Trans. SNAME, Vol.113, 2005. Ultimate limit state design technology for aluminum multi-hull ship structures' authored by J K Paik et al

Discussion

Matthew Collette, Member

I would like to congratulate the authors on presenting a paper which is both timely and extremely interesting. As the application of high-speed aluminum vessels expands to include larger vessels and more severe operating environments, using limit-state design techniques becomes increasingly important. At the present time, such limit state techniques are not as refined for aluminum structures as they are for steel structures in the marine world, which makes a paper such as this one even more valuable. The authors have contributed new information on many key aspects of the ultimate compressive limit states for aluminum, including residual stresses and shakedown effects, panel behavior, numerical panel analysis, and hull-girder ultimate strength analysis.

There have been relatively few experimental collapse tests of aluminum panels built out of marine alloys with marine construction techniques; so I was very interested to see some of the first results from the test program carried out by the authors. The comprehensive work on measuring initial deflections and residual stresses undertaken as part of this study is also very valuable. The computed ultimate strength and shape of the panel's load-shortening curve is influenced by these imperfections, as shown by the finite element results presented by the authors. While the residuals stresses do affect the ultimate strength, they will also affect the fatigue strength of welded details which have proved particularly troublesome for aluminum craft. One of the common questions in fatigue analysis is how the residual stresses affect the mean stress, and whether inducing compressive residual stresses at a fatigue-prone location provides for lasting fatigue resistance. In the present study, shakedown under relatively high loading and a few load cycles was shown to significantly reduce the level of residual stress. Do the authors have any comments on the expected amount of shakedown reduction that might be experienced under a typical service load spectrum with many more cycles but at a lower stress level? Are any more experiments planned in this area?

As noted by the authors, assuming that aluminum follows the traditional elastic-perfectly-plastic stress-strain curve may not always yield conservative results. For un-stiffened plate components in compression, the experimental test programs to date have made it clear that the fully heat-treated temper should be considered separately from alloys without this heat treatment because of differences in the shape of the material stress-strain curve. For the marine world, this means that un-stiffened plates made from 5000-series aluminum alloys should be treated differently from plates made from 6000-series alloys in the T6 temper. This separation is reflected in both the U.S. and European civil engineering design codes for these alloys. For stiffened panels the influence of temper is not always so clear-cut; especially as the plating and stiffeners may not be made out of the same temper. It seems logical that influence of temper on ultimate compressive strength might vary along with the relative slenderness of the panel, the boundary conditions, and how rapid the panel failure mode is (e.g. tripping vs. plate failure). In this regard, I would be very interested to learn if the authors have been able to identify if temper difference are more significant for panels with certain properties in their experimental analysis. In the numerical analysis presented here, I noted that the same minimum value for the "knee" factor in the stress-strain curve was assumed for the –H116 and –T6 tempers (Table 6 of the paper). While this should be a conservative approach for the -T6 alloy, taking such an approach may hide some interesting alloy-dependent behavior differences. In closing, I would like to congratulate the authors again on a most interesting paper and look forward to reading more on the current experimental test program as it progresses.

Bart Boon, Visitor, Bart Boon Consultancy, The Netherlands

The authors are to be complimented on a very interesting and timely paper. Ultimate strength is rapidly gaining ground as basis for assessing structures of ships of many different types. Novel ship types, such as multi-hull ships, can rely upon experience to a far lesser extent than more traditional mono-hull ships. The relevance of ultimate strength analysis for multi-hull ships thus is even greater than it already is for conventional ships.

The authors in this paper extend their earlier work for mono-hull ship structures. Basically they consider the hull as a (long, slender) beam where in bending originally plane cross-sections remain plane and where progressive collapse is concentrated in one, relatively short, cross-section of the hull. For many conventional ship hulls these assumptions may well be valid. For other ship hulls this may be less true. Multi-hull vessel structures in general are characterized by being less slender (at least for the complete hull) and by a much more 3-dimensionnal character. This may result in important shear lag effects and local deformations of the cross-section leading to a non-linear stress and strain distribution. As an example the figure below taken from Heggelund and Moan (2002) shows a limited effectiveness of the superstructure in longitudinal bending.

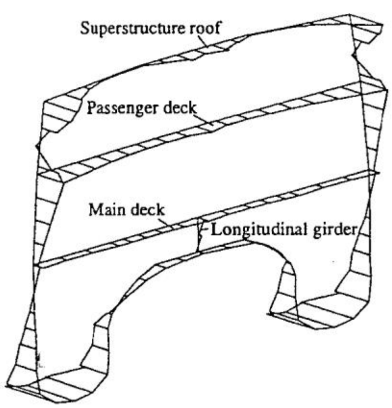


Fig.A.1 Longitudinal bending stress distribution

The way in which longitudinal stresses are transferred from the hull into the upper deck is important. Structures remote from the cross-section under consideration may play an important role. It is possible that such remote structures under an increasing overall bending moment fail much earlier (or possibly later) than the cross-sectional structure considered for the collapse. Such remote failure will then influence the failure at the location of the final total collapse. Similarly, in the cross-section of a catamaran some longitudinal bulkheads or other structural members may not stay at the same relative vertical height because of insufficient vertical support of that structure. This would lead to changes in the shape of the cross-section when subjected to an increase in load. Such situation would be in conflict with some of the assumptions made for the progressive collapse analysis as presented. What recommendations do the authors have for such 3-dimensional structural behavior and what is the influence upon the situation where progressive collapse is assumed to take place in only one cross-section?

A different question arises with regard to the collapse behavior of the structure itself. In a beam with an open U-cross-section after failure of one of the sides under longitudinal bending (tripping of the side walls) the axis around which bending takes place, may get a different orientation (minimum energy needed for collapse). The progressive collapse suddenly may be in a different direction from what it was initially. This may be the result of small non-symmetrical characteristics of the structure or the loads. Maybe even a collapse involving torsion is possible in such later stage. The method presented by the authors assumes only orientation for the bending axis in failure. How important may such interaction between various failure modes (orientation) for the overall structure be?

Reference

Heggelund, S.E. and Moan, T. (2002). Analysis of global load effects in catamarans, Journal of Ship Research, 46, 2002.

Philippe Rigo and **Thomas Richir**, Visitors, University of Liege, Belgium

First we would like to congratulate the authors for the quality of their paper. This one is very useful for ULS based design and strength assessment of aluminum multi-hull ship structures. We read it with great interest and we would like to make some comments, particularly in regard to the closed-form ULS formula (Eq.(3) in the discussed paper).

In the paper entitled Effect of Welding on Ultimate Compressive Strength of Aluminum Stiffened Panels presented at HIPER'04, Richir et al. (2004) investigate the ultimate compressive strength characteristics of a welded aluminum stiffened panel with varying welding related parameters such as weld type, width of HAZ (heat-affected zone) and reduction of yield stress due to HAZ softening. Non linear finite element analyses and the ULS formula were used for the sensitivity analyses on the parameters.

The sensitivity on weld type was analyzed by considering the following weld zones in the mesh modeling (Fig.A.2):

- five longitudinal welds at the junction between the transverse plate and the five stiffeners,
- four longitudinal welds at the intersection between the five extruded elements,
- two transverse welds between plates.

The ultimate strength obtained through the ULS formula for welds A (stiffeners welded on the plate) was quite similar to that for welds B (extruded elements) while the ultimate strength calculated by finite element analyses was higher for welds B than for welds A. Indeed welds B are only taken into account in the ULS formula through the P_s expression which becomes:

$$P_s = (b-2b'_p)\, t\, \sigma_{Yp} + 2b'_p\, t\sigma'_{Yp} + h_w\, t_w\, \sigma_{Ys} + b_f\, t_f\, \sigma_{Ys}$$

Is it correct that the ULS formula is only valid for welds A or can we use it also for welds B? Can we consider the effect of welding along transverse frames, i.e., Welds C?

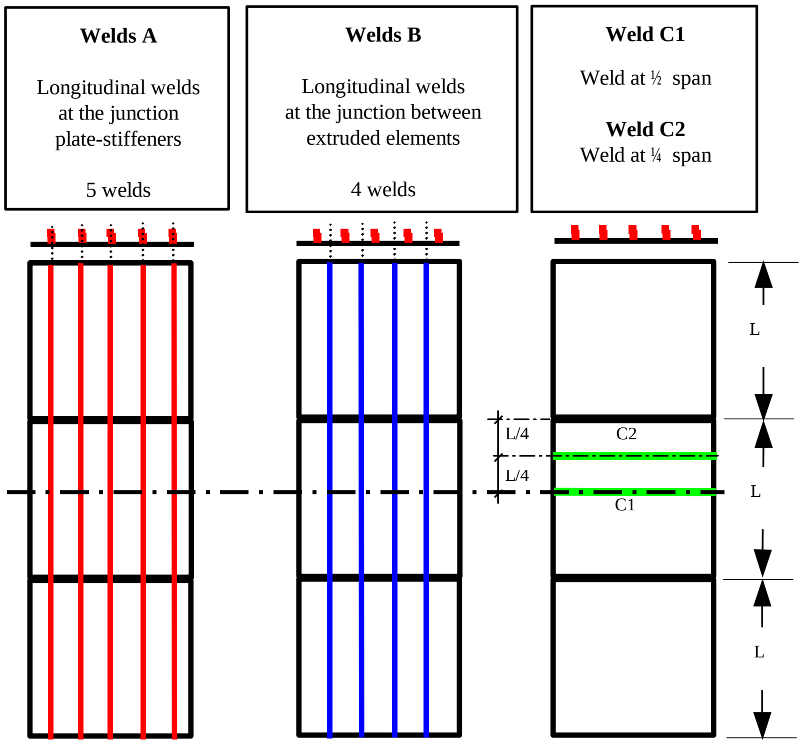


Fig.A.2 Considered weld locations

Figure A.3 shows the effect of yield stress in HAZ on the panel ultimate compressive strength. The ULS formula gives a very small sensitivity of the panel ultimate strength on the yield stress in the HAZ, while a reduction of 10% yield stress in the HAZ results in an ultimate strength reduction varying from 2% to 5% in the finite element analyses. Do you have an explanation? We would like to thank again the authors for their remarkable paper.

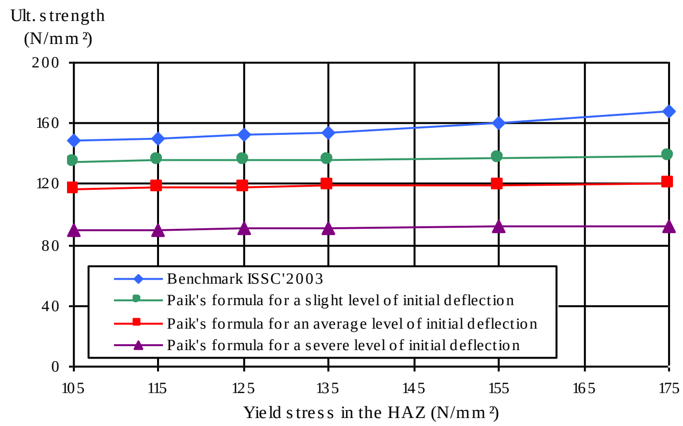


Fig.A.3 Sensitivity on yield stress in the HAZ

References

Richir, T., Toderan, C., Paik, J.K. and Rigo, Rigo (2004). Effect of welding on ultimate compressive strength of aluminium stiffened panels, HIPER'2004, High Performances Marine Vehicules, Rome, September, pp.129-1

Rigo, P. et al. (2003). Sensitivity analysis on ultimate strength of aluminium stiffened panels, Marine Structures, Vol. 16, pp.437–468

Magnus Arason, Visitor, QinetiQ, UK

In summary, the paper provides a basis for ultimate limit state assessment of ship structures that can be applied to aluminium multihulls. The presented computer modelling and analysis system appears appropriate for supplementing the limit state design procedure. A few points for discussion are noted below:

It is difficult to argue that multi-hulls have advantages over conventional mono-hulls in terms of strength and durability, although problems relating to these attributes may not be currently evident.

Whilst the comparative Table 3 suggests good performance of the laser welded aluminum panels in terms of welding induced initial distortions, it should be noted that the distortion values quoted for steel apply to arc welded structures. Material type is therefore not the only varying parameter in the comparison. Laser welding generally induces less initial distortion than conventional arc welding. The same applies to comparison of welding induced residual stresses between the aluminum panels of the study and the observations of Smith et al.

It would have been of interest to include a quantification of the effect of reduced yield stress for the HAZ in the FEA modelling, particularly when modelled jointly with the actual residual stress distribution.

Inclusion of structural imperfections (initial distortions and residual stresses) in the ALPS/HULL assessment of the two steel hull girder sections is not outlined specifically, but the good correlation with the measured data indicates that exact match between test conditions and the computer model has been achieved.

Limit state approaches continue to replace allowable stress methods in new and revised codes of practice, as is noted in the paper. In practice, limit state design is executed using partial safety factors of design standards that account for uncertainties associated with the design conditions. To establish a basis for a comprehensive capacity prediction for a limit state based procedure for aluminum ship structures, a few more parameters than those presented in the paper need to be estimated, with the panel- and hull girders strength predictions in the paper effectively presented as deterministic.

These parameters would feed assignment of partial safety factors accounting for

- variability in material properties,
- variability in geometrical dimensions of aluminum structural components and
- modelling uncertainties, although data is presented that indicates that this is very significant.

In calibration of a limit state based methodology for design of aluminium multi-hulls, random variables for the above parameters need to be identified. Load derivations would also be subject to the same type of uncertainty assessment.

Ton Bos, Visitor, Hydrographic and Marine Consultants BV, The Netherlands

With great interest I read to the present paper. I am of the opinion that the research described is comprehensive and takes into account many important issues such as the shakedown of welding residual stresses under cyclic loading. As fatigue and fatigue life assessment is more and more becoming an important issue we do wonder in how far fatigue and fatigue life assessment can be improved using the knowledge obtained with the research described in the publication. Like to learn the views of the authors on the fatigue issues and whether to authors are of the opinion that practical calculation methods can be developed to obtain more and accurate assessments on fatigue and fatigue life.

Strength and fatigue issues will be come more and more important in our works pertaining the preparation of transport manuals. This relates to the transport on semi submersible barges and heavy lift vessels such bulky and sensitive cargoes. Would like to learn the views of the authors on the possibility to use the ULS approach for this kind of projects.

Dracos vassalos and **Yunlong Zheng**, Visitors, Universities of Glasgow and Stratheclyde, UK

The authors are to be congratulated for the excellent piece of research work presented in this paper, certainly of theoretical and practical importance, offering methodologies and program systems that can be used for aluminium and steel ships. The following are some specific comments and suggestions for a response to which will be gratefully appreciated.

In page 10, in the residual stress relief tests 3 and 5 load cycles are applied to aluminium 5083 and 5383, respectively. To this end, different cycles should be applied to the same material to examine the relaxation characteristics whereas the characteristics of the two materials should be compared using the same number of cycles.

In pages 5 and 14, although engineering stress-strain relations may be sufficient in some plasticity analyses, in ULS analysis one may wish to use true stress-strain relationships if the strain is found to be not small. Therefore, it would be clearer to give the definition of strain and stress in Figs.6 and 21 and provide pertinent data if available.

In page 14, it is important to have pointed out and explained that an elastic-perfectly plastic material model does not always yield conservative results for aluminium alloy. It may be relevant to describe how the yield point is defined in Fig.21.

In page 17, it is seen that the 2-bay FEA always gives a larger ULS than 1-bay FEA. However, in Fig.22(b) the 2-bay FEA does not seem to give larger ULS (mode III/CIP), (4)<(2) and (8) similar to (6). The 2-bay model with free sides will cut the transverse frames off the neighbouring structure. Therefore, discontinuous transverse frames provide some rotational constraint on the one hand (open-web beams are torsionally weak) and loss most of vertical constraints on the other.

In page 20, it is pointed out in the discussion that different finite element modelling can lead to very different or even wrong results. In the light of this, closed-form formulae derived from the fitting of FEA results might be biased if the same mesh scheme is utilised for the same geometry with varying parameters. Further description in this respect will be more informative.

In Figs.27(d) and 28(d), the agreements of ALPS/HULL predictions with test measurements by UK Royal Navy and by US Navy are very good. ALPS/HULL uses sliced models comprising only longitudinal members. Transverse members, however, would have an effect on the ULS. They provide non-rigid support and the buckling of plate panels may be symmetrical or unsymmetrical w.r.t. supports, which would give rise to a difference in ULS. Some description on the effects of transverse members would be appreciated.

Chan Ho Shin, Visitor, Korean Register of Shipping, Korea

In this paper, the influence of wave pressure loads on the ultimate hull girder strength has not been considered. However, it is known that the ultimate strength is reduced by 5-10% due to the local loads. It would be better to explain the reason why the local load effects were neglected in the calculation.

For frigate and catamaran fast ship the hull between two transverse frames was taken as the extent of analysis. However, for double skin tanker the hull between two transverse bulkheads was taken as the analysis extent even though the hull between two transverse web frames could be taken as the extent. Could you explain the reason?

The three ships given as application examples take the longitudinally framing systems. However, it would be more helpful to show the calculation results for a ship with a transversely framing system like a bulk carrier, if any.

In 3rd line from the top of the right side at page 14, it would be better to insert 'always', i.e., … does not always provide …, since the stress-strain curve depends on how to approximate the real material stress-strain relationship including the proportional limit.

Authors' Closure

First of all, the authors are grateful for all the discussers who provided very constructive and valuable discussions. The following are the authors' reply to the discussions.

Our reply begins with the discussions of Dr. Collette regarding shakedown test of welding residual stresses. While the research team under the responsibility of the first author is now in the middle of the testing under lower loading with higher load cycles as well as under high loading with fewer load cycles, some pieces of the latter being presented in the present paper, and we will be pleased to present more detailed results in near future. We would expect that somewhat different features of welding residual stress release depending on loading cycles may be seen.

Dr. Collete also raises a very important issue related to the effect of tempers. We would fully agree with him that the effect of tempers in aluminum alloys is important in buckling collapse behavior of aluminum structures as well as fatigue and fracture, and thus temper differences must be accounted for in the strength assessment. For marine applications, H116 tempers are usually taken for aluminum plates and sheets and T6 or H112 tempers are taken for stiffeners. In the present paper, the effect of such temper differences were not taken into account in detail but the stress-strain relations of material were developed by the Ramberg-Osgood formula with the same knee factor with three parameters (i.e., yield stress, ultimate tensile stress and fracture strain) known. We believe that this modeling technique is reasonable enough to represent the material behavior with different tempers, but we would certainly agree that more detailed study to clarify the effect of temper differences is recommended.

The first issue that Prof. Boon raised is of great importance. Even under pure vertical bending moments, individual structural components can be subject to combined stresses such as longitudinal stresses, transverse stresses and shear stresses which can of course affect the progressive hull girder collapse behavior. This is more important for multi-hull ship structures than mono-hull ship structures. This is a reason to be said why strength analysis of multi-hull ship structures must be done in a three dimensional problem. In this regard, at least one cargo hold between two transverse bulkheads or the full hull must be taken as the extent of the progressive hull girder collapse analysis instead of one single segment model between two transverse frames. Moreover, when combined hull girder actions including horizontal bending and torsional moments as well as vertical bending are applied, the effect of combined stresses will become much more complex and must be important so that the single hull section model may not be acceptable. The research team under the responsibility of the first author is now in the middle of developing such advanced approach which will eventually be implemented to ALPS/HULL program. Figure A.4 shows the ALPS/HULL models with one cargo hold or the full ship which are on-going. The advanced ALPS/HULL analysis model can also be beneficial for the progressive collapse analysis of heeled ship hulls due to accidental flooding as well as intact / damaged ship hulls in upright condition.

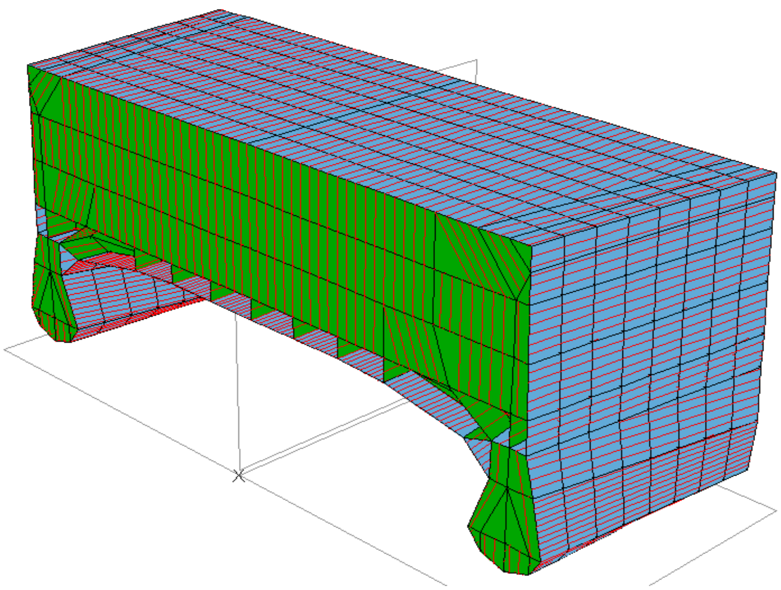


Fig.A.4(a) MAESTRO-ALPS/HULL model with the extent of one cargo hold for progressive hull girder collapse analysis

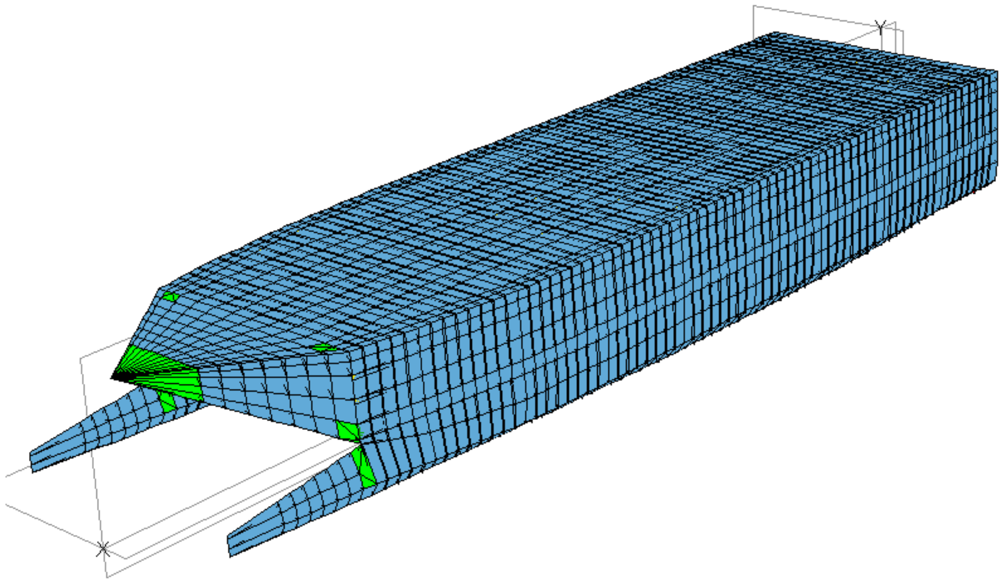


Fig.A.4(b) MAESTRO-ALPS/HULL model with the full ship for progressive hull girder collapse analysis

The second question of Prof. Boon is about non-symmetric failure patterns which can result in non-symmetric cross section neutral axis with respect to the applied bending. The failure pattern with regard to the center line of the vessel can be symmetric under pure vertical bending. However, if other hull girder components such as horizontal bending or torsion are involved and/or when non-symmetric damage exists, this hypothesis will not be valid any more. In some scenarios, vessels can be heeled by unintended flooding due to structural damage. The failure patterns and the related cross section neutral axis can again be non-symmetric. ALPS/HULL program checks the failures of individual structural components at each incremental loading step, and then both the horizontal cross section neutral axis for vertical bending moment component and the vertical cross section neutral axis for horizontal bending moment component are updated separately at each loading step. For a heeled vessel due to accidental flooding, ALPS/HULL models the ship at the heeled condition so that the non-symmetric failure patterns under applied hull girder actions can be accounted for automatically.

Regarding the first question of Prof. Rigo and Dr. Richir, the Paik ULS formula used in the paper of Richir et al. (2004) or in the present paper adopts the concept of equivalent yield stress which represents the effect of softening in the heat affected zones (HAZ) and/or the difference of yield stresses in plate and stiffeners. Upon using simplified ULS formulations, the concept of equivalent yield stress must be useful, as indicated in Eq.(3) together with Figs.23 and 24 in the present paper or as follows

$$\sigma_{Yseq} = \frac{P_s}{bt + h_w t_w + b_f t_f}$$

In the above equation, the value of P_s can be determined based on the HAZ locations and/or the different yield stresses in plate and stiffener. The P_s value presented in Eq.(3) of the present paper is applicable for the <u>Weld A</u> type noted by the discussers, where fillet welding was applied along the junction between plate and extruded stiffener web, namely

$$P_s = (b - 2b_p')t\sigma_{Yp} + 2b_p' t\sigma_{Yp}' + (h_w - b_{sHAZ})t_w\sigma_{Ys} + b_{sHAZ}t_w\sigma_{YsHAZ} + b_f t_f \sigma_{Ys}$$

For Weld B type noted by the discussers, where butt welding was applied in between two extruded plate-stiffener combinations, the P_s value is given by the equation that the discussers presented in their discussions above, namely

$$P_s = (b\text{-}2b_p')\, t\, \sigma_{Yp} + 2b_p'\, t\sigma_{Yp}' + h_w\, t_w\, \sigma_{Ys} + b_f\, t_f\, \sigma_{Ys}$$

For Weld C type where no longitudinal stiffeners do exist, the P_s value besomes as follows

$$P_s = bt\sigma_{Yp} + h_w t_w \sigma_{Ys} + b_f t_f \sigma_{Ys}$$

It is important to realize that the above-mentioned values of P_s were determined when uniaxial compressive loads in the longitudinal direction are applied so that the effect of softening along the transverse support members was neglected. However, if uniaxial compressive loads in the transverse direction are applied, then the effect of softening in the HAZ along the transverse support members must be accounted for, while that along the longitudinal support members may be neglected. When biaxial loads are applied, the effects of softening in the HAZ along both longitudinal and transverse directions must be considered as well. It is also to be noted that FEA usually takes into account the effect of softening in the HAZ along all directions, and even the simplified ULS formulation methods can also adopt the similar approach for more accurate calculations whatever the loading types are. In this case, the equivalent yield stress must be determined in terms of panel volume rather than cross sectional area, as follows

$$\sigma_{Yseq} = \frac{P_s}{abt + ah_w t_w + ab_f t_f}$$

where P_s can be readily determined by considering all heat affected zones due to weld together with the corresponding yield stresses and HAZ breadths as well as the difference of yield stresses in plate and stiffeners.

The second question of Prof. Rigo and Dr. Richir is about the effect of HAZ softening on the ULS. The yield stress of 5083-H116 is 215 MPa for base metal and 144 MPa for weld material, with 33% reduction of yield stress, followed by DNV guidance. This is confirmed by the present test results, as described in the present paper. It is recognized that the reduction of yield stress in the HAZ can cause some large reduction of ULS. For an example panel, the ULS reduction was about 15%, as shown in Fig.A.5. This is also confirmed by the results obtained by the discussers. According to Fig.A.3, the panel ULS value without the softening effect, i.e., with yield stress of 215MPa, may be over 200 MPa (by simply extrapolating the ULS curve), while the panel ULS value with the softening effect, i.e., yield stress of 144 MPa, is about 160MPa, showing some 20% reduction of ULS due to the softening effect in the HAZ. This certainly indicates that the effect of softening in the HAZ must be accounted for in the ULS assessment.

One important thing that must be pointed out is that the results quoted as Paik's formula in Fig.A.3 must have been obtained only for the Weld B type, because the P_s value used by the discussers for their ULS calculations was obtained by the equation of P_s for the Weld B type as noted above.

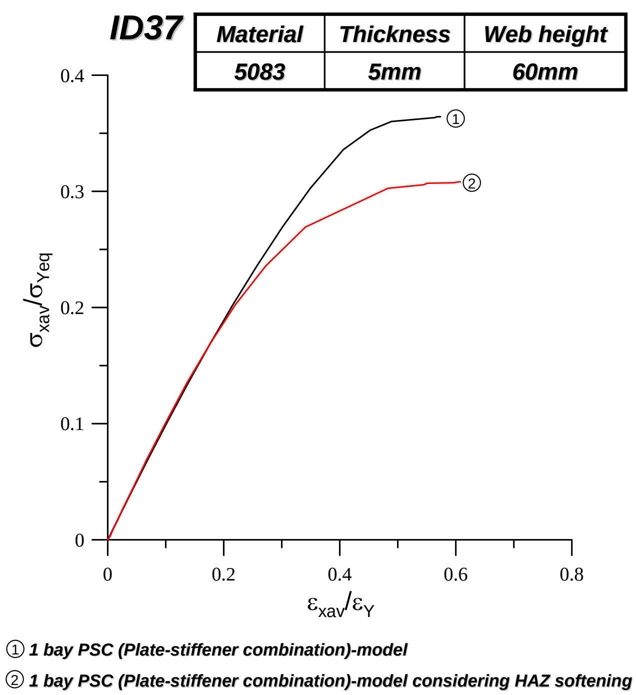


Fig.A.5 Effect of HAZ softening on the ultimate strength behavior of aluminum stiffened panel (yield stress of 5083-116 material in the HAZ is considered to 67% of the yield stress in its base metal)

Regarding the discussion contributed by Mr. Arason, multi-hull ships are more beneficial than mono-hulls in terms of larger deck areas specifically for military purpose. In terms of strength and durability, the former may also be more appropriate than the latter when wave-induced loads are considered. The wetted areas in the former are smaller than those in the latter. Transverse bending actions in the former are usually smaller than those in the latter. It is to be noted that the present test structures have been constructed by MIG welding, not by laser welding. For steel structures, unlike aluminum structures, there is a large database for fabrication related initial imperfections. While our test program is still on-going, our first insights with some limited test results are that the average level of fabrication related initial imperfections (distortions and welding residual stresses) for aluminum plate structures are in between slight and average level of those for steel plated structures. In our FEA modeling, the effect of softening in the HAZ has been included. The detailed information of initial imperfections applied for ALPS/HULL progressive hull girder collapse analysis of the two steel hull sections has been presented in separate publications as referred to in the list of references of the present paper. But we would say that the effect of initial imperfections on the progressive hull girder collapse is significant. For probabilistic design approach, we would agree that the uncertainties of various parameters must be identified.

As Mr. Ton mentioned, aluminum structures are susceptible to fatigue cracking and the knowledge of this issue is insufficient compared to steel structures, and further investigation of fatigue and fracture of aluminum structures is much needed.

Regarding the first question by Prof. Vassalos and Dr. Zheng, our primary aim for the residual stress shakedown test was to investigate the characteristics of welding residual stress release by external loads rather than the difference of the two materials. We expected that the release amount of welding residual stress with more load cycles may be more, but with the present limited amount of test data, we were not able to reach any clear conclusion. As we discussed above, we are continuing the shakedown test for various load levels and also more variety of loading cycles. As far as the static or quasi-static limit state analysis is concerned, the engineering stress-strain relation approach will be reasonable enough. This is because the limit states are mostly reached when the strains are relatively small. However, when accidental actions such as impact pressure, collisions or grounding are concerned, the strains can approach the fracture strain of the material, and in this case the true stress-strain relations must be considered.

The present paper shows that the elastic-perfectly plastic model does not always provide conservative results in terms of ULS predictions. This is in contrast to steel structures. The reason may be due to the fact that the elastic-plastic behavior of material around the material yield stress plays a role, while the elastic-perfectly plastic model neglects this. Therefore, it is recommended that the real stress-strain relation of material must be applied for more refined nonlinear analysis of aluminum structures. On the other hand, it is noted that the effect of material modeling noted above is not significant in terms of ULS predictions, and thus the elastic-perfectly plastic model can be adopted for practical design purposes even for aluminum structures. The yield point of material can be taken at 0.2% offset point strain.

The 2-bay structure FEA model automatically accounts for the effect of rotational restraints along the transverse frames. It is true that the 2-bay FEA model with free unloaded edges will reflect less stiff configuration, while unloaded edges of most test structures are considered to be simply supported and kept straight as actual continuous structures. It is important to realize that different FEA modeling may lead to quite different or even wrong results. The closed-form ULS formula presented in the paper is based on the FEA solutions, where more pessimistic conditions are considered in terms of geometric properties and boundary conditions but using 2-bay plate-stiffener combination models, and the effect of rotational restraints along the transverse frames is taken into account. The last question by Prof. Vassalos and Dr. Zheng has been answered in the reply to Prof. Boon. When the transverse frames are strong enough so that they do not fail prior to longitudinal strength members, one sliced section model may give good enough solutions for progressive hull girder collapse analysis. It is considered that this hypothesis can be adopted for the two test hulls. In general, however, this hypothesis cannot be accepted. Also, when combined hull girder loads together with local pressure actions are applied, at least one cargo hold must be taken as the extent of progressive hull girder collapse analysis. ALPS/HULL can do this.

Regarding the question by Dr. Shin, the effect of lateral pressure loads on progressive hull girder collapse analysis cannot be neglected in general and thus must be considered for the analysis. While ALPS/HULL can deal with the effect of lateral pressure loads, the present paper did not consider it because lateral pressure loads applied in the present catamaran ship hull is small and it is thought that the effect of lateral pressure on the hull girder collapse is small. The US Navy test hull has a unidirectional girder system, where no transverse frames exist in between two transverse bulkheads. This is the reason why one cargo hold was taken as the extent of the analysis. Various ALPS/HULL analysis examples for ship hulls with transverse framing system have been published in separate publications by the first author and his students.

Finally, the authors again thank all discussers for their valuable contributions to the present paper.

Trans. SNAME, Vol.113, The Society of Naval Architects and Marine Engineers, New Jersey, pp.270-305, 2005.

Ultimate Limit State Design Technology for Aluminum Multi-Hull Ship Structures*

Jeom Kee Paik (**M**), Pusan National University, Korea, Owen F. Hughes (**M**), Virginia Tech, Blacksburg, VA, Paul E. Hess III (**M**), Naval Surface Warfare Center, Carderock, MD, and Celine Renaud (**V**), Alcan Marine, France

* Disclaimer: The opinions expressed herein are the views of the authors and are not necessarily those of the Naval Surface Warfare Center or the Department of the U.S. Navy.

- A photo of an aluminum fast catamaran ship -

The present paper is a summary of recent research and developments related to some core ultimate limit state (ULS) technologies for design and strength assessment of aluminum multi-hull ship structures, jointly undertaken by Pusan National University, Virginia Tech, U.S. Naval Surface Warfare Center and Alcan Marine. An extensive study on the subject has been undertaken by the authors theoretically, numerically and experimentally. Methods to analyze hull girder loads / load effects, stiffened panel ultimate strength and hull girder ultimate strength of aluminum multi-hull ship structures are developed in the present study. Application examples of the methodologies for the ULS structural design and strength assessment of a hypothetical 120m long all aluminum catamaran fast ship structure are presented. Important insights and conclusions developed from the present study are summarized. Some of the comparisons have shown that 5383 called Sealium (a patented Alcan Marine alloy) is superior to the standard aluminum alloy 5083 in terms of material properties, ULS characteristics and welding performance. It is our hope that the methods developed from the present study will be useful for ULS design and strength assessment of aluminum multi-hull ship structures.

INTRODUCTION

The use of aluminum alloys in marine construction has certainly obtained many benefits, particularly for building weight-critical vessels such as fast ferries and also for military purposes, as may be surmised from Fig.1. This trend has in fact been more increased by recent advances in materials science which make it possible to produce higher strength aluminum alloys for marine applications such as 5383 called Sealium (a patented Alcan Marine alloy) in addition to the standard alloys such as 5083, 5456 and 5086.

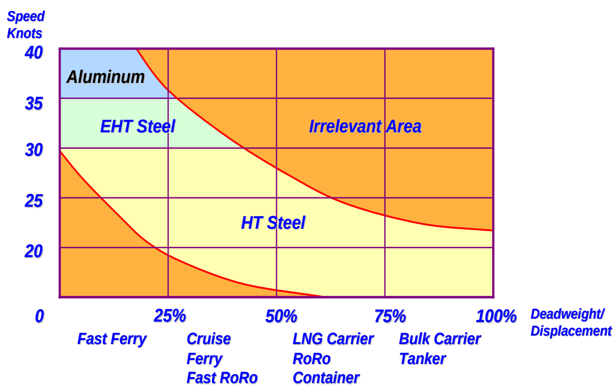


Fig.1 Material selection for different ship types, after Moan (2004)

Multi-hull ships have advantages in terms of lower resistance, excellent strength and durability and greater deck space than monohull alternatives. In smaller ships adequate deck area is difficult to obtain, while in multi-hull ships the deck area is usually 1.25 - 2 times that of mono-hull ships. This is a great advantage for military purposes.

The concept of catamaran-hull ships has now dominated the commercial fast ferry market and is making inroads into military applications. There is also a trend that the size of multi-hull vessels is increasing. The length of very recently developed aluminum multi-hull ships is over 130m.

It is now well recognized that ultimate limit state (ULS) approach is a much better basis for design and strength assessment of structures than the allowable working stress approach (Paik & Thayamballi 2003).

It is important to realize that the design procedure for multi-hull ships is different from that for monohull ships. For instance, designing multi-hull ships is a totally 3-D problem, while 2-D approximation is often adopted for design of monohull ships. At present there is no method available for the overall ULS analysis of multi-hull ships. Since the Navy wants fast ships they will have to be lightweight, meaning that local and overall ultimate strength will be a crucial issue.

The aims of the present study are to develop sophisticated technologies for ULS design and strength assessment of aluminum large multi-hull ships. For this purpose, some core technologies to analyze hull girder loads / load effects, aluminum stiffened panel ultimate strength and aluminum hull girder ultimate strength of multi-hull ships are developed.

It is commonly accepted that the collapse characteristics of aluminum structures are similar to those of steel structures until and after the ultimate strength is reached, regardless of the differences between them in terms of material properties. However, it is also recognized that the ultimate strength design formulae available for steel panels may not be directly applied to aluminum panels even though the corresponding material properties are properly accounted for.

This is partly due to the fact that the stress versus strain relationship of aluminum alloys is different from that of structural steel. That is, the elastic-plastic regime of material after the proportional limit and the strain hardening plays a role in the collapse behavior of aluminum structures, in contrast to steel structures where the elastic-perfectly plastic material model is well adopted. Also, the softening in the heat-affected zone (HAZ) significantly affects the ultimate strength behavior of aluminum structures, whereas it can normally be neglected in steel structures.

In the present study, refined methodologies and related computer programs (installed in MAESTRO and ALPS) for ULS assessment of aluminum multi-hull ship structures are developed.

The MAESTRO program (2005) allows the relatively quick creation of a whole-ship structural model, in which the dimensions and structural properties can be easily altered. The program performs finite element stress analysis and ULS based structural optimization. MAESTRO provides a physics-based model, where all of the structural members are automatically checked for structural failure, for all load cases.

ALPS/ULSAP (2005) performs ULS assessment of steel and aluminum stiffened panels and ALPS/HULL (2005) performs the progressive collapse analysis of steel or aluminum hulls under any combination of hull girder loads such as vertical bending, horizontal bending, shearing force and torsion.

A series of physical model testing on collapse of welded aluminum stiffened panels under axial compressive loads was carried out with varying geometrical dimensions (e.g., stiffener web height), and aluminum alloy types (5083 and 5383). Initial imperfections of the tested panels in the form of initial deflection and welding residual stresses are measured after fabrication and their characteristics are reported. Based on the test results, the collapse strength characteristics of welded aluminum stiffened panels are investigated.

The ultimate strength characteristics of aluminum plates and stiffened panels under a primary load component, i.e., axial compressive loads, are investigated through ANSYS elastic-plastic large deflection finite element analyses with varying geometric panel properties. A variety of different FEA modeling techniques are compared in terms of the resulting accuracy. Closed-form ultimate compressive strength formulae for aluminum plates and stiffened panels are derived by regression analysis of the computed results as well as test data.

This paper also develops methods for the overall ultimate strength analysis of multi-hull ships. ALPS/HULL program based on ISUM (Idealized Structural Unit Method) (Paik & Thayamballi 2003) is employed for the progressive collapse analysis of the ship hull.

The developed technologies are then applied to the ULS assessment of high speed aluminum catamaran ship structures. It is concluded that the developed technologies are very useful for ULS based design and strength assessment of aluminum multi-hull ship structures.

ALLOWABLE WORKING STRESS DESIGN VS ULTIMATE LIMIT STATE DESIGN

In design, any structure is required to have an adequate margin of safety against demands, the safety factor being necessary to account for various uncertainties due to natural variability, inaccuracy in procedures used for the evaluation and control of loads or load effects (e.g., stress, deformation), similar uncertainties in structural resistance (capacity) and its determination, and also variations in building procedures.

The structure should then satisfy the following criterion, namely (Paik & Thayamballi 2003)

$$D_d < C_d \text{ or safety measure} = C_d / D_d > 1 \quad (1)$$

where D_d = design demand, C_d = design capacity. A ‘demand’ is analogous to load, and a ‘capacity’ is analogous to strength necessary to resist that load.

In the allowable working stress design approach, the focus is on keeping the working stresses resulting from the design loads under a certain working stress level which is usually based on successful similar past experience.

In the maritime industry, the value of the allowable working stress is usually specified by regulatory bodies or classification societies as some fraction of the mechanical properties of materials (e.g., yield or ultimate tensile strength).

For the allowable working stress design, C_d in Eq.(1) is then determined based on the allowable stress, while D_d is the working stress which can be obtained by structural analysis under design loading conditions.

In contrast to the allowable working stress design approach, the ULS design is based on the explicit consideration of the various conditions under which the structure may cease to fulfill its intended function. For the ULS design, C_d in Eq.(1) is now the ultimate strength of the structure, while D_d is again the working stresses or loads.

It is now well recognized that the ULS is a much more rational basis than the allowable working stress, because it can consider the various relevant modes of failure directly until the ULS is reached.

The primary aim of the present study is to develop sophisticated core technologies to determine D_d and C_d in Eq.(1) to make ULS design and strength assessment of aluminum multi-hull ship structures possible.

A HYPOTHETICAL ALUMINUM CATAMARAN FAST SHIP

A hypothetical all-aluminum catamaran ship has been designed for use in the present study. Similar ships are in use as fast commercial ferries and are proposed for use as U.S. Navy’s Littoral Combat Ships (LCS).

The principal dimensions of the ship are: LOA = 120 m, breadth = 32.8 m, overall depth =15.4 m, depth of cross structure = 8.1 m. The displacement is 3500 tons and the design speed is about 35 knots.

The ship hull is made of 5383 aluminum alloy. The scantling of structural components of this ship including the plate panels and support members has been determined by the criterion, Eq.(1) in terms of design loads or load effects and the ultimate strength calculated using the methods described in the present study. The progressive collapse analysis of this ship was performed by the ALPS/HULL program.

ANALYSIS OF HULL GIRDER LOADS AND LOAD EFFECTS

The hull girder loads and load effects were calculated using MAESTRO.

For this ship the twin hulls are wider than for a SWATH, and the “cross deck structure” is very boxy and rigid. Therefore the “prying” and “squeezing” of the lower hulls is not a major load as it is in a SWATH. Instead the major hull girder load is longitudinal bending, the same as for a mono-hull.

The DNV High Speed and Light Craft (HSLC) Rules (ship type: Patrol, unrestricted service) (DNV 2003) were used to define a worst possible loading for longitudinal bending. Figure 2 shows the worst sagging case, among others.

The wave is of maximum steepness in order to position a crest at bow and stern, and there is a simultaneous bow slam that causes a large dynamic pressure on the sloping bow plating and the forward end of the wet deck.

For any wave encounter, MAESTRO automatically calculates the wave (Froude-Krylov) pressure distribution on the hull. Then, for any specified slamming pressure distribution, ship mass distribution and ship heave and pitch accelerations, it calculates an instantaneous dynamic equilibrium position for the ship.

In this process the inertia force at any point (node) in the model is the product of the local mass and the total local acceleration due to heave and pitch. To meet the HSLC Rule requirement we specified an upwards vertical acceleration of 1.85 g (g = acceleration of gravity) and a “bow down” pitching acceleration of 0.3183 rad/sec^2. This combination caused the acceleration at the bow to be 2.95 g.

Figure 2 indicates the instantaneous dynamic equilibrium position for the ship which has heaved upwards 0.15 m above its (still-water) design waterline and trimmed down by the bow 1.765 degrees. In this condition the sagging bending moment is 532 MNm. The maximum hogging bending moment is only 445 MNm because there is no simultaneous slamming impact.

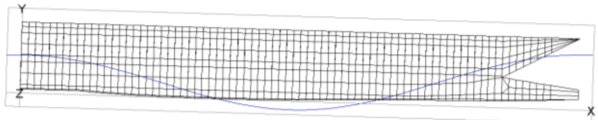


Fig.2 The worst sagging condition of the catamaran ship considered

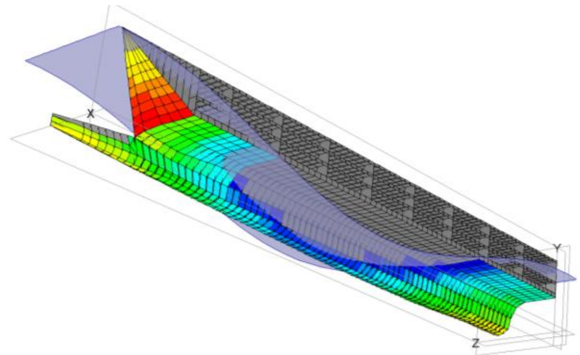


Fig.3 MAESTRO model for the starboard half of the catamaran ship considered, together with the total pressure applied

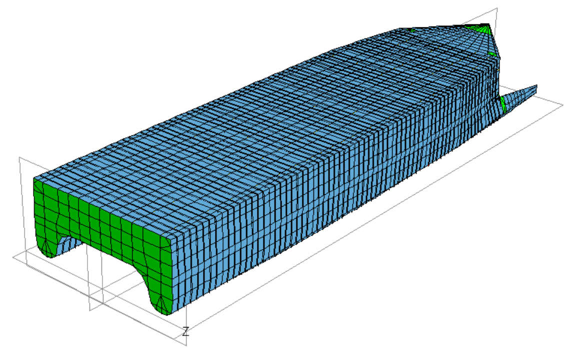


Fig.4 MAESTRO finite element model for analysis of load effects (stresses) on the catamaran ship structure

In comparison, the DNV HSLC Rules, Part 3, Chap.1, Sec.3, page 12, paragraph A503 (DNV 2003) gives 596 MNm for the maximum sagging bending moment and 438 MNm for the maximum hogging bending moment. These values are from the 1996 edition of the Rules. The formulas in paragraph A503 are intended only for early stage design. In paragraph A502 DNV recommends that the bending moments should be calculated “based on a predicted phasing between pitch/heave and the passage of a meeting design wave, and is to include the pitch angle and the inertia forces to be expected in the hogging and sagging conditions.” This is exactly how the present method using MAESTRO does the calculation.

On the other hand, the hollow landing bending moment from paragraph A303 is 863 MNm. However, for large vessels the hollow landing and crest landing scenarios are highly artificial and have been shown (Hughes 1998) to give excessively large predictions.

A “fish eye” view of the total pressure (slamming plus Froude-Krylov) acting on the starboard half of the model is shown in Fig.3. The slamming pressure acts on the sloping bow plating (forward of the wet deck) and has a maximum value of 0.06 MN/m^2.

Figure 4 shows the MAESTRO finite element model for analysis of load effects under the design loading condition.

PHYSICAL MODEL TESTING ON COLLAPSE OF WELDED ALUMINUM STIFFENED PANELS

Test Panel Configuration

Extensive mechanical collapse testing on a total of over 70 welded aluminum stiffened panels with various types of stiffeners (flat bar, Tee bar, extruded INCAT Tee bar), different geometries (plate/web thickness, stiffener web height), and different materials (5083, 5383) of plate and stiffeners are still ongoing by the research group of the first author with the support from some separate sponsors (Alcan Marine, France, U.S. Office of Naval Research, and Ship Structure Committee, USA) and will be reported at a later time.

The present paper presents some preliminary test results on a total of 10 welded aluminum panels (with flat bar stiffeners) under axial compressive loads, where the unloaded edges of the test panels are free, while the other panels under testing are simply supported at all edges which are kept straight. The loaded edges are simply supported.

Figure 5 shows a schematic of the typical panels being tested. The overall dimension of the panel is 1208 mm in length and 1000 mm in breadth. The panel has four stiffeners, two at the unloaded edges (sides) and two evenly spaced within the panel.

Fig.5 A photo of the test panel after fabrication

Mechanical Properties of Materials

Two types of aluminum alloys, namely 5083 and 5383 provided by the aluminum manufacturer, Alcan Marine, France were utilized.

Table 1 indicates average values of mechanical properties for the two materials obtained by tensile coupon testing performed in the present study. It is seen from Table 1 that yield tress and elastic modulus of 5383 are slightly greater than those of 5083.

Table 1 Mechanical properties of 5083 and 5383, obtained by the present tensile coupon testing

Property	σ_Y (MPa)	σ_T (MPa)	E (GPa)	Elongation (%)
5083	236	353	70.5	20.98
5383	244	358	73.0	19.38

Note: σ_Y = yield stress, σ_T = ultimate tensile stress, E = elastic modulus.

Figure 6 shows the stress-strain relationship of 5083-H116 and 5383-H116 obtained in the base and HAZ condition. The stress-strain relationship in the base condition has been obtained by the present tensile coupon testing, while the stress-strain curve for the HAZ condition was estimated using the Ramberg-Osgood formula (Mazzolani 1985), where the reduction factors of yield stress due to weld softening at the HAZ were taken as 0.67 for 5083 and 0.7 for 5383 followed by the guidelines of some classification societies (e.g., DNV 2003).

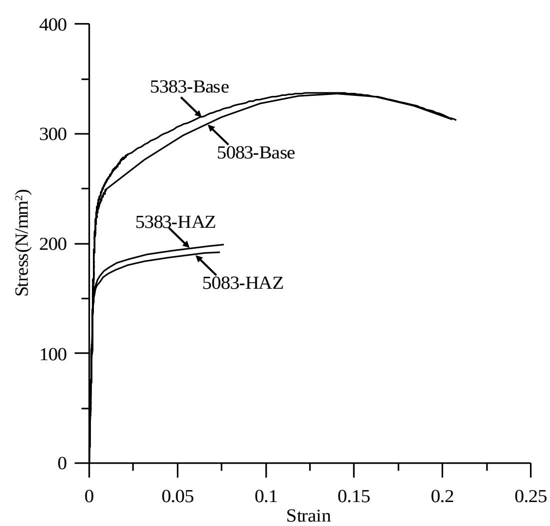


Fig.6 Stress versus strain relationship of 5083 and 5383 at the base and HAZ condition, obtained by the present study

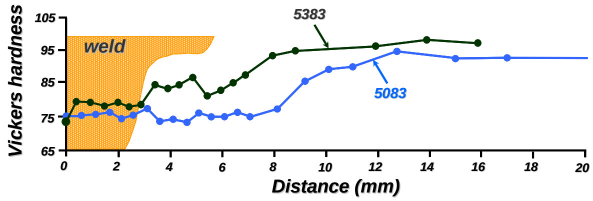


Fig.7 Vickers hardness of 5083 and 5383 at the weld and base location, after Raynaud (1995)

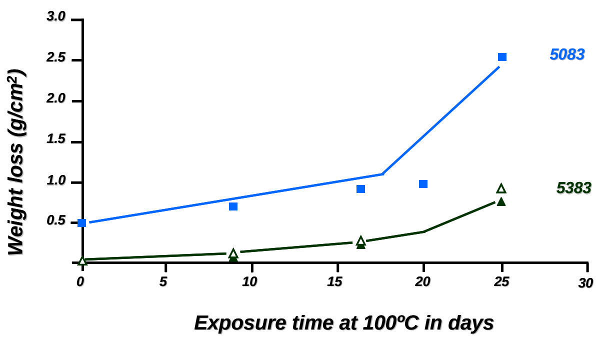


Fig.8 Weight loss of 5083 and 5383 exposed to acid attack, after Raynaud (1995)

It is to be noted that the minimum mechanical properties of the aluminum base metal specified by European Standard (EN 13195-1 2002) is σ_Y (yield stress) = 215 N/mm^2 for 5083-H116 and 220 N/mm^2 for 5383-H116. Also, the reduced yield stress σ'_Y in the heat-affected zone (HAZ) is defined by some classification societies (e.g., DNV 2003) as σ'_Y = 144 N/mm^2 for 5083-H116 and σ'_Y =154 N/mm^2 for 5383-H116.

Figure 7 compares the hardness of 5083 and 5383 at the weld and base location, showing that 5383 is harder than 5083 at both weld and base location. Figure 8 shows that 5383 is also superior to 5083 in terms of corrosion resistance.

Measurements of Fabrication Related Initial Distortions

The thickness of plate and stiffener web of the test panels is 6 – 8 mm and their stiffener height is 60-120 mm. The test panels were fabricated by laser welding in the shipyard of Hanjin Heavy Industries & Construction Co., Ltd. in Korea.

Except for some spot welding made manually to fix the stiffeners for upright positioning to the plate sheet, a MIG welding robot with laser sensors was employed for the fabrication of the test panels, where 5183 filler wire was utilized for fabricating both 5083 and 5383 panels.

Table 2 Summary of initial distortions for plating and stiffeners of the test panels

Material	5083	5083/5383	5383	Total
w_{opl}/t 1)	0.435	0.465	0.505	0.468
w_{opl}/t 2)	0.412	0.335	0.419	0.389
w_{oc}/a 1)	0.00112	0.00108	0.00106	0.00109
w_{oc}/a 2)	0.488	0.458	0.468	0.471
w_{os}/a 1)	0.00093	0.00073	0.00077	0.00081
w_{os}/a 2)	0.734	0.552	0.503	0.596

Note: w_{opl} = maximum initial deflection of plating between stiffeners, w_{oc} = maximum column type initial deflection of stiffeners, w_{os} = maximum sideways initial deflection of stiffeners, t = plate thickness, a = length of stiffeners. The superscripts 1) and 2) denote the average values and coefficients of variation, respectively. 5083/5383 material type indicates that plate is 5083, while stiffeners are 5383.

Table 3 Comparison of plate initial deflections for aluminum and steel stiffened panels

Material	$w_{opl}/(\beta^2 t)$
5083	0.054
5083/5383	0.059
5383	0.061
Aluminum (mean)	0.058
Steel (slight)*	0.025
Steel (average)*	0.1
Steel (severe)*	0.3

Note: * indicates the observations by Smith et al. (1988), $\beta = b/t \cdot \sqrt{\sigma_Y/E}$ = plate slenderness ratio, see Table 1 for the symbols, b = plate breadth, t = plate thickness.

The initial imperfections of the test panels after fabrication were measured in terms of initial distortions and welding residual stresses. The following three types of initial distortions which affect the ultimate strength of stiffened panels were measured for stiffeners and plating between stiffeners, namely

- Initial deflection of plating between stiffeners
- Column type initial deflection of stiffeners
- Sideways initial deflection of stiffeners

Table 2 summarizes the maximum initial distortions measured for a total of 27 test panels (9 test panels for each type of panel material).

Table 3 compares the maximum initial deflection of plating for aluminum alloy and steel panels, the latter being based on the insights developed by Smith et al. (1988). It is observed that the plate initial deflection of aluminum panel is in between slight and average level of steel plate initial deflection. Further study results observed for all of the test panels (76 panels) will be reported at a later time.

While the column type initial deflection of stiffeners shall be an important parameter in the column type collapse mode of the stiffened panel, while the sideways initial deflection of stiffeners more likely affects the lateral-torsional buckling or tripping failure mode of the stiffened panel.

Figure 9 shows selected measurements of initial deflections of plating and stiffeners. Figure 10 shows some other types of plate initial deflection shape between stiffeners observed from the present test panels. It is seen from Fig.10 that the plate initial deflection shape resembles the so-called “hungry horse” which is typical in plating of steel ship structures.

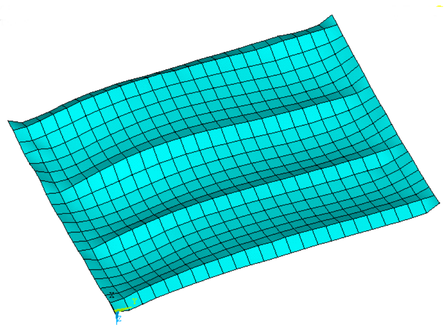


Fig.9(a) A typical pattern of overall initial deflections (multiplied by 30 times)

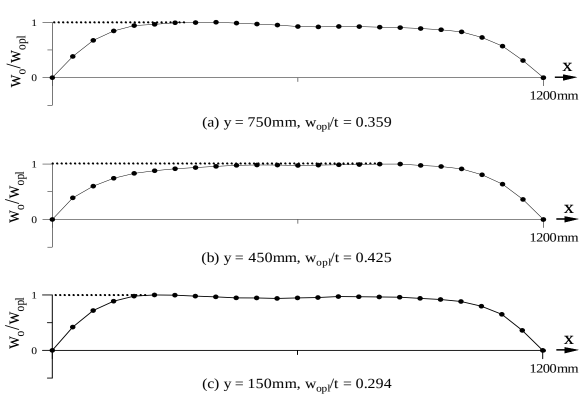


Fig.9(b) Plate initial deflection pattern

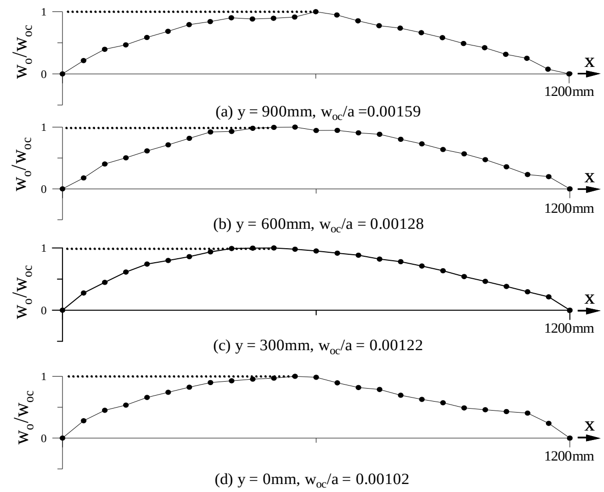


Fig.9(c) Column type initial deflection pattern of stiffeners

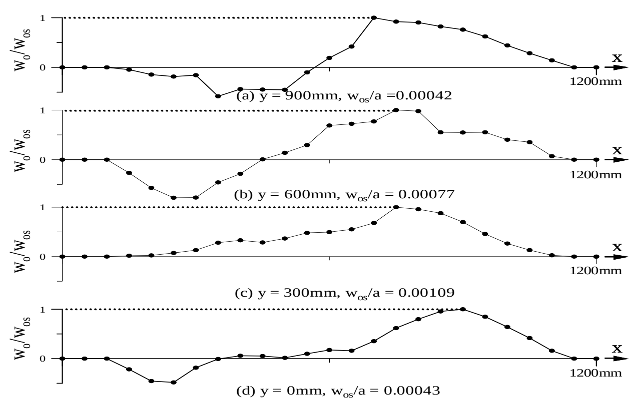


Fig.9(d) Sideways initial deflection pattern of stiffeners

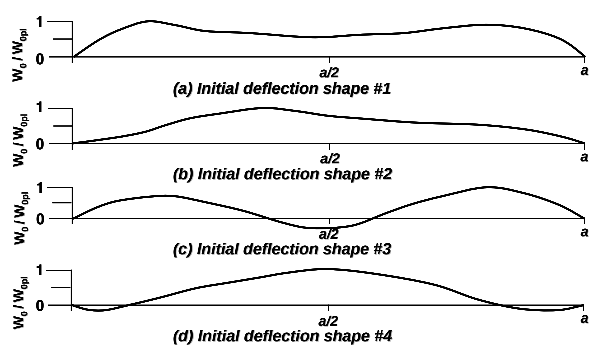


Fig.10 Some selected shapes of plate initial deflection between stiffeners

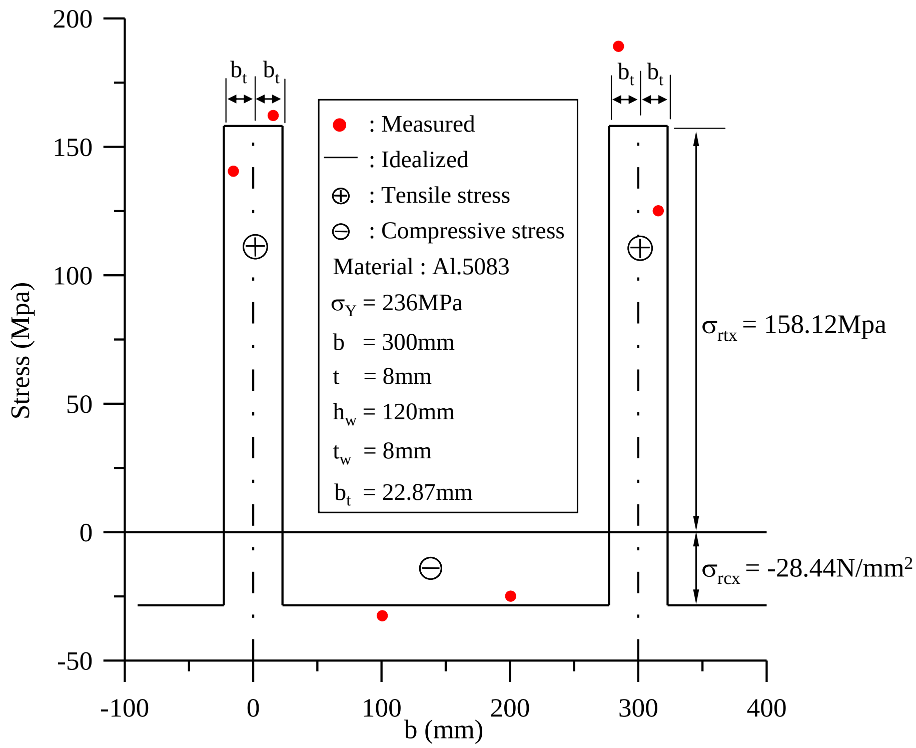


Fig.11(a) Welding residual stress distribution at plating between stiffeners for 5083 aluminum alloy panel measured after fabrication

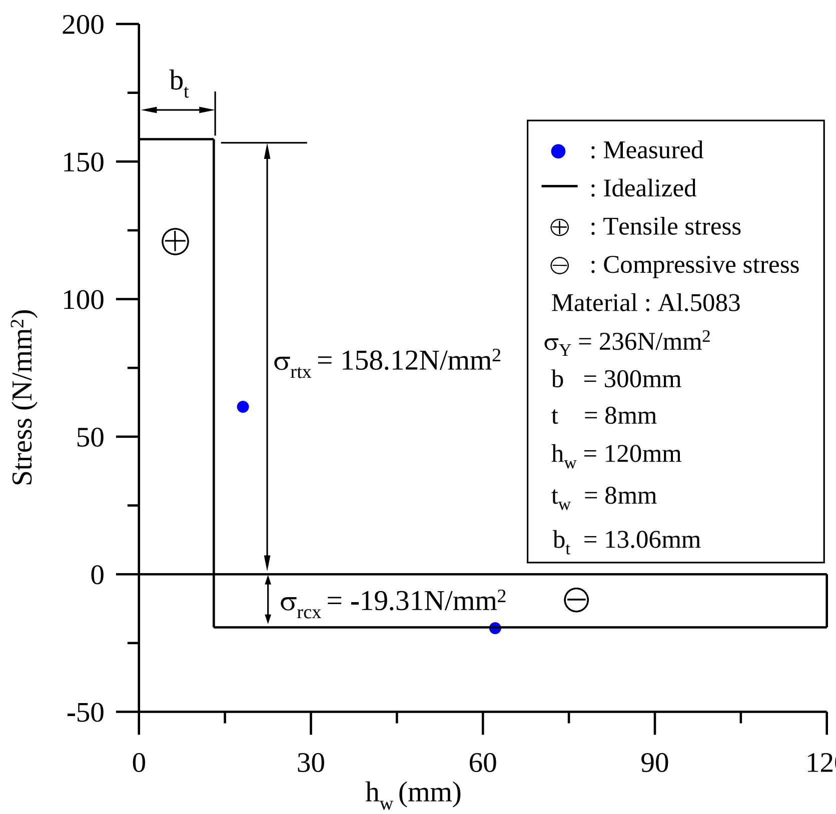


Fig.11(b) Welding residual stress distribution at stiffener web for 5083 aluminum alloy panel measured after fabrication

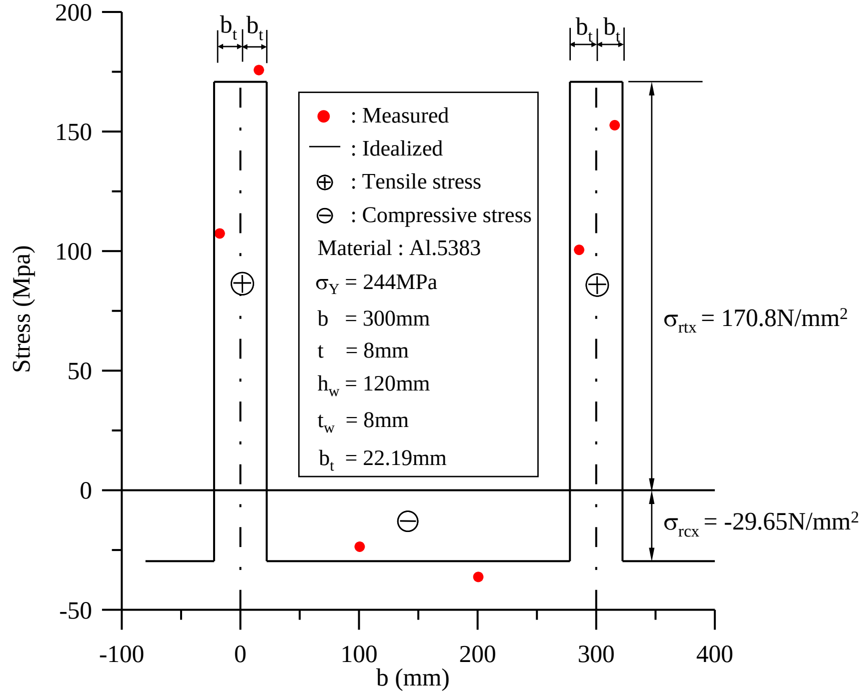


Fig.12(a) Welding residual stress distribution at plating between stiffeners for 5383 aluminum alloy panel measured after fabrication

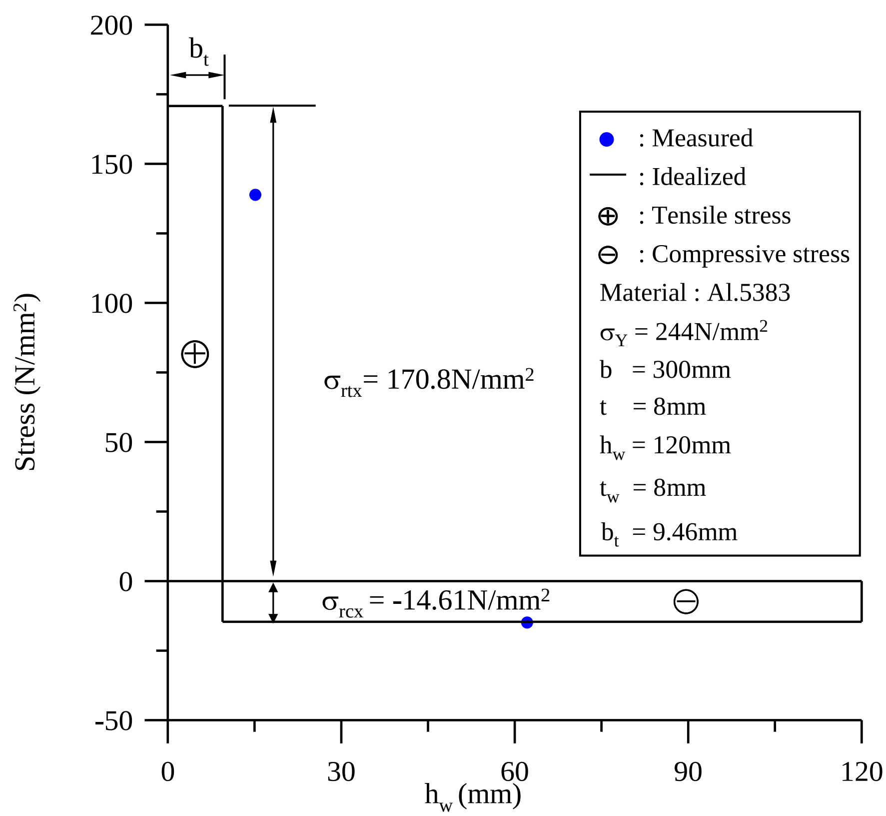


Fig.12(b) Welding residual stress distribution at stiffener web for 5383 aluminum alloy panel measured after fabrication

Measurements of Fabrication Related Residual Stresses

The residual stress distribution and magnitude in both plating (between stiffeners) and stiffener web were measured by the technique of drilling a hole.

Figures 11 and 12 show selected measurements of welding residual stress distribution for 5083 and 5383 panels, respectively. Table 4 is a summary of welding residual stress measurements at plating and stiffener web for 5083 and 5383 panels. The compressive residual stresses at aluminum plating were 12% of the material yield stress. It is worth noting that this is similar to an average level (15% of yield stress) of welding residual stresses in steel plates (Smith et al. 1988).

It is usually considered that the welding residual stress distribution of aluminum stiffened panels can be idealized like that of steel stiffened panels, where the residual stress distribution is composed of tensile residual stress block and compressive residual stress block.

Table 4 Summary of welding residual stress measurements for 5083 and 5383 panels (with 8mm-thick plate and 120mm-web height)

Residual stress	5083		5383	
	Plate	Web	Plate	Web
σ_{rtx} / σ_Y	0.67	0.67	0.7	0.7
σ_{rcx} / σ_Y	0.12	0.08	0.12	0.06
b_{HAZ} (mm)	22.87	13.06	22.19	9.46

Note: σ_{rtx} = tensile residual stress (positive), σ_{rcx} = compressive residual stress (negative), b_{HAZ} = breadth of the tensile residual stress block equivalent to the HAZ.

The tensile residual stress block with the breadth b_t is considered to be equivalent to the softening area in the HAZ. Table 4 confirms that the extent of the weld softening zone typically taken from the popular "1-in." rule (Kontoleon et al. 2000) is available for the plate between stiffeners.

Shakedown of Welding Residual Stresses Under Cyclic Loading

It has been said that welding induced residual stresses may be released to some extent after cyclic loading of the structures while in service. To examine this phenomenon quantitatively, some physical model tests were performed in the present study.

Two butt-welded 8mm-thick aluminum plate strips as shown in Fig.13, one for 5083 and the other for 5383 are tested. The test strip was also fabricated by the same welding machine of Hanjin Heavy Industries & Construction, Co., Ltd. Filler wire for welding is 5183 aluminum alloy.

A 3-point cyclic bending test was undertaken; a line load at the plate strip center was cyclically applied to generate sagging and hogging in the plate strip, as shown in Fig.14.

The maximum deflection of the plate strip after loading was about 9mm, which is equivalent to the maximum bending stress of about 208.4 N/mm^2 at the strip surface, meaning that the strip behaves within the elastic regime.

For the 5083 test strip, a total of 3 cyclic loads (i.e., 3 times for sagging and 3 times for hogging) was applied by turn, while a total of 5 cyclic loads was applied for the 5383 test strip.

Application of different loading cycles was considered to examine possible relaxation characteristics of welding residual stresses due to the number of loading cycles.

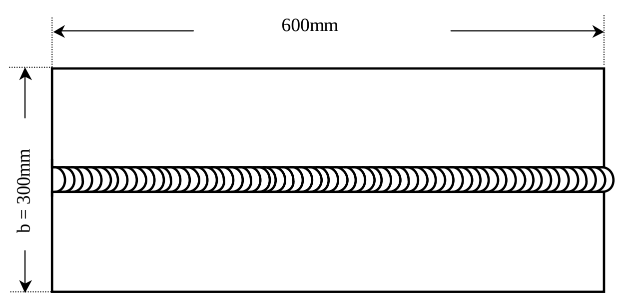


Fig.13 A butt-welded 8mm-thick plate strip

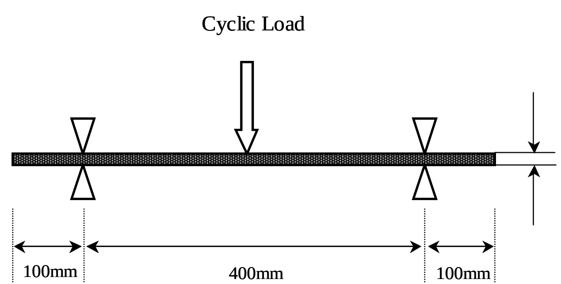


Fig.14 A 3-point cyclic bending test set-up on the butt-welded plate strip

The welding residual stresses were measured before and after the bending cycles using the same method noted in the previous section. Figure 15 shows the measurements of the residual stresses before and after the bending cycles.

The welding residual stresses have indeed been noticeably reduced after the load cycles. For compressive residual stresses which affect the plate buckling, the reduction ratio was by about 36% for the 5083 plate with 3 load cycles and by about 33% for the 5383 plate with 5 load cycles. For the tensile residual stresses, the reduction ratio was smaller than that for the compressive residual stresses, i.e., 21% for the 5083 plate and 7.6% for the 5383 plate.

While pending further study, it is confirmed that in-service cyclic loading can result in some distinct reduction in welding induced residual stresses.

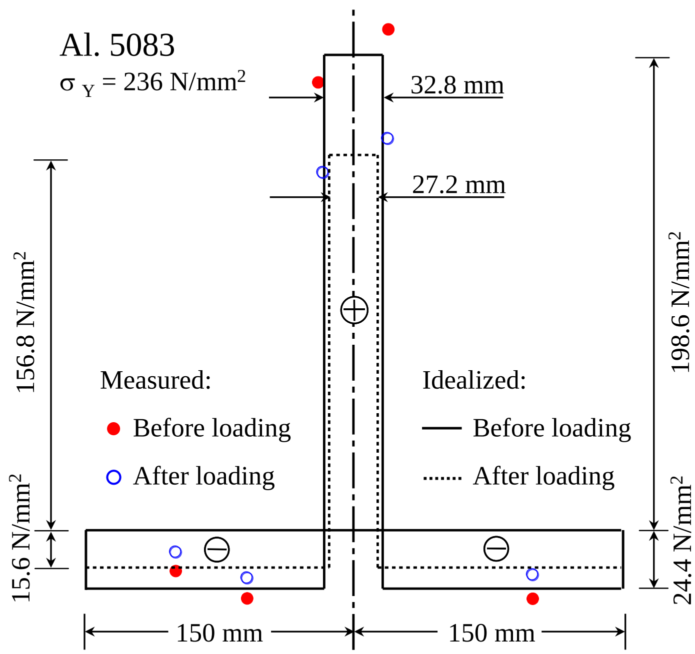


Fig.15(a) Welding residual stress distribution of the 5083 butt-welded plate strip before and after 3 cycles of 3-point bending

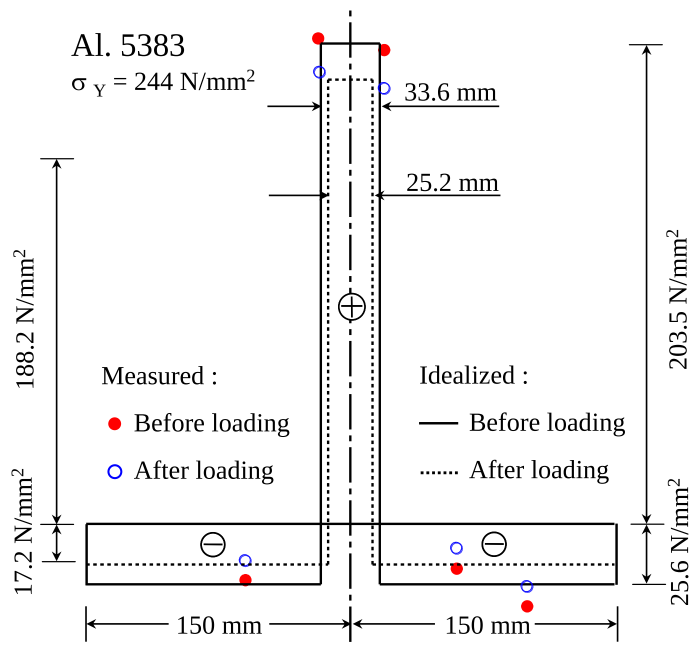


Fig.15(b) Welding residual stress distribution of the 5383 butt-welded plate strip before and after 5 cycles of 3-point bending

Fig.16(a) Test set-up for physical model testing on stiffened panels (unloaded edges are free)

Fig.16(b) Test set-up for physical model testing on stiffened panels (unloaded edges are simply supported and kept straight)

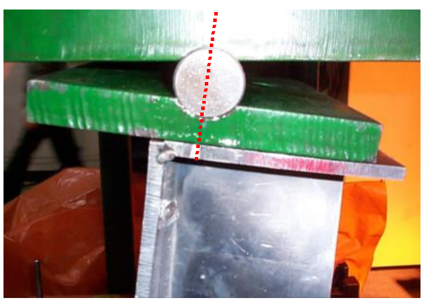


Fig.17 Simply supported condition at loaded edges and axial compressive loading at the neutral axis of the panel cross section

Fig.18(a) A photo of the tested panel failed by beam-column type collapse (Mode III) – unloaded edges remain free

Fig.18(b) A photo of the tested panel failed by stiffener tripping (Mode V) – unloaded edges remain free

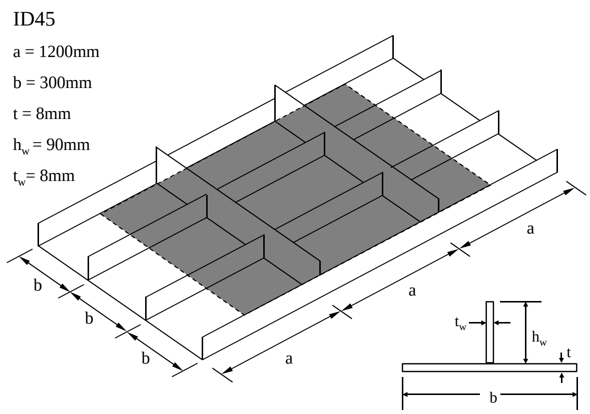


Fig.19(a) The extent of FEA by the 2 bay stiffened panel model (SPM)

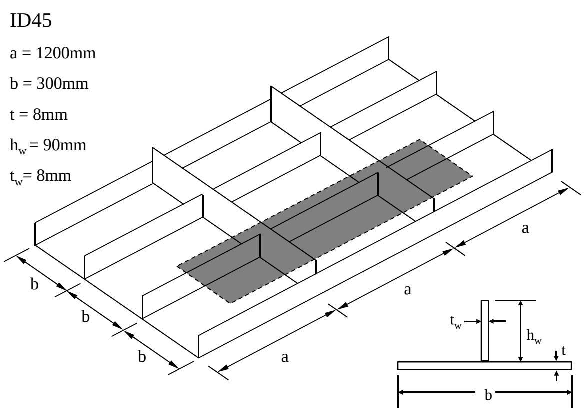


Fig.19(b) The extent of FEA by the 2 bay plate-stiffener combination (PSC)

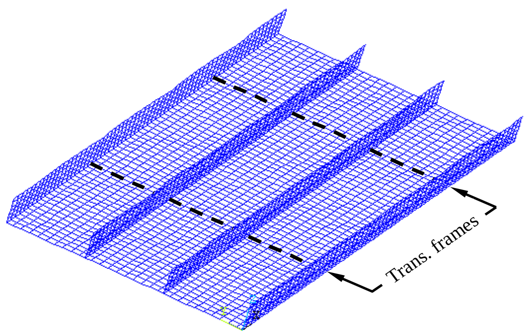


Fig.20(a) Nonlinear FEA model by the 2 bay stiffened panel model (SPM)

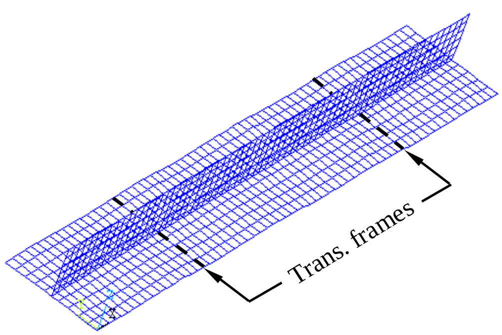


Fig.20(b) Nonlinear FEA model by the 2 bay plate-stiffener combination (PSC)

Physical Model Testing – Collapse Test Results

Figure 16 shows a set-up of the physical collapse testing on stiffened panel models. The loaded edges are simply supported and the axial compressive loading is applied at the neutral axis of the panel cross section as shown in Fig.17.

Two types of unloaded edge condition are considered, namely free and simply support conditions, as shown in Fig.16.

Figure 18 shows some typical patterns of the failed test panels after testing. Figure 18(a) shows the beam-column type collapse mode with buckling of plating between stiffeners – Mode III, and Figure 18(b) shows the failure of the panel by stiffener tripping – Mode V (Paik & Thayamballi 2003).

While the detailed description of the collapse patterns is not presented in this paper, it was observed that the panel collapse patterns were clearly different depending on the panel geometries.

Where the ratio of stiffener web height to web thickness is relatively large, the panel with flat bars mostly collapsed by tripping, while the beam-column type collapse took place for panels with a smaller web height.

THEORETICAL AND NUMERICAL ULS ANALYSIS OF ALUMINUM STIFFENED PANELS

Nonlinear Finite Element Analysis

ANSYS nonlinear finite element analysis was carried out on the test panels by a comparison with FEA and test results.

While some arguments in terms of selecting relevant FEA modeling techniques still remain, 8 types of FEA modeling are considered in the present study. The extent of analysis and the direction of column type initial deflection of stiffeners are as follows (where CIP = compression in plate side, CIS = compression in stiffener side, SPM = stiffened panel model, PSC = plate-stiffener combination model)

- 1 bay SPM with initial deflection in CIP
- 1 bay SPM with initial deflection in CIS
- 2 bay SPM with initial deflection in CIP
- 2 bay SPM with initial deflection in CIS
- 1 bay PSC with initial deflection in CIP
- 1 bay PSC with initial deflection in CIS
- 2 bay PSC with initial deflection in CIP
- 2 bay PSC with initial deflection in CIS

In addition to the 8 models noted above, another 2 bay FE model was considered with the unloaded edges being simply supported and kept straight, namely

- 2 bay SPM with all (four) edges simply supported

While the test panels are primarily a 1 bay system, i.e., considering the longitudinally stiffened panels between two transverse frames, a 2 bay system including transverse frames as shown in Fig.19 is also considered in the present FEA to reflect the continuity support condition along the transverse frames in a continuous plate structure.

All of the 1 bay models are analyzed using load control, while the 2 bay models are loaded using displacement control, because of easier application of the load with regard to the neutral axis at the panel cross section.

After some convergence studies, the FE mesh size adopted has one plate-shell element representing the HAZ at plating and at the stiffener web, ten plate-shell elements represent the plating between stiffeners, and six elements model stiffener web, including the elements at the HAZ. Figure 20 shows nonlinear FEA models by the 2 bay SPM or PSC.

The softening in the HAZ is considered in the FEA, where the reduced yield stress ratio in the HAZ is set by the guidance of classification societies, i.e., 0.67 for 5083 and 0.7 for 5383. The welding induced residual stresses are also considered in the FEA with the measured values.

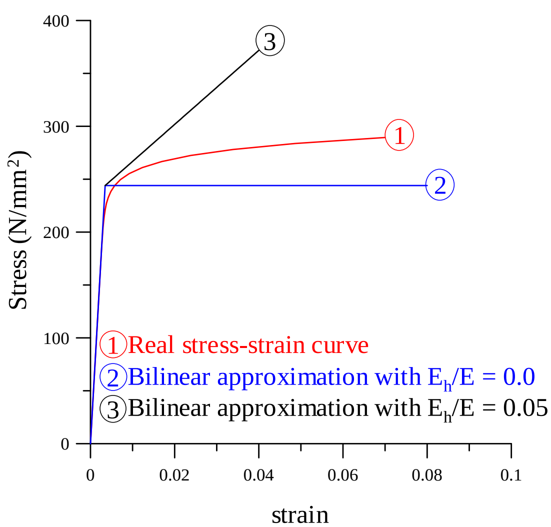


Fig.21(a) Three types of material stress-strain relation modeling

While some details of the nonlinear FEA in terms of FE meshing and material stress-strain relation idealization may be found from Paik & Duran (2004), Figure 21 shows the modeling effects of material stress-strain relationship on the aluminum panel ultimate strength behavior, where three different models including real stress-strain curve usage. The same characteristics of HAZ softening were considered in this comparison.

It is seen from Figs.21(b) and 21(c) that the elastic-perfectly plastic material model neglecting the strain-hardening effect does not provide more conservative results than the case of the real material properties.

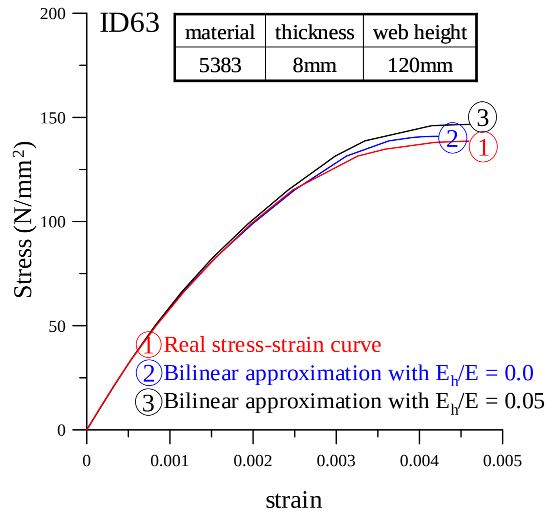


Fig.21(b) Effect of material stress-strain relation models on the aluminum panel ultimate strength behavior obtained by 1 bay PSC FEA with stiffener column type initial deflection in CIP

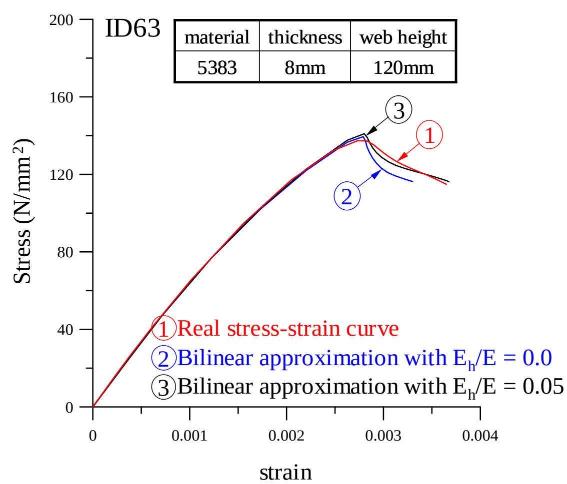


Fig.21(c) Effect of material stress-strain relation models on the aluminum panel ultimate strength behavior obtained by 2 bay SPM FEA with stiffener column type initial deflection in CIS

This is in contrast to steel plated structures where the elastic-perfectly plastic material approximation always gives lower ultimate strength estimates than the case with real material stress-strain relationship. This is because the elastic-plastic regime of material after the proportional limit (before the yield point) plays a role in the collapse behavior of aluminum structures unlike steel structures where it can be neglected.

For practical fast ULS calculations of aluminum structures, however, it is considered that the elastic-perfectly plastic material approximation neglecting the elastic-plastic effect after the proportional limit (before the yield point) and also the strain-hardening effect (after the yield point) may be acceptable as for steel structures as long as the softening effect in the HAZ is accounted for, i.e., by considering the reduced yield stress in the HAZ.

Figure 22 compares FEA solutions obtained by the 9 types of FE modeling noted above together with test data for two selected test panels.

It is to be noted in Fig.22 that all FEA except for No. 10 were undertaken considering that the unloaded edges are free as in the actual testing, while No.10 considers the unloaded edges (as well as the loaded edges) as simply supported and kept straight.

The reason why the in-plane stiffness of the panel obtained by the test is slightly greater than that by the FEA is due that the former involves the real pattern of initial distortions with the so-called hungry horse shape, while the latter presumes the buckling mode initial deflection of plating for convenience of FE modeling. The buckling mode initial deflection shape reduces in-plane stiffness compared to the hungry horse shape (Paik et al. 2004a).

In the testing, the test panel ID 40 collapsed by column type collapse (Mode III) and ID 63 collapsed by stiffener tripping (Mode V).

As would be expected, it is evident that the direction of column type initial deflection of the stiffener significantly affects the FE solutions.

It is seen that the 2 bay FEA always gives a larger ULS than 1 bay FEA. This is because the 2 bay FEA involves the rotational restraint effects along the transverse frames in the continuous plate structures.

It is to be noted that the different FE modeling approaches give quite different solutions. It is of vital importance to correctly reflect all of the influential parameters in the FE modeling in this regard.

It is important to recognize that the direction of column type initial deflections of stiffeners, among other factors, may significantly affect the ultimate strength behavior when the magnitude of initial deflections is substantially large.

It is evident that the type and extent of model in the FE analysis must be determined carefully. The elastic-perfectly plastic material approximation for the analysis of welded aluminum structures may not always give conservative results unlike for steel structures.

Since softening in the HAZ plays a significant role on the welded aluminum plate structures, it must be carefully dealt with as well. These aspects definitely make the aluminum panel ULS evaluation works cumbersome. ALPS/ULSAP and closed-form formulations will be useful alternatives for quick ULS estimates of aluminum stiffened panels as well as steel panels in this regard.

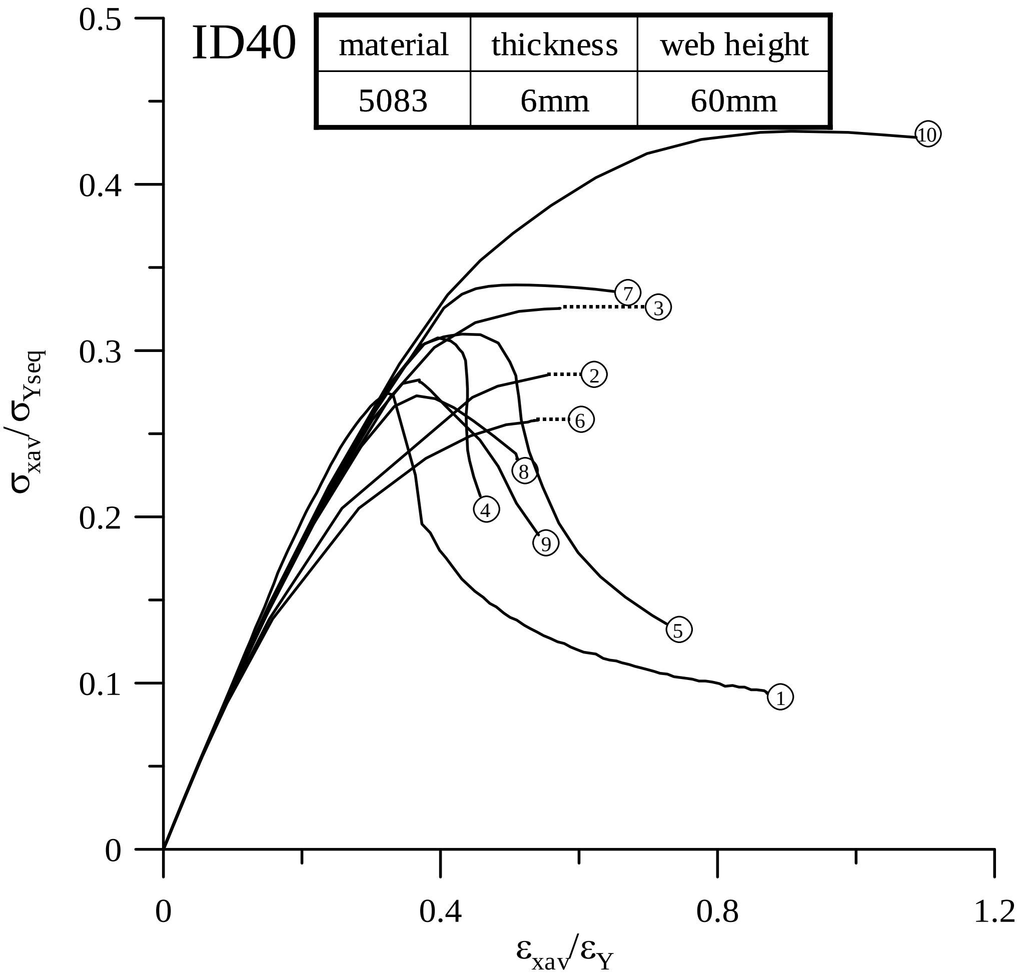
ALPS/ULSAP Program

ALPS/ULSAP program (ALPS/ULSAP 2005) performs ULS assessment of steel or aluminum stiffened panels using a semi-analytical method. The detailed theory of ALPS/ULSAP may be found from Paik & Thayamballi (2003).

The main features of the ALPS/ULSAP program include (Paik & Seo 2005):

- ULS assessment of unstiffened plates, uni-axially stiffened panels and cross-stiffened panels.
- Six types of collapse modes, namely overall collapse (Mode I), biaxially compressive collapse (Mode II), beam-column type collapse (Mode III), stiffener web buckling (Mode IV), stiffener tripping (Mode V) and gross yielding (Mode VI).
- Any combination of load components, namely longitudinal axial compression or tension, transverse axial compression or tension, edge shear, longitudinal in-plane bending, transverse in-plane bending and lateral pressure can be applied.
- Either steel or aluminum alloy material can be dealt with, considering the softening effect in the heat affected zone (HAZ) caused by welding.
- Initial imperfections in the form of initial deflections and welding residual stresses are dealt with as parameters of influence.
- Elastic-perfectly plastic material model is applied.
- Various types of structural degradation such as corrosion wastage (general or pitting), fatigue cracking and local denting are dealt with as parameters of influence.
- Impact pressure action arising from sloshing, slamming and green water can be analyzed for providing permanent set in terms of panel deflection.

ALPS/ULSAP can be used in three ways: within MAESTRO to automatically evaluate ULS of all of the panels in a MAESTRO ship model, within MAESTRO to evaluate ULS of a single panel that is defined through MAESTRO's graphical menus, and as a standalone program to evaluate ULS of a single panel that is defined through a set of Visual Basic graphical menus.



① Experiment, collapse mode III (CIP)

② 1 bay FEA(SPM), collapse mode III (CIP), column type initial deflection with CIP

③ 1 bay FEA(SPM), collapse mode III (CIP), column type initial deflection with CIS

④ 2 bay FEA(SPM), collapse mode III (CIP), column type initial deflection with CIP

⑤ 2 bay FEA(SPM), collapse mode V (CIS), column type initial deflection with CIS

⑥ 1 bay FEA(PSC), collapse mode III (CIP), column type initial deflection with CIP

⑦ 1 bay FEA(PSC), collapse mode III (CIP), column type initial deflection with CIS

⑧ 2 bay FEA(PSC), collapse mode III (CIP), column type initial deflection with CIP

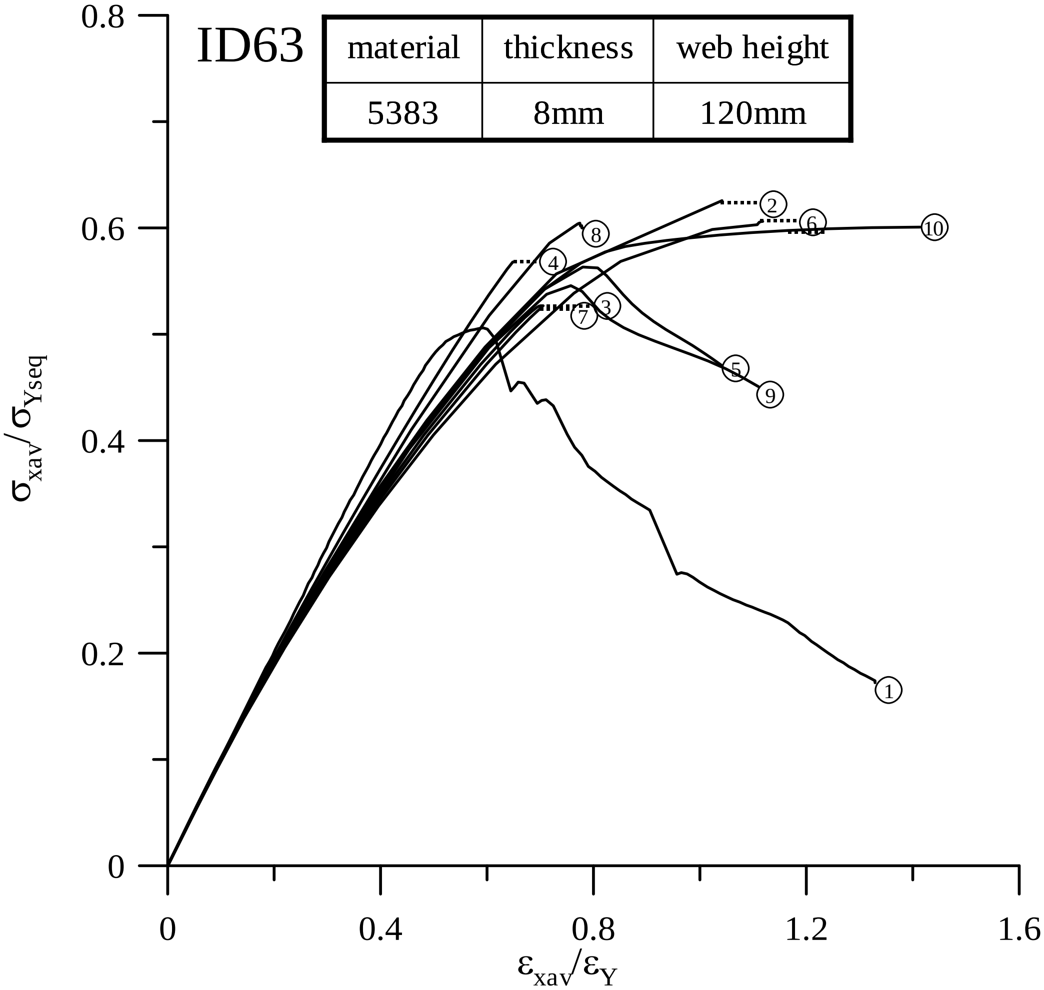
⑨ 2 bay FEA(PSC), collapse mode V (CIS), column type initial deflection with CIS

⑩ 2 bay FEA(SPM), collapse mode III (CIP), column type initial deflection with CIP

(All edges simply supported keeping them straight)

Note: CIP = compression in plate side, CIS = compression in stiffener side

Fig.22(a) Comparison of FEA solutions as those obtained by 9 types of FE modeling together with test data for a 5083 panel with 6mm-thick and 60mm-web height (σ_{xav} = average axial stress, σ_{Yseq} = yield stress, ε_{xav} = average axial strain, $\varepsilon_Y = \sigma_{Yseq}/E$, E = elastic modulus)



① Experiment, collapse mode V (CIS)

② 1 bay FEA(SPM), collapse mode III (CIP), column type initial deflection with CIP

③ 1 bay FEA(SPM), collapse mode V (CIS), column type initial deflection with CIS

④ 2 bay FEA(SPM), collapse mode III (CIP), column type initial deflection with CIP

⑤ 2 bay FEA(SPM), collapse mode V (CIS), column type initial deflection with CIS

⑥ 1 bay FEA(PSC), collapse mode III (CIP), column type initial deflection with CIP

⑦ 1 bay FEA(PSC), collapse mode V (CIS), column type initial deflection with CIS

⑧ 2 bay FEA(PSC), collapse mode III (CIP), column type initial deflection with CIP

⑨ 2 bay FEA(PSC), collapse mode V (CIS), column type initial deflection with CIS

⑩ 2 bay FEA(SPM), collapse mode V (CIS), column type initial deflection with CIS

(All edges simply supported keeping them straight)

Note: CIP = compression in plate side, CIS = compression in stiffener side

Fig.22(b) Comparison of FEA solutions as those obtained by 9 types of FE modeling together with test data for a 5383 panel with 8mm-thick and 120mm-web height (σ_{xav} = average axial stress, σ_{Yseq} = yield stress, ε_{xav} = average axial strain, $\varepsilon_Y = \sigma_{Yseq}/E$, E = elastic modulus)

Closed-form ULS Design Formulations

Closed-form design formulations are often very useful for a first cut estimate of the panel ULS and also they are needed to do risk or reliability assessment which requires repetitive calculations to identify the uncertain characteristics of the problem.

In the present study, empirical formulae were developed by curve-fitting of the ANSYS computations and available test data (Paik & Duran 2004, Paik et al. 2004b).

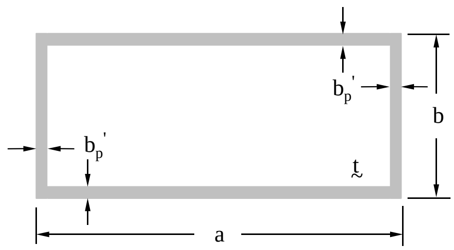


Fig.23 Softening zones inside a welded aluminum plate

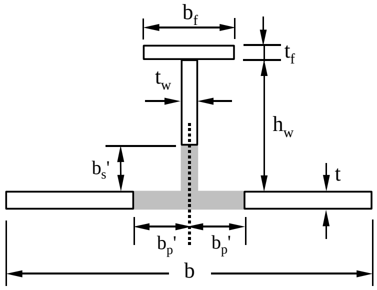


Fig.24 Cross section of the plate-stiffener combination with softening zones

For welded aluminum plates simply supported at all (four) edges and under axial compressive loads, the ultimate strength formula developed by the authors is as follows

$$\frac{\sigma_{xu}}{\sigma_{Yp}'} = \begin{cases} 1.0 & \text{for } \beta' \le 0.46 \\ -0.215\beta' + 1.1 & \text{for } 0.46 < \beta' \le 2.2 \\ -0.083\beta' + 0.81 & \text{for } \beta' > 2.2 \end{cases} \tag{2}$$

where $\beta' = \frac{b}{t}\sqrt{\frac{\sigma_{Yp}'}{E}}$, $\sigma_{Yp}' = \frac{P_p}{ab}$,

$$P_p = (a - 2b_p')(b - 2b_p')\sigma_{Yp} + 2[ab_p' + (b - 2b_p')b_p']\sigma_Y',$$

a = plate length, b = plate breadth, b'_P = breadth of softening in HAZ (heat affected zone), see Fig.23, σ'_Y = yield stress in HAZ, σ_{Yp} = yield stress of the plate in base metal.

In Equation (2), β' takes into account the effect of softening in the HAZ in terms of the plate volume or surface.

Equation (2) implicitly considers an average level of initial deflection (e.g., w_{opl} = 0.009b), while the effect of welding residual stress is not accounted for.

For aluminum stiffened panels simply supported at all (four) edges and under axial compressive loads when they are modeled by a plate-stiffener combination as representing the panel, the following ultimate strength formula was derived by the authors for three levels of initial deflections [For symbols not defined below, see Eq.(2)].

$$\sigma_{xu} = \frac{\sigma_{Yseq}'}{\sqrt{C_1 + C_2(\lambda')^2 + C_3(\beta')^2 + C_4(\lambda'\beta)^2 + C_5(\lambda')^4}} \le \frac{\sigma_{Yseq}'}{(\lambda')^2} \tag{3}$$

where, $\lambda' = \frac{a}{\pi r}\sqrt{\frac{\sigma_{Yseq}'}{E}}$, $\sigma_{Yseq} = \frac{P_s}{bt + h_w t_w + b_f t_f}$,

$$P_s = (b - 2b_p')t\sigma_{Yp} + 2b_p' t\sigma_{Yp}' + (h_w - b_{sHAZ})t_w\sigma_{Ys} + b_{sHAZ}t_w\sigma_{YsHAZ} + b_f t_f \sigma_{Ys},$$

σ_{Ys} = yield stress of stiffener,

b'_P, b'_S, = breadths of HAZ softening in plating and stiffener web, see Fig.24, b_f = breadth of stiffener flange, t_f = thickness of stiffener flange, h_w = height of stiffener web, t_w = thickness of stiffener web, r = $\sqrt{(I / A)}$ = radius of gyration, I = moment of inertia of stiffener with attached plating, A = cross-sectional area of stiffener with attached plating.

In Eq.(3), the coefficients C_1 – C_5 are defined depending on the level of initial deflections of plating and stiffeners, as indicated in Table 5.

It is noted that Eqs.(2) and (3) do not account for welding residual stress effect, while the effects of initial deflections and softening in HAZ are taken into account.

When the welding residual stresses exist in the panel, the formulations must then be corrected to account for their effect. A knock-down factor approach may be applied, by multiplying R_r defined by

$$R_r = 1 - \frac{\sigma_{req}}{\sigma_{Yeq}} \tag{4}$$

where σ_{req} = equivalent compressive residual stress, σ_{Yeq} = equivalent yield stress.

Table 5 Coefficients depending on the levels of initial deflections of plating and stiffeners

Coefficient	Slight	Average	Severe
C_1	0.878	1.038	1.157
C_2	0.191	1.099	2.297
C_3	0.106	0.093	0.152
C_4	-0.017	-0.047	-0.138
C_5	1.30	1.648	3.684

Table 6(a) Dimmensional properties of the stiffened panels considered

ID	ST	a mm	b mm	t mm	h_w mm	t_w mm	b_f mm	t_f mm
P1	B	1000	200	8.5	100	6	0	0
P2	B	1000	200	8	100	5	0	0
P3	B	1000	200	6	100	6	0	0
P4	B	1000	200	6	100	6	0	0
P5	B	1000	200	6	75	6	0	0
P6	B	1000	200	6	75	6	0	0
P7	B	1000	200	5	100	5	0	0
P8	B	1000	200	5	80	5	0	0
P9	B	1000	200	5	60	5	0	0
P10	B	1000	200	5	60	6	0	0
P11	T	750	250	4	52	4	17	13
P12	J	431.8	285.5	1.9	23.4	2	7.1	2
P13	T	530	160	9.5	59	9	36	9

Notes: ST = Stiffener type, B = Flat Bar, T = Tee, J = J-type.

Table 6(b) Geometric and material properties of the stiffened panels considered

ID	β	λ	Material Type
P1	1.43	0.69	6082-T6
P2	1.39	0.65	5083-H116
P3	2.03	0.65	6082-T6
P4	2.03	0.65	6082-T6
P5	2.03	0.91	6082-T6
P6	2.03	0.91	6082-T6
P7	2.22	0.59	5083-H116
P8	2.22	0.77	5083-H116
P9	2.22	1.10	5083-H116
P10	2.44	1.15	6082-T6
P11	3.40	0.56	5083-H116
P12	10.44	1.78	6013 T6 sheet
P13	1.45	0.56	7449 T7951 100mm

Notes: $\beta = \frac{b}{t}\sqrt{\frac{\sigma_{Yp}}{E}}$, $\lambda = \frac{a}{\pi r}\sqrt{\frac{\sigma_{Yeq}}{E}}$, σ_{Yeq} = equivalent yield stress of stiffener with attached plating.

Table 6(c) Minimum base- and welded yield stresses for selected alloys

Alloy Temper	σ_Y	σ'_Y	f_R	E (GPa)	n_c
5083-H116	215	144	0.67	70	15
5383-H116	220	154	0.70	70	15
6082-T6	260	138	0.53	70	15
6013-T6	350	-	-	72.5	21
7449-T7951 100mm	540	-	-	73	17

Notes: σ_Y = 'minimum' yield stress of base material, σ'_Y = 'minimum' yield stress of welded material, $f_R = \sigma'_Y / \sigma_Y$, E = Young's modulus, n_C = knee factor for the stress-strain relationship of the Ramberg-Osgood formula, , ν = Poisson's ratio (aluminum alloys) = 0.33.

Table 7 Sensitivity analysis of ALPS/ULSAP and Eq.(3) for both average and severe levels of initial deflections (upper digits for average level and lower digits for severe level)

ID	ALPS/ULSAP(A)		Eq.(3) (B)	
	A/Exp.	A/FEA	B/Exp.	B/FEA
P1	1.126 0.848	0.922 0.695	1.241 0.995	1.017 0.815
P2	1.003 0.661	1.057 0.696	0.954 0.774	1.006 0.816
P3	1.404 0.870	1.119 0.693	1.320 1.074	1.052 0.856
P4	1.333 0.826	1.119 0.693	1.253 1.019	1.052 0.856
P5	0.953 0.738	1.116 0.863	0.931 0.708	1.089 0.829
P6	1.113 0.862	1.116 0.863	1.087 0.827	1.089 0.829
P7	1.151 0.853	0.948 0.703	1.256 1.034	1.035 0.852
P8	1.039 0.784	1.007 0.759	1.071 0.846	1.038 0.820
P9	1.214 1.201	0.897 0.887	1.214 0.892	0.897 0.659
P10	0.766 0.888	0.828 0.960	0.899 0.659	0.972 0.713
P11	1.005 0.916	0.869 0.793	1.088 0.895	0.941 0.774
P12	0.516 0.516	0.662 0.661	0.413 0.413	0.530 0.530
P13	1.582 1.256	1.460 1.159	1.340 1.118	1.237 1.032
Mean	1.093 0.863	1.009 0.802	1.082 0.866	0.996 0.798
COV	0.249 0.226	0.192 0.178	0.230 0.226	0.163 0.148

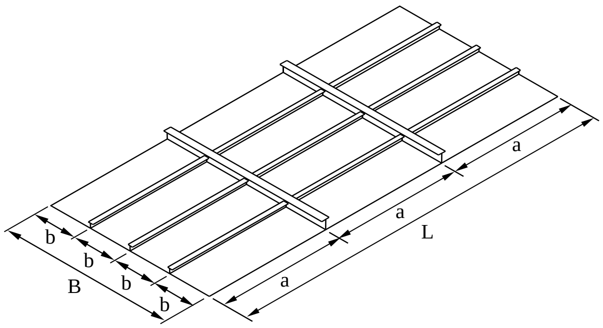


Fig.25 Nomenclature: a stiffened panel

As previously discussed, the residual stresses in aluminum structures can be released to some extent after time in service.

A comparison of Eq.(3) with ALPS/ULSAP, FEA and existing test data was undertaken for a number of 13 aluminum stiffened panels under axial compressive loads as indicated in Fig.25 and Table 6 (Paik et al. 2004b). Table 7 shows the results of the present sensitivity analysis.

Discussions

Based on some observations obtained from the present study on ULS of aluminum stiffened panels, the following conclusions can be drawn:

- It is evident that the nonlinear elastic-plastic large deflection FEA can give quite different ULS solutions depending on the difference of structural modeling as would be expected. If FE structural modeling does not properly reflect in terms of reflecting the reality in association with boundary conditions and initial imperfections as well as geometric / material properties and loading application, then the FEA may give wrong results for the ultimate strength behavior, and thus one should be careful in this regard.
- FEA solutions are significantly affected by the direction of column type initial deflections of stiffeners as well as their amplitude, among other factors. For instance, the direction of column type initial deflection of stiffeners, e.g., compression in plate side (CIP) or compression in stiffener side (CIS) can govern the direction of panel buckling deflection, leading to a different collapse pattern.
- By considering the continuity of stiffened panels in a continuous plate structure and the related rotational restraints along transverse frames, 2 bay FEA modeling is recommended.
- It is seen that the elastic-perfectly plastic material model may not give conservative ULS solutions but its effect is small. For practical purposes, therefore, the stress-strain relationship of aluminum alloy can be approximated by the elastic-perfectly plastic model.
- The effect of softening in the HAZ is very significant on the ultimate strength behavior of aluminum panels. Therefore, the reduced yield stress in the HAZ must be considered for FEA or other analytical approaches.
- The ULS solutions obtained by ALPS/ULSAP which apply the elastic-perfectly plastic material model together with the softening effect in the HAZ are in reasonably good agreement with nonlinear FEA and experimental results.

PROGRESSIVE COLLAPSE ANALYSIS OF THE ALUMINUM CATAMARAN FAST SHIP HULL

ALPS/HULL Program

The ALPS/HULL program (ALPS/HULL 2005) performs the progressive collapse analysis of ship hulls until and after the ultimate strength is reached.

The main features of the program include (Paik & Thayamballi 2003, Paik 2004, Paik & Seo 2005):

- Progressive collapse analysis of ship hulls until and after the ultimate limit state is reached, using simplified nonlinear finite element method (idealized structural unit method).
- Any combination of hull girder load components, namely vertical bending, horizontal bending, sectional shear and torsion can be applied.
- All possible failure modes of structural components such as overall buckling collapse, column type collapse, stiffener web buckling, tripping (lateral-torsional buckling) and gross yielding are considered.
- Either steel or aluminum alloy ship hulls can be dealt with, considering the softening effect in the heat affected zone caused by welding.
- Initial deflections and welding residual stresses are dealt with as parameters of influence.
- Various types of structural degradation, e.g., corrosion wastage (general or pitting), fatigue cracking
  and local denting are dealt with as parameters of influence.
- Structural modeling is undertaken using the MAESTRO modeler.
- In addition to numerical results the stress distribution, failure mode and vertical bending versus curvature curve can be displayed graphically.

ALPS/HULL employs ALPS/ULSAP as a major module for the ULS evaluation of plate panels or support members.

While a ship hull can be idealized as an assembly of various types of structural units such as plates, stiffened panels, support members or plate-stiffener combinations for ALPS/HULL application, as those shown in Fig.26, it is recognized that it is better to model the ship hull as an assembly of plate-stiffener separation units rather than plate-stiffener combination units or stiffened panel units.

It is to be noted that ALPS/HULL progressively evaluates the ULS of individual structural components as functions of combined stresses applied by hull girder actions. Under pure vertical bending, for instance, side shell panels or bottom girder plates are subjected to edge shear stresses or inner bottom panels are subjected to transverse axial stress, although they may be predominantly subjected to longitudinal bending stresses. ALPS/HULL accounts for the effects of all stress components in the failure and stiffness assessment of individual structural components.

Applicability of ALPS/HULL Program – Frigate Hull Tested by UK Royal Navy

While experimental results on aluminum ship hulls are not found in the literature, several existing mechanical collapse test data for steel ship hulls under vertical bending moments have been adopted by the authors for the validation of ALPS/HULL progressive collapse analysis program (ALPS/HULL 2005). The U.K. Royal Navy performed the collapse testing on a 1/3 scale steel frigate hull model under sagging bending moment (Dow 1991). The test results for the frigate hull model are now compared with ALPS/HULL solutions.

For the frigate test hull, a slice of the hull section between two transverse frames was taken as the extent of analysis. All hull sections are located at maximum applied bending moment.

Figures 27(a) to 27(c) show the distributions of axial stresses, von Mises stresses and collapse modes of structural components at ULS of the frigate test hull under sagging or hogging condition, obtained by ALPS/HULL. The computing time required by a laptop computer with Pentium (M) processor was about 5 sec.

It is evident from Fig.27(b) that the plate elements of the hull are subjected to transverse axial stress and shear stress as well as longitudinal stress even under vertical bending alone. It is observed from Fig.27(c) that buckling collapse took place at the deck panels and gross yielding occurred at bottom panels until the ULS is reached, while the mid-height part of the hull still remains intact. It is confirmed from Fig.27(d) that ALPS/HULL progressive analysis is in very good agreement with the experimental results for the frigate test ship hull.

Applicability of ALPS/HULL Program – Double Skin Tanker Hull Tested by NSWCCD

The Naval Surface Warfare Center, Carderock Division (NSWCCD), USA (Bruchman 2000) performed collapse testing on a steel double-skin tanker model with a uni-directional girder system under both sagging and hogging conditions. The progressive collapse analysis results by ULTSTR program (Bruchman 2000) were also compared with the test data.

For ALPS/HULL progressive collapse analysis, the test hull is modeled as an assembly of plate units. The full hull between two transverse bulkheads is taken as the extent of ALPS/HULL analysis.

Figures 28(a) to 28(c) show the distributions of axial stresses, von Mises stresses and collapse modes of structural components for the double-skin tanker test hull under sagging or hogging.

Again, it is apparent from Fig.28(b) that the plate elements of the hull are subjected to transverse axial stress and shear stress as well as longitudinal axial stress even under vertical bending alone. It is seen from Fig.28(c) that until ULS is reached under the sagging condition buckling collapse took place at the upper part, while gross yielding occurred at the lower part. No elements remain intact, in contrast to typical ship hulls which are composed of stiffened panels together with support members. It is interesting to note that this type of unique hull girder system is very efficient and advantageous in terms of expecting a full contribution of all plate elements against the hull girder collapse process.

It is confirmed from Fig.28(d) that ALPS/HULL and ULTSTR analyses are in reasonably good agreement with the experimental results.

Progressive Collapse Analysis of the Aluminum Catamaran Fast Ship Hull under Vertical Bending

Based on the verification examples noted above, it is considered that the ALPS/HULL program is useful for the progressive collapse analysis of aluminum ship hulls as well as steel ship hulls; ALPS/ULSAP is employed as a main module of ALPS/HULL for ULS assessment of aluminum or steel structural components considering possible softening effect in the HAZ, and its applicability has been verified.

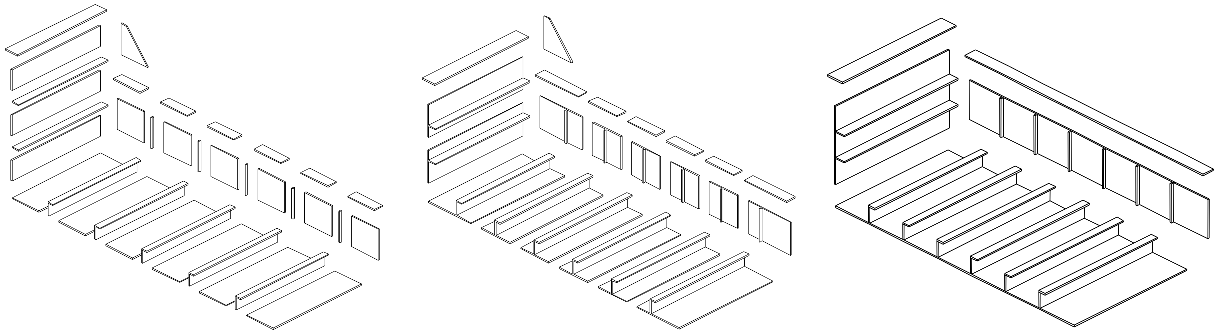


Fig.26 Structural idealization as an assembly of plate-stiffener separation models (left) or plate-stiffener combination models (middle) or stiffened panel models (right)

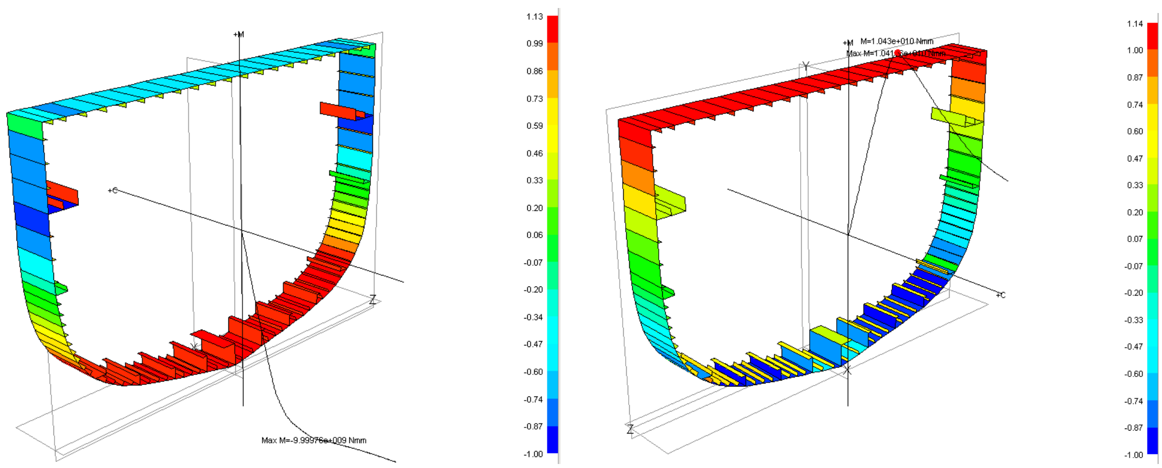


Fig.27(a) Axial stress distributions at ULS for the frigate test hull under sagging (left) and hogging (right), obtained by ALPS/HULL

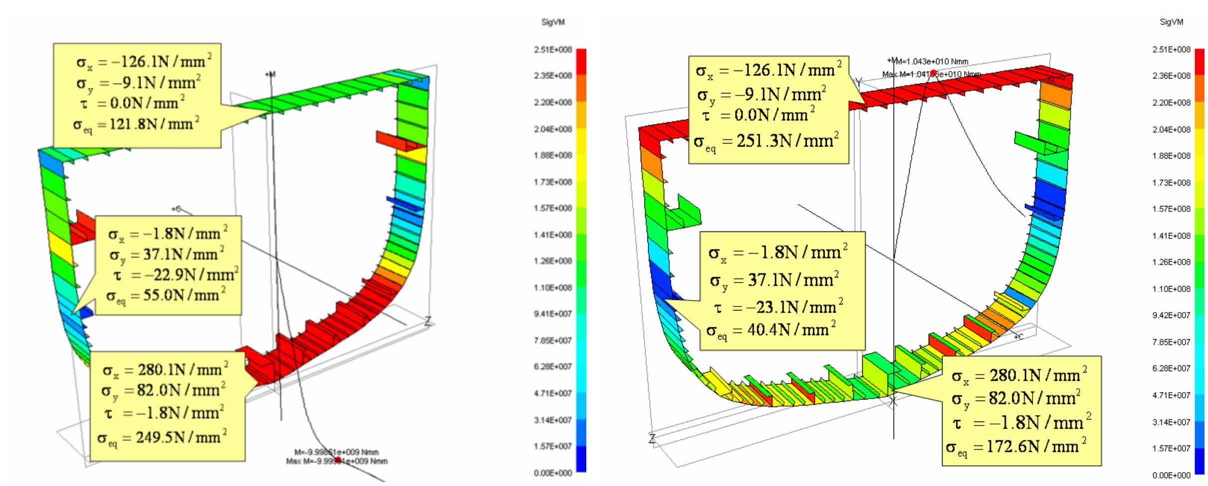


Fig.27(b) von Mises stress distributions at ULS for the frigate test hull under sagging (left) and hogging (right), obtained by ALPS/HULL

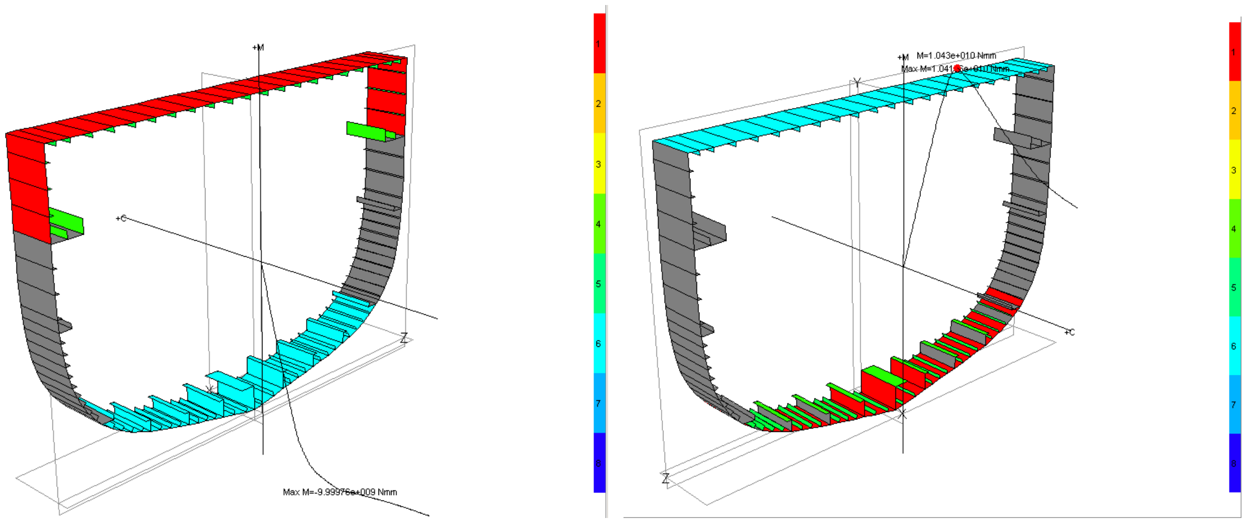


Fig.27(c) Collapse mode distributions of structural components at ULS for the frigate test hull under sagging (left) and hogging (right), obtained by ALPS/HULL

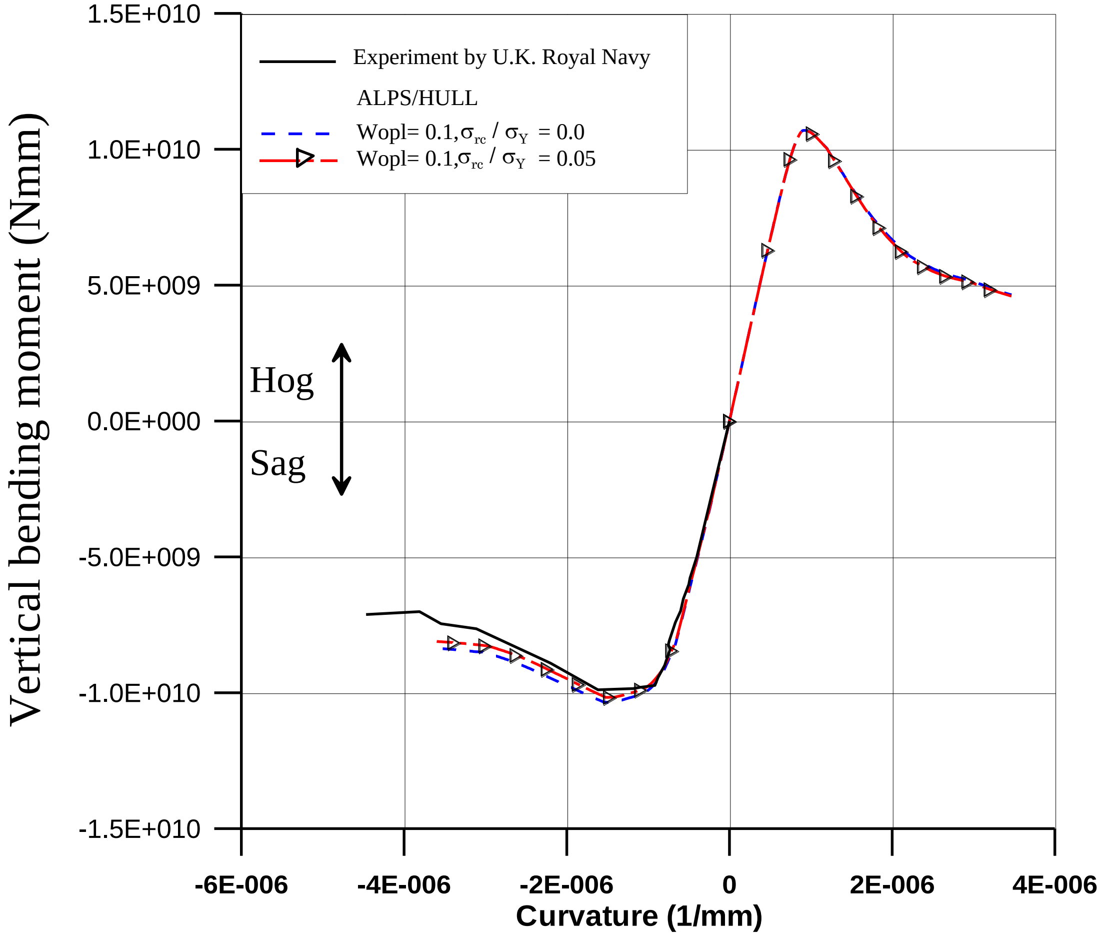


Fig.27(d) Vertical bending moment versus curvature for the frigate test hull, obtained by the experiment and ALPS/HULL

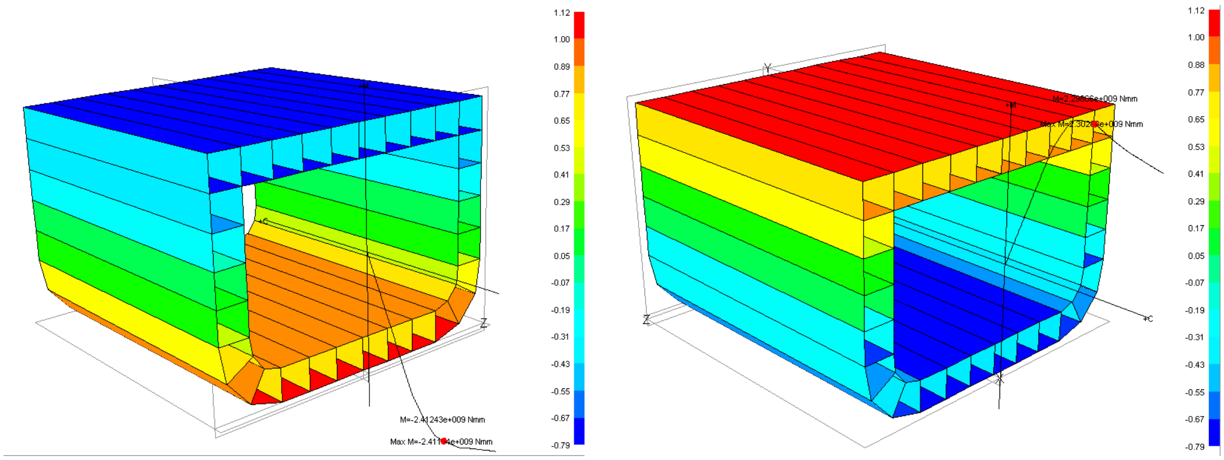


Fig.28(a) Axial stress distributions at ULS for the uni-directional double-skin tanker test hull under sagging (left) and hogging (right), obtained by ALPS/HULL

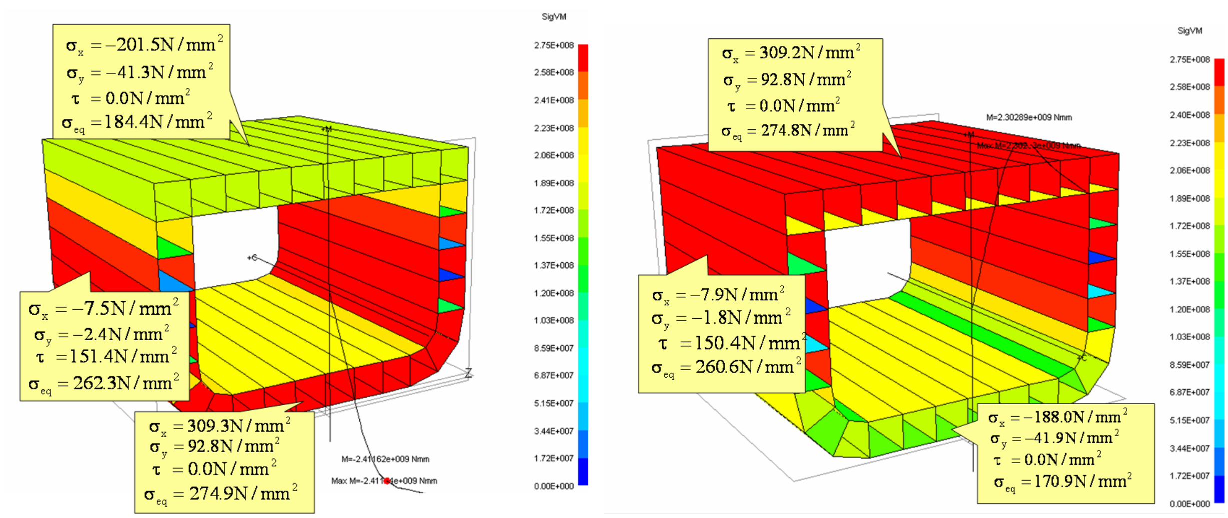


Fig.28(b) von Mises stress distributions at ULS for the uni-directional double-skin tanker test hull under sagging (left) and hogging (right), obtained by ALPS/HULL

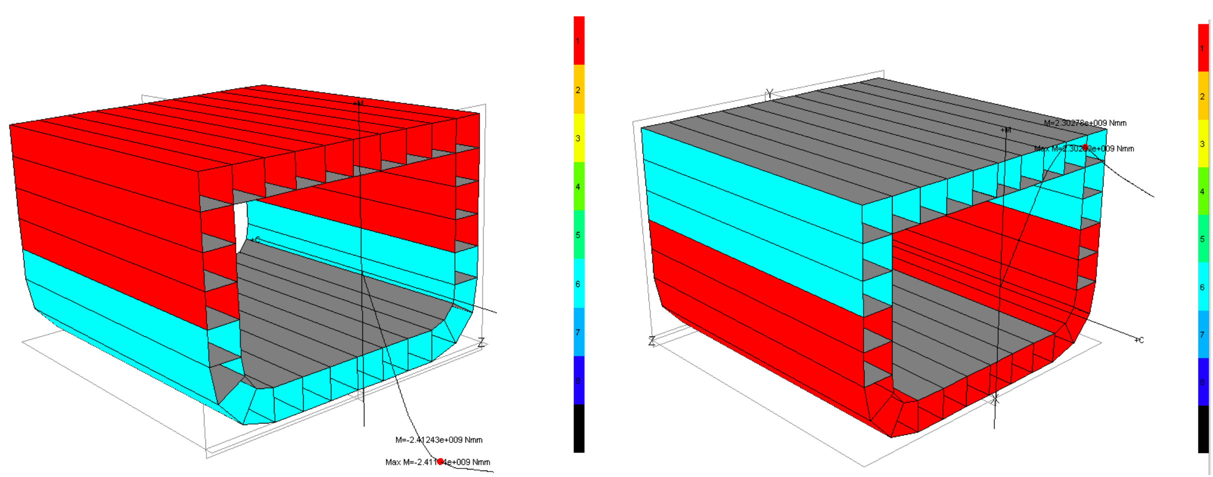


Fig.28(c) Collapse mode distributions of structural components at ULS for the uni-directional double-skin tanker test hull under sagging (left) and hogging (right), obtained by ALPS/HULL

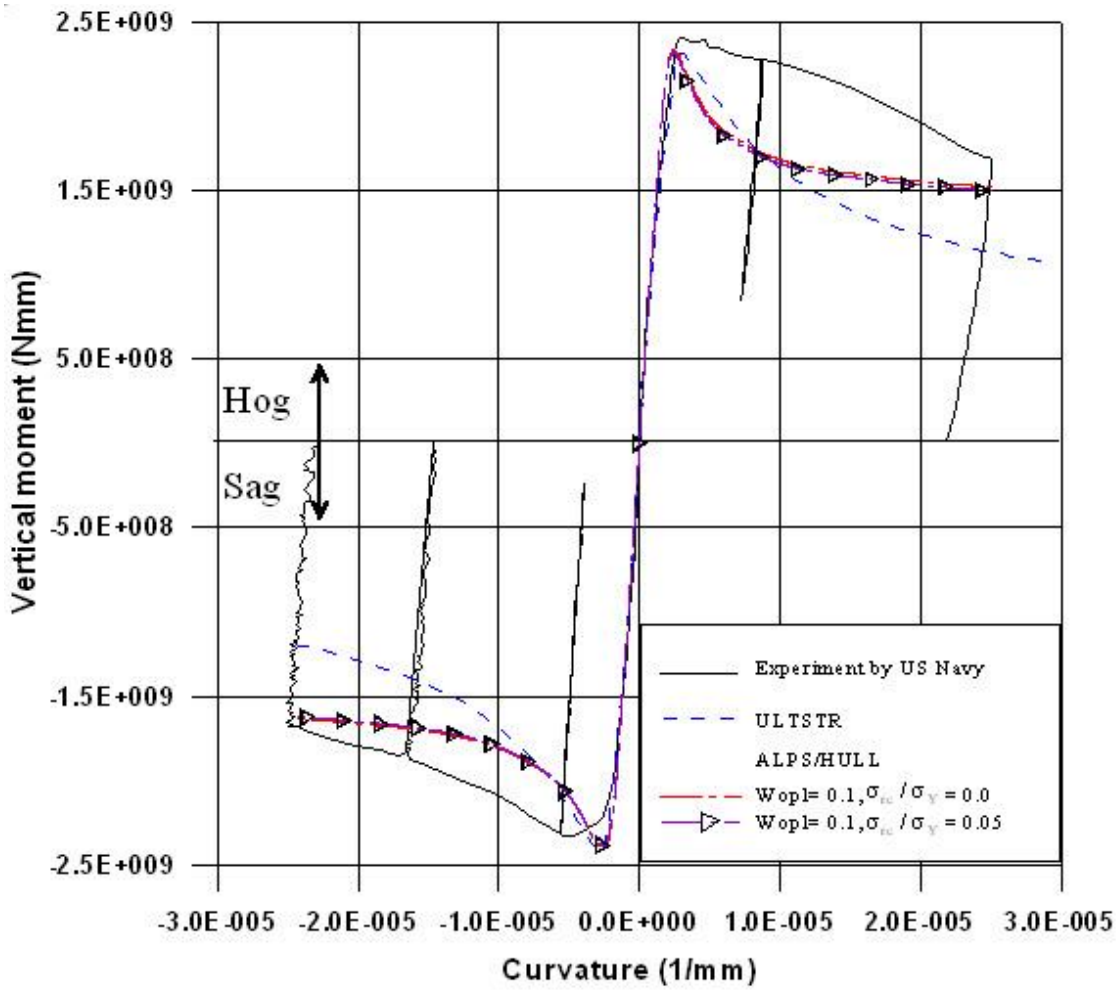


Fig.28(d) Vertical bending moment versus curvature for the uni-directional double-skin tanker test hull, obtained by the experiment, ULTSTR and ALPS/HULL

ALPS/HULL is now applied to progressive collapse analysis of the aluminum catamaran ship hull under vertical bending moment.

A sliced hull section between two transverse frames is taken as the extent of the analysis, since the transverse frames are strong enough so that they do not fail before the longitudinal strength members.

For ALPS/HULL simulations, the ship hull is modeled as an assembly of plate elements and support members, the modeling method being similar to the frigate test hull described in the previous section. It is considered that all structural elements have an 'average' level of initial imperfections. The ALPS/HULL model for the ship hull was developed by the MAESTRO modeler.

While the present ship hull is made of aluminum 5383, it is assumed that the yield stress of the material is 220 N/mm^2 and the reduced yield stress due to softening at the HAZ is 220*0.7 = 154 N/mm^2. The breadth of the HAZ after welding is presumed to be 25mm.

Figure 29 shows the ALPS/HULL model together with stress distributions, collapse mode distribution and the progressive collapse analysis results for the aluminum catamaran ship hull under sagging or hogging moments.

The maximum applied bending moment of the ship obtained by direct calculations using MAESTRO as previously noted is 445 MNm for hogging and 532 MNm for sagging. The ultimate bending moment obtained by ALPS/HULL is 2120 MNm for hogging and 1940 MNm for sagging.

From Eq.(1), the safety measure of the ship is determined as 2120/445 = 4.76 for hogging, and 1940/532 = 3.65 for sagging, which both are remarkably greater than 1.0, concluding that the present ship hull is robust enough to withstand maximum vertical bending moments arising at the worst loading and environmental conditions.

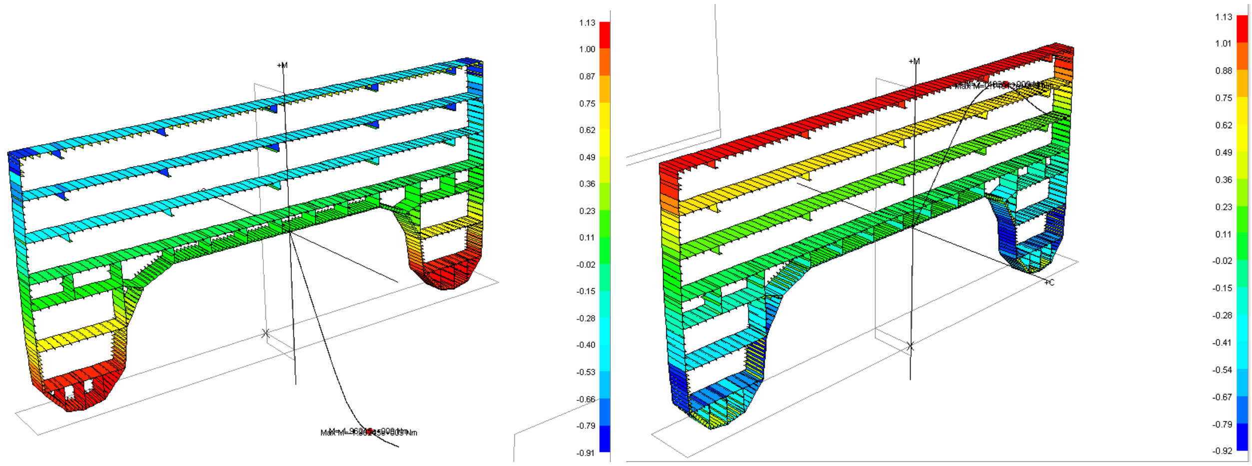


Fig.29(a) Axial stress distributions at ULS for the aluminum catamaran ship hull under sagging (left) and hogging (right), obtained by ALPS/HULL

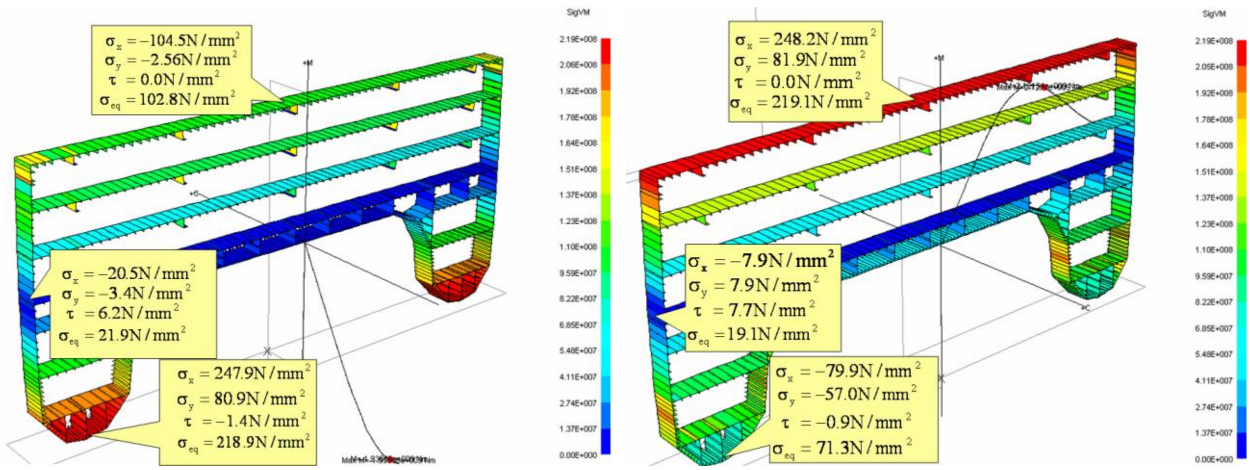


Fig.29(b) von Mises stress distributions at ULS for the aluminum catamaran fast ship hull under sagging (left) and hogging (right), obtained by ALPS/HULL

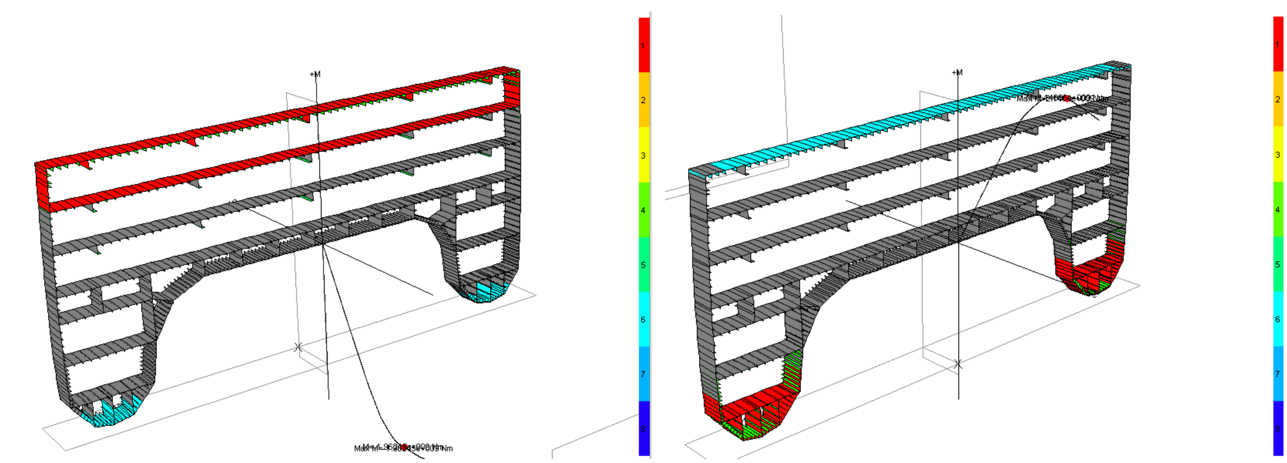


Fig.29(c) Collapse mode distributions of structural components at ULS for the aluminum catamaran ship hull under sagging (left) and hogging (right), obtained by ALPS/HULL

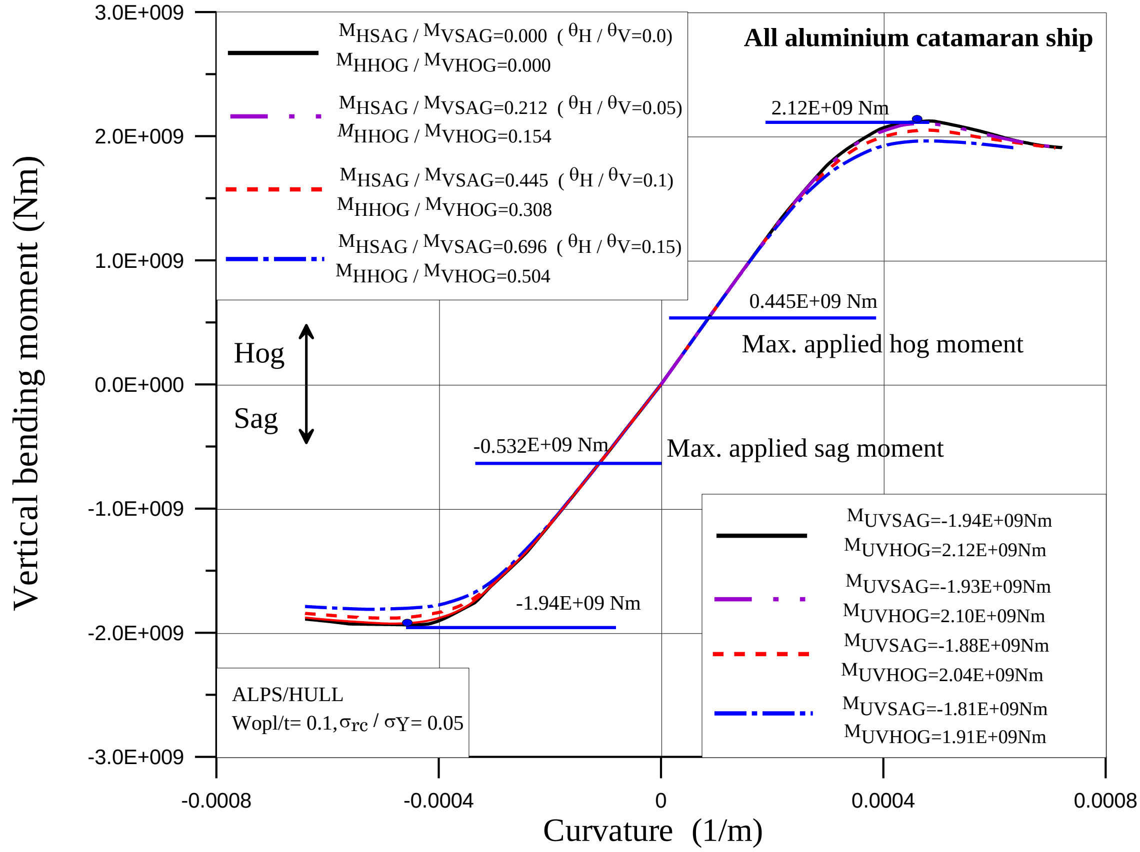


Fig.29(d) Vertical bending versus curvature for the hypothetical aluminum catamaran fast ship hull, obtained by ALPS/HULL

Effect of Horizontal Bending Moment on ULS of the Aluminum Catamaran Fast Ship Hull

Theoretically speaking, the effect of horizontal bending moment needs to be taken into account in the process of structural scantlings and hull girder ultimate strength evaluation.

In oblique or beam sea states, the horizontal bending moments of catamaran ships can be of importance, although they are usually negligible in head sea states (Faltinsen et al. 1992).

Figure 29(d) shows the effect of horizontal bending on the progressive hull girder collapse behavior under combined vertical and horizontal bending. For the present catamaran ship, the effect of horizontal bending moment is considered to be very small, because:

(1) The ship has a very large beam, more than twice the overall depth, and thus the horizontal moment of inertia for the hull cross section is very large, and

(2) The twin hulls are widely separated so that the ship has large roll stability and would not experience a large heel angle which is the main source of horizontal bending.

(3) The ultimate hull girder vertical bending moment reduction is only about 10% even when the applied horizontal bending is some 50% of the applied vertical bending in magnitude.

CONCLUDING REMARKS

The aim of the present study has been to develop some sophisticated core technologies for structural design and strength assessment of large aluminum multi-hull ships. The methodologies for ultimate limit state assessment of aluminum stiffened panels and aluminum ship hulls were developed by extensive studies undertaken theoretically, numerically and experimentally.

The developed technologies have been applied to the design and safety assessment of a 120m long aluminum catamaran fast ship, confirming that the hull is robust enough against hull girder design loads.

It is hoped that the technologies and insights developed from the present study will be useful for the design and strength assessment of large aluminum multi-hull ship structures.

Some parts of this study have shown that aluminum 5383 is superior to the standard aluminum alloy 5083 in terms of material properties, ULS characteristics and welding performance. The new aluminum stiffened panel strength models are undergoing further validation against mechanical collapse panel tests, the results of which will be published at a later time.

ACKNOWLEDGEMENTS

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REFERENCES

ALPS/HULL (2005). A computer program for the progressive collapse analysis of ship hulls (Version 2005.5). Ship and Offshore Structural Mechanics Lab., Pusan National University.

ALPS/ULSAP (2005). A computer program for ultimate limit state assessment of stiffened panels (Version 2005.5). Ship and Offshore Structural Mechanics Lab., Pusan National University.

Bruchman, D.D., Kihl, D.P. and Adamchak, J.C. (2000). Evaluation of the effect of construction tolerances on vessel strength. Ship Structure Committee Report, SSC-411, Washington, D.C.

DNV (2003). Rules for ships: high speed, light craft and naval surface craft. Det Norske Veritas, Norway.

Dow, R.S. (1991). Testing and analysis of 1/3-scale welded steel frigate model. Proceedings of the International Conference on Advances in Marine Structures, Dunfermline, Scotland, 749-773.

EN 13195-1 (2002). Aluminum and aluminum alloys: wrought and cast products for marine applications (shipbuilding, marine and offshore). European Standard: French Standard, Association Francaise de Normalisation (ANFOR).

Faltinsen, O.M., Hoff, J.R., Kvalsvold, J. and Zhao, R. (1992). Global loads on high-speed catamarans. Proceedings of the 5th Symposium on Practical Design and Ships and Other Floating Structures (PRADS'92), Newcastle, England, Vol.1, pp.360-373.

Hughes, O.F. (1998). Two first principles structural designs of a fast ferry – all-aluminum and all-composite, Proceedings of 4th International Conference on Fast Sea Transportation, Vol. I, Sydney, Australia, July, pp.91-98.

Kontoleon, M.J., Preftitsi, F.G. and Baniotopoulos, C.C. (2000). Butt-welded aluminum joints: a numerical study of the HAZ effect on the ultimate tension strength. Proceedings of the NATO Advanced Research Workshop on the Paramount Role of Joints in the Reliable Response of Structures, Ouranoupolis, Greece, May 21-23, pp.337-346.

MAESTRO (2005). Version 8.7.1. Proteus Engineering, Stevensville, MD 21666, USA.

Mazzolani, F.M. (1985). Aluminum alloy structures. Pitman Advanced Publishing Program, London.

Moan, T. (2004). Marine structures for the future – a sea of opportunities. J. of Marine Systems & Ocean Technology, Vol.1, No.1, pp.1-19.

Paik, J.K. (2004). A guide for the ultimate longitudinal strength assessment of ships. Marine Technology, Vol.41, No.3, pp.122-139.

Paik, J.K. and Duran, A. (2004). Ultimate strength of aluminum plates and stiffened panels for marine applications. Marine Technology, Vol.41, No.3, pp.108-121.

Paik, J.K. and Seo, J.K. (2005). Ultimate limit state assessment of ship structures. Proceedings of International Conference on Computational Methods in Marine Engineering (MARINE 2005), 27-29 June, Oslo, Norway.

Paik, J.K. and Thayamballi, A.K. (2003). Ultimate limit state design of steel-plated structures. John Wiley & Sons, Chichester, U.K.

Paik, J.K., Thayamballi, A.K. and Lee, J.M. (2004a). Effect of initial deflection shape on the ultimate strength behavior of welded steel plates under biaxial compressive loads. Journal of Ship Research, Vol.48, No.1, pp.45-60.

Paik, J.K., van der Veen, S., Duran, A. and Collette, M. (2004b). Considering aluminum welded panel structures for aerospace, marine and land-based applications: a comparison of ultimate compressive strength design methods. Proceedings of PRADS'2004, Germany, 12-17 September, Vol.2, pp.717-735.

Raynaud, G.M. (1995). New aluminum products for high speed light crafts. Building High-speed Aluminum Marine Vessels in Victoria – A Feasibility Study, Business Victoria, Melbourne, Australia.

Smith, C.S., Davidson, P.C., Chapman, J.C. and Dowling, P.J. (1988). Strength and stiffness of ships' plating under in-plane compression and tension. RINA Transactions, Vol.130, pp.277-296.