematic approach in studying the core-skin debonds coupled with core materials densities. Experimental, analytical and finite element studies have been conducted in this investigation to assess the critical buckling load, deformation behavior and the failure modes under edge-wise compressive load.

Materials Processing: Panels were fabricated using vacuum assisted resin infusion molding (VARIM) process (Fig. 1). It was a manufacturing technique that used atmospheric pressure to infuse resin into a dry fabric preform placed on a single sided tooling. Primarily two types of panels: with and without debonds were prepared and tested. For each type of panel four different densities of core, namely, R-75, R-130, R-260 and R-300 were used. Subsequently, panels were also fabricated with delamination between two face sheet layers of R-75 core panels for conducting a parametric study of the effect of delamination length on critical strain. Plain weave S-2 glass fabric (Owens Corning 24 oz/sq. yd. 463 sized) and Klegecell (PVC) foam were cut into the desired dimensions. Three layers of fabric were used on either side of the foam for face sheet. Other components of manufacturing were porous Teflon sheet, distributing mesh, spiral wrap, non-porous Teflon sheet, vacuum bag and piping. Non-porous Teflon sheets were laid up at the desired location between the foam and the fabric to introduce core-skin debonding or between two fabric layers of either top or bottom face sheets to implant delamination. The mold preparation included cleaning an aluminum plate mold with acetone followed by a spray of Frekote-700 NC. Mold was prepared by lay-up of fabric, foam, Teflon sheet and distributing media in proper sequence and enclosing the entire mold in the vacuum bag. Debulking was done for two hours with the help of a vacuum pump and the vacuum level was monitored with a vacuum gage. Resin was prepared by mixing Vinyl Ester (Derkane VE 411-350) with other ingredients. These included CONAP (Cobalt Naphthalene) 0.2%, DMA (Di-Methyl Aniline) 0.05%, 2,4-P (Acetyl Acetate) 0.075% and MEKP (Methyl Ethyl Ketone Peroxide) 1.25% by
volume. Resin was infused in the mold under vacuum (28.5 to 30 in. mercury). Resin flow took place both in the plane of preform and in the thickness direction. Uniform wettability was attained. Upon complete part wetting, the infusion lines were blocked and the resin was allowed to cure under vacuum at room temperature for eight hours. After curing the bagging and other accessories were removed and the panel was trimmed and cut to the desired size (20.5 in. x 10 in.) by a rotating cutter with diamond edge. Strain gages (CEA-13-240UZ-120; Gf=2.12±0.5 %) were attached to the face sheets on both sides at the center of the panel with M-bond 200 and catalyst. The gages were placed parallel to the loading direction in such a way that they could take the response from two adjacent plain weave cells. The strain gages were connected to a scanner that checked the zeroing balance and calibration of the strain gages. The panel was then ready for testing. A portion of the face sheet was cut to find the void fraction of the composite. This was done by thermo gravimetric analysis (TGA). Fiber volume fraction was 55-60% and the void fraction was 4-7%.

**Experimental Work:** A test fixture has been designed with simple supported end conditions on the loading edges that ensured the repeatability of the tests (Fig. 2). The following guidelines were followed for fixture designing:

1. The fixture had to conform to the panel geometry and boundary conditions.
2. Specimen geometry did not support global compression failure before buckling.
3. Panels were elastically restrained at the loading edges against end crushing.
4. Panel width to length ratio (≥ 0.5) was consistent to a limiting plate or strut.
5. Panel slenderness ratio (>30) met limiting short column criterion.
6. Rotational degree of freedom of the panel under load was ascertained by the introduction of roller supports at the loading edges.
7. Face sheet layers were thin skins, a compromise between very thin and thick skins (total thickness to skin thickness ratio was between 5.77 and 100).
8. Fixture dimensions and material were suitable for the machine space and capacity considerations.
9. Fixture had to ensure the safety of the operator. Side plates were provided on the both sides of the fixture to safeguard an accidental burst out of the roller at the moment of panel failure, especially from the upper end.

The panels were tested in servo-hydraulic controlled MTS machine (Fig. 3) controlled by software, Test Ware SX. The software recorded load vs. in-plane displacement data directly. The scanner using another software, System 5000, collected strain gage data. Load vs. out of plane data were recorded manually using a dial gage set at the middle of the panel.

**Results and Discussion:** The buckling analysis in the current investigation was conducted through a few sequential steps. First, the load displacement behavior was analyzed in conjunction with the strain gage data to determine the critical buckling load. Second, the influence of implanted debonding was studied by analyzing the load-displacement data for the debonded plates. Third, the effect of core densities was analyzed by comparing the load-displacement behavior of plates having various core densities both in case of with and without debonding. A parametric study of the effect of delamination length on critical strain was also carried out. Finally, a comparison between the experimental results and both FEM and analytical solutions was made. Comprehensive failure analysis at various test conditions was also made.

**Experimental Failure and Buckling Loads:** The load-displacement behavior for specimens without implanted delamination is shown in Fig. 4. The load versus in-plane displacement curves for four densities; R-75, R-130, R-260 and R-300 are shown in Fig. 4. The curves in Fig. 4 were plotted according to the data recorded from the MTS machine, which came from the actuator movement and the load cell reading. The initial ramp up region up to the failure point for four categories is almost identical, as seen in Fig. 4. Because of the high compressive modulus, the
face sheets carried the major portion of the load compared to the foam. A close observation of Fig. 4 indicates that panels having both R-75 and R-130 core had the failure loads around 9,000 lbs., R-260 core panels had the highest failure load around 13,000 lbs., which is about 30% more than the failure load of either R-75 or R-130 core. As it is expected, the higher density foam, i.e., R-260 is failing at a higher load, approximately around 13,000 lbs. There is a difference if one looks at the curves after failure. For example, in case of R-260, after the initial buckling failure at 13,000 lbs, there is a sharp drop in the load until 10,000 lbs. when it continues to sustain load at this level for sometime, and then fails almost catastrophically. For R-75 the load drop is simultaneous to the failure. This suggests the presence of a rapidly progressing failure mode during the final failure event. The behavior of R-130 as seen in Fig. 4 is somewhat intermediate between R-260 and R-75. Whereas R-300 core panels failed at about 11,000 lbs. For R-300 panels the initial failure caused a load drop of about 700 lbs. The load level increased at higher displacements, i.e., at higher displacements panels recovered a significant portion of the dropped load. The global panel failure in each case was accompanied by a cracking sound mainly coming from the fracture of the face sheet.

As mentioned previously, strain gages were installed on both faces of the specimen near the central location to monitor the in plane displacements. These strains were recorded with respect to time as shown in Fig. 5. As it is observed in Fig. 5, strains from two (front and rear) gages are compressive up to certain time (around 11 min), and then they begin to diverge. After this time interval, one gage continues to monitor compressive strain while the other begins to record tensile strain. This is a clear indication of the initiation of the buckling of the specimen. Since the time is known for this event, load can be traced back from this time scale. The time for the initial buckling event is shown to be around 11.00 min. in Fig. 5. Although the actuator speed was only 0.01 in/min, the apparently long time to initiate buckling was due to fact that a 10-sec. holding time was allowed during the test after every 30 second of ramping in addition to the pre-loading time (35 sec.). This 10-sec. holding was necessary to record the out of plane displacement from the dial gage. While relating the time with the load, the holding and gain time was subtracted from the calculation. Buckling load calculated in this manner is shown in Table-1.

In case of implanted disbonds in the panel, the load-displacement behavior changes slightly as seen in Fig. 6. The failure load for R-260 is lower than what it was observed in Fig. 4. However, for R-75 and R-130, the loads are almost similar to the previous case. The post failure behavior is quite different, especially for R-130 and R75. Both demonstrate permanent deformation behavior after the failure. These are characteristics of unstable deformation, and are due to the progression of the uncontrolled core-skin debonding. If it is compared directly, the behavior of R-75 with and without the presence of debondings, the behavior appears to be very similar. Here also, R-300 panels exhibited lower failure loads (around 10,000 lbs) than the R-260 panels (about 11,500 lbs). It is evident that the increase of core density increased failure load up to a certain limit, beyond that core density did not play a significant role (Fig. 7). It should be noted that in case of implanted disbonds, strain gage data were also collected (Fig. 8) using the previously mentioned technique. Experimental buckling and failure loads for the deboned panels are shown in Table-2. Percentage reduction in buckling load due to the presence of Teflon is shown in Table-3. It is clear in Table-3 that as the density increases, the difference between the two categories also widens except for the R-300 case. This suggests that the influence of implanted debonding to cause buckling would be more pronounced with higher density foam up to a certain limit (Fig. 9). However, that is not the case when one considers the post-buckled failure loads. It is observed from Figs. 4 and 6 that the effect of implanted debonding is most significant for R-130 density cores. This also points to fact that implanted disbonds trigger failure modes such as core shear only with lower density foams. This will be more evident when failure analysis is made.

To predict the buckling load the variation of out of plane deflection should also be taken into consideration. An observation of Figs. 10 indicates that at certain load level the out of plane
displacement increased uncontrollably with small or without significant increase of load. The Southwell method as described in the reference [16] is a convenient way to determine the buckling load. The Southwell curves as shown in Fig. 11 was generated by plotting 'W' vs. 'W/P', where 'W' is the out of plane displacement and 'P' is the applied load. The slope of the Southwell curve was taken as the buckling load. For example, the value of the buckling load as obtained from Fig. 11 for R-260 specimen without Teflon was around 10,400 lbs. The variation of this buckling load from the previous one found with strain gage was about 6% and as such the average value was taken as the experimental buckling load (Table-1).

**FEM:** Finite element models were developed using ANSYS to simulate the buckling tests. Three sets of models were developed; two were with implanted delamination; one of these introduced debondings between the core and the skins, another set introduced delamination between the layers of one face sheet and the other was without any delamination. Debonding between the core and skin was represented by selecting a set of nodes both on the skin as well as on the core. These two sets of nodes had the same geometric coordinates but were uncoupled to simulate a crack between the two sets. The delamination between the two layers of one face sheet was also models in the similar way. The area and location covered by these nodes were corresponding to the Teflon sheet used in the experiment. The face sheets and the core were modeled with element type SOLID73. Material properties like elastic and shear modulii and Poisson’s ratio etc. were experimentally determined for the face sheet, while properties of the core material were collected from the manufacturer [17]. Compressive load was applied in the form of surface pressure. As a standard procedure, pre-stress analysis was first performed before invoking the buckling module. The buckling load was obtained as the lowest eigen value at which the system became unstable. This was shown as a factor after the completion of the solution. The corresponding buckling load was then calculated from the product of applied pressure, the extracted factor and the cross sectional area of the panel. An alternate scheme was taken to verify the buckling load for R-75 panels without debonding (Fig. 12). Static analysis was performed with step loadings. The out of plane deflection (w) was recorded for a particular point near the middle of a face sheet. Using these data, Southwell plot was generated in a similar way as described previously. The Southwell plot estimates a buckling load of 7,200 lbs., which is about 2.7% less than the results predicted by FEM. The method was not extended for other core densities or for panels with debonding because of prolonged running time and memory requirements. Results from the FEM simulation are shown in Figs. 13 and 14. Buckling modes of panels with and without Teflon are shown in these figures.

The buckling analysis was also done with displacement loading. Here uniform (unit) axial displacement was applied as the active loading. The corresponding buckling factor when multiplied with the input (loading) displacement indicated the level of displacement for which a panel was expected to buckle. However the buckling load could not be found directly in this case. To determine the buckling load, a static compression program was run with the displacement (the product of buckling factor and the displacement input for the buckling program) as the loading. After the solution had been evoked, the summation of the nodal loads at the loading face was considered as the corresponding equivalent buckling force (critical load). Nodal displacements and mode shapes were also extracted during the analysis. Values obtained from FEM analysis are shown in Table-1. It is apparent from Figs. 4 and 13, that the correlation between the two is excellent. However, this was not the case with specimens having implanted debonds. Correlation between the experimental and FEM results in case of panels with debonds is moderate. Although FEM predicts a dual-curve mode shape, it was found during the experiment that the mode was indeed very similar to the earlier case, i.e., a single-curve mode shape until failure. The load predicted by FEM in this case was also lower than what was found experimentally.

**Failure Analysis:** Post failure analyses of various categories of sandwiches were performed using optical and Scanning Electron Microscopes (SEM). Micrographs of panels with and without Teflon in the region of the failure are shown in Fig. 15 and 16. Extensive core shear crisscrossing
the entire width of the core is observed in Fig. 15. As it was recorded during the test that the failure initiated in the form of debonding on both sides of the core near the buckled region, and then progressed as core shear, which then coalesced to cause shear failure of the foam. This coalescence was further enhanced by the squeezing effect caused by the two face sheets during buckling. The final failure was due to fiber rupture accompanied by cracking sound and wrinkling of the face sheets. The initial failure modes as seen in Fig. 16 were somewhat different from that shown in Fig 15. Because of the presence of implanted delamination, core-skin debonding was much rigorous in this case. The delamination continued to spread over to the area beyond the initial debonding, as the loading increased. It did not cause severe core shear as it was observed in the previous case. It is believed that near the buckled region, wide separation between the core and the skin took place at the very early stage of the loading, which relieved the core from being squeezed. This reduced the scope of developing core shear in that region. However, the potential site for core shear was the location near the tip of the implanted Teflon. Core shear developed in that region is shown in Fig. 16. Since the core did not deform as much as it did in the previous case, some of the energy is believed to have been spent in continuing the delamination further towards the end of the specimen. This probably explains the flat regions of the load-displacement curves of Fig. 6. Core-skin debonding and core shear phenomena were mostly associated with the lower density foams (Figs. 15 through 18). Close viewgraphs of post-fractured specimens with higher density are shown in Figs. 19 through 22. The face sheet as well as the edge view of core can be seen in these figures. It is observed that debonding and core shear were minimal in these cases. The core is found to be almost undamaged for R-260 and R-300 cases. The failure was entirely due to the collapse of the face sheets.

**Effect of Delamination Length**: The results of the effect of delamination (between two face sheet layers) length (a/L) on critical strain are presented in Fig. 23. The FEM study indicates a decrease of critical strain with the increase of delamination length. The rate of decrease becomes less as the delamination length increases. However the experimental data reflect a different picture. Panels having two different lengths of the four categories tested were off from the FEM curve. For a/L value of 0.17, the critical strain is seen to be less than that at the a/L value of 0.20. At a/L= 0.30, the critical buckling strain does not show a significant reduction from the previous case. These variations could be attributed to the fact that FEM simulated critical strain for mixed modes (global and local) but the experimental buckling was global for all types.

**Comparison with Analytical**: Analytical calculations were also performed to compute the critical buckling load for panels without debonding using eqn. in reference [18]. The calculated values are shown in Table-1. Table-1 shows a comparison of the critical buckling loads determined in three separate ways. Correlation between the analytical and FEM results is excellent. Experimental values are slightly higher but well within 11% of the corresponding values predicted analytically. Analytical predictions for debonded panels require the modifications of formulation presented in reference [14] for simply supported panels. Similar modifications of the equations proposed in reference [19] will allow the prediction of buckling loads for the panels with interlaminar delaminations.

**Conclusions**: The followings are the summary of the investigation:

- Vacuum assisted resin infusion molding (VARIM) technique was employed successfully for fabricating sandwich panels with implanted delaminations.
- A buckling fixture, for sandwich panels with simple supported end conditions has been developed. The fixture allowed rotational degrees of freedom at the ends and ensured repeatability of the experimental buckling tests.
- Panels with higher density foam demonstrated higher buckling load than the one with lower density foams having similar face sheet constructions up to a certain limit. At higher density the buckling load became independent of the core density. Moreover, if the core densities were low, the variation in their failure loads was seen to be minimal.
- Presence of core-skin disbands slightly reduced the buckling load, initiated extensive delamination in the panel, and significantly changed the post-buckling behavior.
- Implanted Teflon also reduced the amount of core shear as part of the input energy was expended to continue the debonding growth.
- Higher density foam arrested core-skin debonding and core-shear phenomena almost entirely during buckling.
- The correlation among experimental, analytical and FEM results were good in case of sandwiches without implants. However the correlation in case of core-skin debondings was average.
- Variation of critical strain with delamination length (a/L) has also been studied. Both FEM and experimental studies were conducted to determine the influence of a/L. FEM studies predicted a gradual decrease in critical strain as a/L increased. The correlation between experimental and FEM results was found to be moderate.
- Finally, a comprehensive study has been performed in determining the buckling response of foam core sandwich composite panels. The study has demonstrated that core density and core-skin debonding are two most important criteria that need to be considered when designing with sandwich constructions under compressive loading situations.

Acknowledgements

The authors would like to appreciate the Office of Naval Research (ONR) to support this grant through grant No. N00014-90-J-11995. Dr. Y. D. S. Rajapakse is the Technical Monitor.

References


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Table 1: Critical Buckling and Failure Loads for Panels without debonding

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Table 2: Buckling and Failure loads for Panels with core-skin debonds

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Table 3: Comparison of Experimental Buckling and Failure Loads with and without debinding
Fig. 1: A Schematic diagram of panel fabrication process

Fig. 2: Buckling fixture with roller support

Fig. 3: Buckling set-up with simple ends

Fig. 4: Load vs. in-plane displacement curves for panels without debonding

Fig. 5: Strain-Time plot for a (R-260 core) panel without debonding

Fig. 6: Load vs. in-plane displacement curves for panels with debonds

Fig. 7: Variation of failure load with core density

Fig. 8: Strain-Time plot for a panel (R-260 core) with core-skin debonds
Fig. 9: Variation of buckling load with core density

Fig. 10: Load vs. out of plane displacement curves for panels without debonding

Fig. 11: Experimental Southwell plot for a R-260 core panel without debonding

Fig. 12: Southwell plot with FE data for a panel (R-75 core) without debonding

Fig. 13: FE buckling mode for a panel without any debonding or delamination

Fig. 14: FE buckling mode for a panel with core-skin debonds

Fig. 15: SEM picture of a R-75 core panel (without debonding) after failure

Fig. 16: SEM picture of a R-75 core panel (with core-skin debonds) after failure
Fig. 17: Viewgraph of a R-130 core panel (without debonding) after failure

Fig. 18: Viewgraph of a R-130 core panel (with debonds) after failure

Fig. 19: Viewgraph of a R-260 core panel (without debonding) after failure

Fig. 20: Viewgraph of a R-260 core panel (with debonds) after failure

Fig. 21: Viewgraph of a R-300 core panel (without debonding) after failure

Fig. 22: Close viewgraph of a R-300 core panel (with debonds) after failure

Fig. 23: Variation of critical strain with delamination length