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# Local buckling strength of steel foam sandwich panels

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## Abstract

In this paper a design method for the compressive capacity of sandwich panels comprised of steel face sheets and foamed steel cores is derived and verified. Foamed steel, literally steel with internal voids, provides the potential to mitigate many local stability issues through increasing the effective width-to-thickness of the component for the same amount of material. Further, steel foams have exceptional energy dissipation and deformation capacity. A design methodology for the compressive capacity of steel foam sandwich panels (plates) is needed to facilitate application of such panels and in the civil engineering domain. Winter's classical effective width expression was generalized to the case of steel foam sandwich panels. The generalization requires modification of the elastic buckling expressions to account for shear deformations. Further, an equivalent yield stress is introduced to provide a single parameter description of the yielding behavior of the steel face sheets and steel foam core. The provided analytical expressions are verified with finite element simulations employing brick elements that explicitly model the steel face sheets and steel foam cores. The closed-form design expressions are employed to conduct parametric studies of steel foam sandwich panels with various face sheet and steel foamed core configurations. The studies show the significant strength improvements possible with steel foam sandwich panels when compared with plain steel sheet/plate. The design expressions and related parametric study provide insights on the optimal balance between face sheets and core. Given the success in defining optimal targets the obvious next step is assembly and testing of full-scale steel foam sandwich panels. This will complement existing efforts on material characterization of steel foam itself. This work is part of a larger effort to help develop steel foam as a material with relevance to civil engineering applications.

## **1. Introduction**

Foamed steel intentionally introduces internal voids in steel, e.g. Figure 1. A variety of manufacturing methods are used to introduce the voids from powder metallurgy and sintering of

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hollow spheres to gasification (Ashby 2000). Steel foams are largely still under development, e.g. (Kremer 2004); however steel foam sandwich panels have been utilized in a demonstration project as a parking garage slab (Hipke 2011). Mass production of aluminum foam sandwich panels (Figure 2, Banhart 2008) as well as successful aluminum foam sandwich panel applications in aerospace (Banhart 2008), automotive (Lefebvre 2008, Cardoso 2010), and manufacturing (Neugebauer 2004) demonstrate the basic potential. In general, metal foams have high effective bending stiffness and energy absorption. In addition, metal foams have improved thermal conductivity (Neugebauer 2004), enhanced fire resistance (Coquard 2010, Lu 1999), better noise attenuation (Ashby 2000, Bao 2009), and provide improved electromagnetic and radiation shielding (Losito 2010, Xu 2010) when compared with solid metals.



Figure 1: Steel foam 18% relative density: A) interior foam morphology through cut section, B) contact between spheres as shown in cross-section, C) sphere walls are not fully dense.



Figure 2: Aluminum foam sandwich panels a) on pallet, b) in section (Banhart 2008)

The overall objective of this study is to develop a design method for the determination of the inplane compressive strength of steel foam sandwich panels comprised of solid steel face sheets and foamed steel cores. The design method development requires: (a) determination of the effective bending rigidity, including shear deformations, and the resulting local buckling stress, (b) determination of the yield strength for the composite (solid and foamed steel) panel, and (c) application and verification/calibration of Winter's effective width expression (originally from Winter 1947) suitably modified by (a) and (b). Validation of the developed bending rigidity and design expressions is provided through continuum finite element solutions of steel foam sandwich panels.

#### 2. Basic steel foam material properties

#### 2.1 Uniaxial stress-strain behavior

A typical compressive stress-strain curve for the steel foam of Figure 1 is provided in Figure 3. This commercially available steel foam, manufactured by the Fraunhofer Institute in Germany, employs sintered hollow steel spheres and has a relative density  $\rho = 0.18$ . The authors are involved in a wider experimental program for complete materials characterization of this foam. For a typical sample the initial compression modulus,  $E_{fc}$  is approximately 450 MPa, the yield stress in compression  $f_{yf}$  is approximately 6 MPa, and the compressive strain before the onset of densification of the steel foam walls is nearly 100%. In tension the initial modulus and yield stress are similar but tensile strain capacity is only on the order of 2%. These properties are utilized throughout this paper as representative of an available low density steel foam.



Figure 3: Uniaxial compression test for calibration of D-F plasticity

#### 2.2 Plate bending rigidity and local plate buckling stress

The bending rigidity of a steel foam plate exceeds that of a solid plate. This is not immediately obvious when one considers that the foaming process itself decreases the apparent modulus. Consider a plate with initial thickness  $t_{ini}$ , if the entire plate is foamed, the thickness  $t_f$  is:

$$t_f = t_{ini}/\rho \tag{1}$$

where  $\rho$  is the relative density of the foamed steel ( $t_{ini}=1$  is a solid steel plate). Based on the work of (Ashby 2000) the foamed steel modulus,  $E_f$  is related to the solid steel modulus,  $E_s$ , by:

$$E_f \propto E_s \rho^2 \tag{2}$$

Substituting these relations into the standard expression for plate bending rigidity (and assuming no change in Poisson's ratio,  $\nu$ , for the foamed steel):

$$D_f = \frac{E_f t_f^3}{12(1-v^2)} \propto \frac{(E\rho^2)(t_{ini}/\rho)^3}{12(1-v^2)} \propto \frac{1}{\rho} \frac{E t_{ini}^3}{12(1-v^2)} \propto \frac{1}{\rho} D_{solid}$$
(3)

Thus, by virtue of the strong role that thickness plays in plate bending rigidity, a foamed steel plate has a higher plate bending rigidity than a solid plate.

If instead of foaming the entire plate, only a central fraction of the core,  $\alpha$  ( $0 \le \alpha \le 1$ ) is foamed, thus creating an all steel sandwich panel, the increase in plate bending rigidity can be even more pronounced. Assuming now the relative density,  $\rho$ , applies only to the foamed core, then the core thickness,  $t_c$ , increased from the initial solid plate thickness  $t_{ini}$ , is:

$$t_c = \frac{\alpha t_{ini}}{\rho} \tag{4}$$

The remaining portion of the initial solid sheet is split evenly between two face sheets of thickness,  $t_s$ :

$$t_s = \frac{1-\alpha}{2} t_{ini} \tag{5}$$

The plate bending rigidity, again assuming constant  $\nu$ , is:

$$D_p = \frac{E_s(t_c + 2t_s)^3 - (E_s - E_c)t_c^3}{12(1 - v^2)}$$
(6)

which after substitution of Equations 1, 4 and 5 results in:

$$D_p = \frac{1}{\rho^3} \left[ \left( (1 - \alpha)\rho + \alpha \right)^3 + \alpha^3 (\rho^2 - 1) \right] \frac{E t_{ini}^3}{12(1 - v^2)}$$
(7)

Graphical representation of Equation 7 (Figure 3) shows that foaming (i.e.,  $\alpha$ ) between 30-90% of the initial solid sheet ( $t_{ini}$ ) results not only in improved bending rigidity above the solid plate, but improved bending rigidity above foaming the entire plate ( $\alpha$ =1, i.e. 1.0 foamed plate). Thus, foamed steel sandwich panels have the potential for greatly improved stiffness and local buckling stress under in-plane load.



foam relative density of the core, ρ Figure 4: Increase in bending rigidity with panel foaming

### 3. Local buckling of foamed steel sandwich panels

For the foamed steel sandwich panel introduced in the previous section the in-plane elastic local plate buckling stress,  $f_{cr}$ , is proportional to the plate bending rigidity:

$$f_{cr} = k \; \frac{\pi^2 D_p}{b^2 (t_c + 2t_s)} \tag{8}$$

where k is the plate buckling coefficient, b is the plate width, and all other variables are previously defined. Thus, the improved plate bending rigidity (Equation 7) also provides plates with higher in-plane elastic local buckling stress.

However, if  $f_{cr}$  of Equation 8, utilizing Equation 7 for the plate bending rigidity is employed the predicted local buckling stress is often higher than the actual local buckling stress due to shear deformations in the low density core and lack of composite action between the core and face

sheets resulting in local bending of the face sheets in isolation. This problem has seen significant study in the literature (Allen 1969, Plantema 1966, Vinson 1999, Hohe 2004 and Kardomates 2010). In particular, Kardomates (2010) found that Allen's (1969) solution was in best agreement with rigorous continuum mechanics solutions. Thus, Allen's approach has been adopted for further study here.

The approach of Allen, for incorporation of shear and face sheet bending, is to (a) simplify the bending rigidity, and (b) smear the rest of the effects into the plate buckling coefficient, k. The plate bending rigidity,  $D_p$ , is reduced (and simplified) by ignoring the stiffness of the core, i.e.  $E_c$  of Equation 6 is set to zero, resulting in:

$$D_p = \frac{E_f t_s (t_c + t_s)^2}{2(1 - \nu_f^2)} \tag{9}$$

For low density foam cores (e.g.,  $\rho = 18\%$  for the foam of Figure 1) and utilizing Equation 2 it is found the contribution of the foamed core to the plate bending rigidity is less than 1%. Thus, the simpler expression of Equation 9 is justified even without considering shear deformations.

For a simply supported plate of length a, width b, uniformly compressed on the sides with width b, the plate buckling coefficient, k, of Allen, including shear deformation is as follows:

$$k = \left(\frac{mb}{a} + \frac{n^2a}{mb}\right)^2 \left\{ \frac{1}{1 + r\left(\frac{m^2b^2}{a^2} + n^2\right)} + \frac{t_s^2}{3(t_c + t_s)^2} \right\}$$
(10)

where the first term in the parentheses is the classic isotropic plate solution (and converges to k=4 as  $a/b \rightarrow \infty$ ), *n* is the number of transverse buckling half-waves, *m* is the number of longitudinal (in the direction of loading) buckling half-waves, and *r* accounts for shear deformation as given by:

$$r = \frac{\pi^2}{b^2} \frac{D_p}{G_c(t_c + t_s)^2/t_c} = \frac{\pi^2}{2(1 - v_c^2)} \frac{E_s}{G_c} \frac{t_s t_c}{b^2}$$
(11)

Note, if the core is isotropic unfoamed steel *r* depends on *v* and the ratio of  $t_s t_{o'}/b^2$ , and for typical b/t, *r* is less than 0.1. If the core is completely rigid in shear *r*=0. Note, even for *r*=0 Equation 10 still predicts a reduction in the plate buckling coefficient (note the last term) as Allen's method accounts for both face sheet bending and overall shear deformations.

As illustrated in Figure 5, in classic isotropic theory the minimum k occur at a/b = integer and converge to 4 as  $a/b \rightarrow \infty$ . However, for k of Equation 10 the minima no longer occur at integer values and instead occur at  $a/b = \chi_{min}$  where  $\chi_{min}$  is a function of r and  $t_s/(t_c + t_s)$ . Allen proposed that iteration be used, i.e. for a given a/b iterate on m and n until the minimal k is determined.

To expedite the use of Allen's solution a closed-form solution to the a/b at which k is a minimum is derived. First, noting n=1 always generates a minima, Equation 10 is simplified and re-written in a form more amenable to analytical manipulation:

$$k = \left(s + 2 + \frac{1}{s}\right) \left\{ \frac{1}{1 + r(s+1)} + \frac{1}{3}\xi^2 \right\}$$
  

$$s = \left(\frac{mb}{a}\right)^2, \qquad \xi = \frac{t_s}{t_c + t_s}$$
(12)



Figure 5: Plate buckling coefficient, *k*, as a function of aspect ratio (a/b) comparing classical local buckling (Kirchoff) theory with the solution of Allen for r = 0.3 and  $t_s/(t_c + t_s) = 0.1$ 

Differentiation with respect to *s* and setting to 0 to find the minima provides:

$$\left(\frac{1}{s^2} - 1\right) \left\{ \frac{1}{1 + r(s+1)} + \frac{1}{3}\xi^2 \right\} + \frac{\left(s + 2 + \frac{1}{s}\right)r}{(r(s+1)+1)^2} = 0$$
(13)

Which has four solutions, however only one of the solutions is positive, thus:

$$s_{min} = \frac{\frac{4}{9r} + \frac{1}{9r^2} + \frac{1}{\xi^2 r} - \frac{1}{\xi^2 r^2} + \frac{4}{9}}{\beta} + \beta - \frac{\xi^2 r^2 + 2\xi^2 r}{3\xi^2 r^2}$$
(14)

The auxiliary variables employed to simplify the expression for  $s_{min}$  are:

$$\beta = \left(\sqrt{\alpha_1} + \alpha_2\right)^{\frac{1}{3}} \tag{15}$$

$$=\frac{8/9}{\xi^2 r^2} + \frac{20/9}{\xi^2 r^3} + \frac{2}{\xi^2 r^4} - \frac{1/3}{\xi^4 r^2} + \frac{7/9}{\xi^2 r^5} + \frac{10/3}{\xi^4 r^3} + \frac{1/9}{\xi^2 r^6} + \frac{4}{\xi^4 r^4} + \frac{4/3}{\xi^4 r^5} - \frac{1}{\xi^6 r^3} + \frac{2/3}{\xi^4 r^6} + \frac{1}{\xi^6 r^6} + \frac{3}{\xi^6 r^4} - \frac{3}{\xi^6 r^5} + \frac{1}{\xi^6 r^6}$$
(16)

$$\alpha_2 = \frac{4}{9r} + \frac{2}{9r^2} + \frac{1}{27r^3} + \frac{1}{\xi^2 r} + \frac{1}{\xi^2 r^2} + \frac{1}{\xi^2 + r^3} + \frac{4}{27}$$
(17)

The minimum number of half-waves,  $m_{min}$ , for a given panel aspect ratio a/b is:

$$m_{min} = \sqrt{s_{min}} \left(\frac{a}{b}\right) \tag{18}$$



Figure 6: Graphical representation of closed-form solution for  $\chi_{min}$ 

Alternatively the aspect ratio at which a given number of half-waves, *m*, reaches a minimum is:

$$\left(\frac{a}{b}\right)_{min} = \chi_{min} \ m = \sqrt{1/s_{min}} \ m \tag{19}$$

 $\chi_{min} = \sqrt{1/s_{min}}$  can also be estimated from Figure 6 for known *r* and  $\xi$ .

The overall potential impact of shear deformation and non-composite face sheet bending on the local buckling solution is illustrated in Figure 7. As shear deformations increase, i.e. as r increases, the plate buckling coefficient decreases. The local plate bending (captured in the ratio of the face sheet thickness to the sum of face sheet and core thickness,  $t_s/(t_c+t_s)$ ) also influences the solution, but to a far lesser extent. Note, as  $a/b \rightarrow 0$  the inclusion of shear deformation, r, in Equation 10 causes k to converge to a finite value instead of infinity, as in the case of an isotropic plate.



Figure 7: Plate buckling coefficient, *k*, as a function of plate aspect ratio (a/b) demonstrating the impact of shear deformation (*r*) and face sheeting bending  $t_s/(t_c+t_s)$  on the solution

### 4. Computational modeling of steel foam sandwich panels

To further explore the predicted behavior for steel foam sandwich panels and provide predictions of the ultimate strength of in-plane loaded steel foam sandwich panels a series of finite element models was constructed. The models were completed in LS-DYNA (Hallquist 2006). Brick elements (500,000 to 1,000,000 type 164 solids, Hallquist 2006) were used throughout: 150 to

200 transverse elements, and six elements through the thickness, as shown in Figure 8a were typical, but element aspect ratios were maintained from 1 for b/t = 50, up to 2 for b/t = 200. Thin steel plates (0.3 mm) along the panel perimeter were employed to eliminate the sharp load application to the continuum representation (Figure 8a). The steel face sheets were modeled with a standard J-2 plasticity formulation and isotropic hardening. The steel properties: E = 203000 MPa,  $f_y = 385$  MPa, and complete strain hardening regime were obtained from coupon tests (Vieira 2011) of steel sheet. In addition, tensile failure in the face sheet was simulated via element deletion at an accumulated plastic strain of 18%.

Modeling the steel foam core requires a more sophisticated approach than standard J-2 plasticity. Steel foam is still compressible after its yield and in the plastic regime  $\nu$  is typically less than 0.3, as opposed to solid steel, which is practically incompressible and thus  $\nu = 0.5$ . For steel foam, the yield and subsequent plastic surface evolution depend not only on deviatoric stress invariant  $J_2$  but also on the trace of the stress tensor  $I_1$ . Miller et al. (2000), and later Deshpande and Fleck (D-F, 2000) introduced a generalized von Mises-Huber plasticity, which accounts for pressure dependence. Reyes (2003) and Hansen et al. (2002) enhanced D-F plasticity with tensile fracture criteria based on the major principal stress and D-F plasticity with the fracture criteria is implemented in LS-DYNA (Hallquist 2006). The D-F formulation must be calibrated against a uniaxial material test, and the low density hollow sphere foam of Figure 1 as tested and reported in Figure 3 is used for that purpose here.





(b) typical buckling mode for a shear deformable core (r=1.45), inset highlights shear deformation (mm)

Figure 8: Finite element model of a simply supported steel foam sandwich panel (steel face sheet and steel foam core are modeled with brick elements in LS-DYNA) under in-plane compression

Eigenbuckling analysis, Figure 8b, was performed on the developed finite element model to explore the accuracy of Allen's elastic buckling solution (Equations 8-10). For the eigenbuckling models, based on a  $t_{ini} = 1 mm$ , 30% of the solid sheet was foamed to 18% relative density (i.e. the foam of Figure 1) resulting in  $t_s = 0.35 mm$  and  $t_c = 1.67 mm$ . Panel width b was varied from 50 to 200 to explore a wide range of b/t ratios. Figure 9 shows that Allen's elastic buckling solution works well for steel foam sandwich panels over a large variation in b/t ratios (and shear deformation ratio, r).



Figure 9: Comparison of Allen's elastic buckling solution with numerical plate buckling model (dashed lines provide a means to understand the impact of shear deformation on solution)

#### 5. Strength of in-plane loaded sandwich panels

Prediction of the compressive strength of a steel foam sandwich panel loaded in-plane is the ultimate goal of the work presented herein. In this section Winter's effective width approximation is modified for steel foam sandwich panels and then compared against nonlinear collapse simulations in LS-DYNA.

### 5.1 Squash load and equivalent yield stress

The squash load is the compressive load at which the section is fully yielded. In the case of steel foam sandwich panels this is modified to the compressive load at which the steel face sheets are fully yielded. The equivalent yield stress for the sandwich panel,  $f_{yp}$ , may then be found from simple force balance:

$$f_{yp} = \frac{2 t_s f_{ys} + t_c \cdot min\left(f_{yc}, E_c \frac{f_{ys}}{E_s}\right)}{2t_s + t_c}$$
(20)

where the yield stress of the face sheets,  $f_y$ , is explicitly denoted here as  $f_{ys}$ , and the yield stress and modulus in the foamed core are denoted as  $f_{yc}$  and  $E_c$ . Typically, the core is still elastic when the face sheets yield, thus the second term of the minimum in Equation 20 usually controls. Alternatively  $f_{yp}$  may be expressed explicitly in terms of the foaming parameters  $\alpha$  and  $\rho$ :

$$f_{yp} = \frac{(1-\alpha)f_{ys} + \alpha\frac{1}{\rho} \cdot min\left(f_{yc}, E_c\frac{f_{yf}}{E_s}\right)}{(1-\alpha) + \frac{\alpha}{\rho}}$$
(21)

Also note, per Ashby (2000):  $f_{yc} \propto f_{ys} \rho^{1.5}$ , and this approximation combined with Equation 2:  $E_c \propto E_s \rho^2$  may be used to provide an approximate expression for  $f_{yp}$  that is only dependent on the foaming parameters.

#### 5.2 Winter's design method

For thin solid steel plates the most widely accepted engineering approach to predicting their inplane compressive strength is Winter's effective width approach 0 or some variant thereof. Winter's approach (see Ziemian 2010 for a full summary) is predicated on the early test observations of (Schuman 1930) and the semi-empirical derivation of von Karman (1932). Winter conducted his own tests in (Winter 1947) which lead to empirical corrections to von Karman's work to account for imperfections. Ultimately, modern specifications (AISI 2007) have led to further small modifications. As implemented (AISI 2007), Winter's approach provides the reduced width of the plate,  $b_e$ , that is effective in carrying the maximum stress,  $f_y$ :

$$b_{e} = \begin{cases} b & \text{if } f_{cr} \ge 2.2 f_{y} \\ b \left( 1 - 0.22 \sqrt{\frac{f_{cr}}{f_{y}}} \right) \sqrt{\frac{f_{cr}}{f_{y}}} & \text{if } f_{cr} < 2.2 f_{y} \end{cases}$$

$$(22)$$

where b is the plate width,  $f_{cr}$  is the local plate buckling stress, and  $f_y$  is the plate material yield stress. The method results in a predicted compressive strength,  $P_n$ , for the plate of

$$P_n = b_e t f_v \tag{23}$$

Here we explore the generalization of this design approach where  $f_y$  is replaced with  $f_{yp}$  of Equation 21 and  $f_{cr}$  includes Allen's reductions for shear deformation and face sheet bending: Equations 8, 9 and 10 as well as utilize the closed-form expression of Equation 18 to determine the number of buckling half-waves that provides a minimum  $f_{cr}$  for a given plate.

#### 5.3 Sandwich panel collapse simulations and comparisons

The LS-DYNA brick element model, employing J-2 plasticity for the face sheets and the triaxial stress dependent D-F model for the foamed steel core as described in Section 4, is employed here to conduct material and geometric nonlinear collapse analysis of simply supported steel foam sandwich panels loaded under in-plane compression. Geometric imperfections in the shape of the first eigenmode with magnitudes of 0.1t and 0.34t (see Schafer 1998) where t is the total thickness, were employed. As in the eigenbuckling analysis of Section 4:  $t_{ini} = 1 \text{ mm}$ ,  $\alpha = 30\%$ ,  $\rho = 18\%$  (i.e. the foam of Figure 1) which results in  $t_s = 0.35 \text{ mm}$  and  $t_c = 1.67 \text{ mm}$ . Panel width *b* was varied from 50 to 200.

The force at collapse in the models (normalized by the solid sheet squash load  $P_y=bt_{ini}f_{ys}$ ) is provided as a function of the panel width-to-thickness ratio in Figure 10. The figure also provides the strength prediction based on Winter's method, Equation 23. Three curves are provided for Winter's method: solid steel (unfoamed) sheet; sandwich panel - ignoring shear effects, and; sandwich panel - including shear effects. The results indicate that shear effects must be included in the solution, but if they are included (and the yield stress suitably modified to  $f_{yp}$ ) Winter's method provides an accurate prediction of strength. Further, even granting the small loss in capacity due to shear deformations, the foamed panel outperforms the solid steel sheet for a large range of b/t ratios.



Figure 10: Comparison of finite element collapse simulations of steel foam sandwich panels with predicted strength based on modified version of Winter's method.

The collapse simulations also provide further insight into how the sandwich panel carries load. Consider the  $b/t_{ini}=50$  model at peak strength; the longitudinal stress contours are provided in Figure 11. The variation in stress along the length, in the face sheets, increases and decreases (though in net compression) as it follows the buckling waves. The stress at the center, in the foamed steel, is essentially zero. This is in stark contrast to a solid steel sheet, which has high net compression in the center. This can all be observed in greater detail for a transverse cut of the longitudinal stress: consider the section called out in Figure 11 and provided in Figure 12b. If the longitudinal stress at the same section is integrated through the thickness, then divided by the total thickness ( $t_c+2t_s$ ) to provide an equivalent stress, the result is Figure 12a. The distribution of Figure 12a is readily recognized as similar to the classic stress distribution that motivated the effective width expressions of von Karman and later Winter. Interestingly, as shown in the figure, the maximum stress at failure is approximately  $f_{yp}$  (i.e. 117 MPa).



Figure 11: In-plane stress distribution in a panel: A. top face (steel plate), B. mid-plane (foam plate), C. top face (steel plate), D. cross-section (top steel steel-foam-bottom steel face)



Figure 12: Resistance mechanism of sandwich panels: A. integral through the thickness (effective compressive resistance) expressed in terms of the equivalent smeared stress, B. stress distribution in: convex steel face, concave steel face and foam mid-plane.

## 6. Steel foam sandwich panel optimization

To illustrate the performance that is possible with steel foam sandwich panels the strength predicted by the suitably modified and validated Winter's method (Equation 23) is compared to a solid plate (thickness= $t_{ini}$ ) of the same weight for a variety of different foamed depths. The commercially available steel foam of Figure 1 ( $\rho$ =18%) is again used for the core density, and the depth of foaming,  $\alpha$ , is varied from 0.1 to 0.6 (i.e. the initial portion of the plate that is foamed varies from 0.1 $t_{ini}$  to 0.6 $t_{ini}$ ). The plate width is varied and the resulting strength prediction is provided in Figure 13.

Fundamentally, foaming decreases  $f_y$  (to  $f_{yp}$  via Equation 21) and increases the local buckling stress  $f_{cr}$  (through an enhanced plate rigidity appropriately reduced for shear deformations and face bending Equations 8,9,10). Thus, as shown in Figure 13a for stocky plates (low  $b/t_{ini}$ ) the sandwich panel has a reduced capacity when compared to a solid plate of the same weight, but as slenderness increases the sandwich panel capacity exceeds that of the solid plate. In striking the balance between reduced  $f_y$  and enhanced  $f_{cr}$  it is shown that a foamed depth of  $0.3t_{ini}$  ( $\alpha$ =0.3) provides the biggest improvements over the solid plate, over the widest range of  $b/t_{ini}$ , Figure 13b. In the studied case strength gains above the solid plate between 150% and 200% are realized for  $b/t_{in}$ >100.

## 6. Discussion

This work provides a basic building block in the development of steel foams for structural engineering. Strength predictions of steel in in-plane compression, and appropriate reductions for local buckling, are fundamental to the creation of thin-walled members comprised of steel foam. It is somewhat remarkable that Winter's equation once again can be utilized to predict capacity. It is worth noting that the final form of Winter's expression and its modifications should be based on tests, not just the simulations provided here; however, the work here provides confidence that the basic approach can be realized, though additional calibration will no doubt be required.

This study elucidates the potential stiffness and strength gains of steel foam sandwich panels, but does not explore energy absorption and ductility. Even for the cases where the squash load is reduced (i.e. the "Gain" in Figure 14b is < 1.0) the compressive deformation capacity in these sandwich panels will be greatly increased. Design procedures for prediction of the deformation capacity (and thus ductility and energy dissipation) are a logical next step for this work. Significant effort remains at all levels to develop steel foam as a structural material; nonetheless, work such as that provided herein is intended to aid and encourage that development.



Figure 13: Strength of solid steel and sandwich panels of the same weight,  $\rho=18\%$  in the foam cores and depth of foaming varied, (a) strength normalized to yield as a function of initial plate width-to-thickness, (b) strength normalized to solid plate strength as a function of initial plate width-to-thickness

## 8. Conclusions

Steel foam is emerging as a new structural material with intriguing properties: high stiffness-toweight ratio, high energy absorption, and other advantages. Foaming steel increases bending rigidity, but decreases the effective modulus and yield stress. A steel foam sandwich panel, consisting of solid steel faces and an interior of foamed steel further increases the bending rigidity, and limits the loss in effective modulus and yield stress. However, depending on the density of the foamed steel core, shear deformations and non-composite bending of the face sheets, must be accounted for in the behavior of steel foam sandwich panels. It is found that the approximation of Allen (1969) effectively captures these phenomena in the prediction of the elastic local buckling stress for a steel foam sandwich panel. This observation is verified, by detailed continuum finite element models of a steel foam sandwich panel with brick elements. Allen's elastic local buckling prediction is extended and a closed-formed solution provided. The ultimate strength of steel foam sandwich panels is explored with the detailed finite element model and it is found that Winter's classic effective width method suitably modified for the effective yield stress (derivations provided herein) and local buckling stress (based on Allen's method) is an excellent predictor of steel foam sandwich panels over a wide slenderness range. Further, exploration of the developed expressions utilizing one commercially available steel foam demonstrates that foaming the middle 30% of a solid steel plate leads to optimal strength gains, which can be in excess of 200% of the strength of the solid steel sheet of the same mass. Significant work and experimental validation remain, but the work presented herein shows that a basic buckling block of thin-walled member design: Winter's effective width method, can be suitably modified for steel foam sandwich panels.

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