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Method for Quantifying Floating Marsh Strength and Interaction with Hydrodynamics

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METHOD FOR QUANTIFYING FLOATING MARSH STRENGTH AND INTERACTION WITH HYDRODYNAMICS

A Thesis

Submitted to the Graduate Faculty of the
Louisiana State University and
Agricultural and Mechanical College
in partial fulfillment of the
requirements for the degree of
Master of Science in Coastal and Ecological Engineering

in

The Department of Civil and Environmental Engineering

by

Jason Haydel Collins III
B.S., Louisiana State University, 2014
August 2017
This thesis is dedicated to my family. In particular my late grandmother, Malane and grandfather, Walter who passed during the final months leading up to completion of this degree.
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I would like to thank Dr. Willson, Dr. Twilley, and Dr. Sasser for their continuous support and excellent advise throughout this process. I would also like to thank Louisiana State University for allowing me the opportunity to receive an excellent education close to home. The unconditional backing from my friends and family was an invaluable asset in maintaining my confidence through the end. Lastly, portions of this research were conducted with high performance computing (HPC) resources provided by LSU. It would not have been possible to complete this research in a timely manner without this capability provided by the university.
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Abstract

Louisiana possesses over 350,000 acres of unique floating vegetated systems known as floating marshes or flotants. Due to their buoyant nature, floating marshes are susceptible to high energy changes in the hydrodynamic environment that may result from proposed river diversion projects which introduce flow to areas that are typically somewhat isolated. The overall goal of this research is to improve the understanding of how exposed flotants deteriorate under increased hydrodynamic stresses. More specifically, this thesis aims to answer how the material limits of floating marshes can be measured and how the mats interact with hydrodynamics. The two primary objectives are: 1) Develop a technique for accurate, in-situ measurement vegetative mat root-soil matrix material properties; and 2) Develop a means for predicting floating marsh washout (critical velocities) through numerically modeled derived empirical relationships.

The device constructed to capture the tensile properties of the vegetative mats, called the Marsh Mat Tensile Strength Tester (MMTST), successfully produced full stress-strain profiles including the Young’s modulus, yield stress, and ultimate strength of a root-soil matrix (sod). The estimated mean Young’s modulus, yield stress, and ultimate strength values (sod) were found to be 31.95 kPa, 9.58 kPa, and 9.91 kPa, respectively. Next, flows around 25 idealized mat aspect ratios were simulated with 2-D & 3-D Fluent models. Mat-specific drag coefficients (C_{d,m}) were found ranging from 1.084-1.645 depending on mat aspect ratio. An equation developed for predicting C_{d,m} successfully estimated the modeled drag coefficients with a mean percentage error of 2.33%.

A finite element analysis (FEA) was performed on the 25 mat shapes using the predicted drag forces and the material properties measured by the MMTST. By applying various failure criteria (F_c), a correlation was found between the modified mat width-to-length aspect ratio (β) and critical velocity (V_c). The critical velocities ranged from 0.31-1.48 m/s depending on mat aspect ratio and
material properties. The general equation developed for predicting floating marsh failure due to flow, in the form: $V_c = f(\beta, F_c)$, performed well with a mean percentage error of 3.33% relative to the unique values directly extracted from the FEA.
1. Introduction

1.1 Background

South Louisiana’s delta is a result of the Mississippi River (M.R.) meandering from east to west between six distinct deltaic lobes (Figure 1). This process took place over the course of numerous millennia, creating diverse wetlands and ecosystems by depositing sediment and nutrients (Day et al., 2014). The resource rich, nascent deltaic lobes that formed early on became heavily vegetated wetlands, nurtured by the riverine input. As deltas grow over time, they become more hydraulically resistive and eventually force river avulsions further upstream.

![Figure 1. Historic lobes of the M.R (Mac et al., 1998; Modified from Kolb and Lopik, 1958.).](image)

When the river changes course, sediment load into the wetlands is reduced drastically and organic peat begins to accrue in the substrate (Sasser et al., 2010). These river-abandoned systems often contain large expanses of vegetative buoyant peat mats called floating marshes or flotants. Floating marshes are unique wetlands that consist of a low-density organic base connected by a
tightly woven root system from which vascular vegetation and, sometimes, wooded plants can grow. Because flotants possess a buoyant nature, they are immune from most hydrostatic stresses by rising and falling with ambient water level (Sasser et al., 1994). The exact process by which floating mats develop is not completely understood. R.J. Russell (1942) suggested that the mats formed from coalescing floating aquatic plants in still lakes and back waters, while O’Neil (1949) later proposed that subsidence caused the more buoyant organic peat layer to separate from the submerged substrate, driving the mat to the surface (Russell 1942, O’Neil 1949, Sasser et al., 1994.).

The deterioration of floating marshes in south Louisiana is drastic and a result of a variety of stressors including: salt water intrusion, sediment deposition, nutria grazing, and flood events. Though some of these interactions have been well studied, there is room for research that quantitatively investigates the response of floating marshes to flows caused by higher flow events. By measuring strength properties of the mats and the hydrodynamic stresses imposed on them, a determination of washout probability can be made. With the use of 2-D & 3-D computer models, the fate of floating marshes under different hydrodynamic flow conditions can be simulated, leading to better preventative strategies or mitigation solutions or mitigating design solutions for floating marsh protection and restoration.

1.2 Research Objectives

The overarching goal of this thesis is to establish a baseline approach for quantifying how exposed floating marshes respond to increased hydraulic loads. In particular, this research aims to answer how the tensile material strength properties of the floating mats can be measured, as well as, determine the potential fluid forces that may be experienced by the exposed mats resulting in washout of the marsh. Ultimately, general relationships will be developed to assist in 1) the
prediction of stream-wise drag forces on various mat shapes & 2) the estimation of flow conditions required for mat failure in the form of critical velocities. These formulas seek to simplify the determination of marsh washout by relating the mat aspect ratio (A.R.) and material properties to a mat-specific critical velocity \( V_c \).

To accomplish these objectives, specific tasks will be performed:

1) Design a tensile strength measuring method and device that can capture the full tensile stress-strain profile of a bulk vegetative root-soil matrix where the values of interest are Young’s modulus, yield stress, and ultimate strength.

2) Determine the validity of the tensile strength measuring method and device through a laboratory test and comparison with published data. Sod grass will be used a proxy material.


4) Develop a numerically empirical relationship between the drag coefficients, flow conditions (velocity), and mat aspect ratio in the form: \( C_{d,m} = f(Re_m, L_m) \). Where \( Re_m \) is the mat Reynold’s and \( L_m \) is the stream-wise mat length.

5) Use the calculated drag coefficients to predict the stream-wise forces on the mats that will then be applied as boundary conditions in static FEA of the mats.

6) Perform FEA of the 25 mat aspect ratios, using ANSYS Static Structural, to determine the loads required for failure. The material properties found in Step 1 and the loads from Step 5 will be used as model inputs.

7) Apply various failure criteria (\( F_c \)) to determine mat-specific critical velocities.
8) Develop a general relationship between mat width \((W_m)\) to mat length aspect ratio, failure criterion, and velocity that can be used for estimating floating marsh mat failure in the form: \(V_c = f(W_m, L_m, F_c)\).

1.3 Thesis Organization

This thesis will first summarize the existing literature on floating marshes. After which, the method for quantifying mat tensile strength will be presented along with the relevant results. Next, the ANSYS software modelling background, underlying theory, validation, and setup are explained for both the computational fluid dynamics (Fluent) and Static finite element analysis (ANSYS Static Structural). The results are presented in chronological order corresponding with the steps mentioned in the previous section. Here, the Fluent extracted drag coefficients, static FEA, and general relationships are described. Lastly, a thorough discussion of results and recommendations for future additions are included.
2. Literature Review

2.1 General Description

Floating fibrous peat mats that are capable of supporting vegetation occur in many places around the world, usually nearby riverine freshwater systems. The floating meadows of the Amazon River basin, the *Cyperus Papyrus* floating mats near the Nile, and the floating plavs of in Eastern Europe are just a few documented cases globally (Sasser et al., 1994). In particular, The *Cyperus papyrus* sudd in Lake Naivasha is an important archetype exhibiting a very robust well-formed mat that resists the shearing force of the Malewa River (Figure 2).

![Diagram of floating Cyperus papyrus mat in Lake Naivasha](image)

Figure 2. Floating *Cyperus papyrus* mat in Lake Naivasha. The Malewa River (solid arrow) travels underneath the mat (dotted arrow) (J.J. Gaudet 1979, Petr 2000, Google Satellite Imagery 2013).
Geographical surveys show that floating marshes cover an estimated 130,000 hectares of Louisiana’s freshwater wetlands, roughly 60-70% of total freshwater wetland acreage (Sasser et al. 1994, Evers et al. 1996). Many of these flotant systems exist on private property, as well as public lands. Figure 3 illustrates the approximate locations of various types of floating marshes in South Louisiana (Evers et al. 1996).

Two drastically different forms of floating marshes exist, thick mat (50 cm thick) and thin mat (<25 cm thick). Thick and thin mat flotants are dominated by different species that highly influence their structural integrity and ability to accumulate biomass (Sasser et al. 1995a, 1996). Thick mat floating marshes possess very robust peat mats with a securely interwoven system of roots and have enough strength to support the weight of a human. During the mid-20th century in Terrebonne
parish, thick mat plant species dominated the wetlands, covering 67% of the fresh and oligohaline marshes. In 1992, thick mat floating marsh species made up only 19% of fresh and brackish marshes while thin mat floating marsh species rose from covering 3% in 1968 to 53% in 1992 (Visser et al. 1998). Figure 4 depicts this drastic reduction in thick mat percentage of area.

**Terrebonne Basin**

<table>
<thead>
<tr>
<th>Year</th>
<th>Thick Mat</th>
<th>Thin Mat</th>
</tr>
</thead>
<tbody>
<tr>
<td>1968</td>
<td>67%</td>
<td>3%</td>
</tr>
<tr>
<td>1992</td>
<td>53%</td>
<td>19%</td>
</tr>
</tbody>
</table>

Figure 4. Percentage of total freshwater and oligohaline wetland coverage (Visser et al. 1998).

*Panicum hemitomon* (Figure 5) is the dominant species that makes up most thick mat floating marshes and it is able to maintain mats between 30 and 60 cm thick (Figure 6) with a dry bulk density between 0.05 – 0.11 $\frac{g}{cm^3}$.

Figure 5. Photos of species that dominate floating marsh habitats. Right: *Panicum hemitomon* (Maidencane). Left: *Eleocharis baldwinii* (Spike Rush) (Lovell, 2009a; Lovell, 2009b)
Panicum thick mats float continuously throughout the year and make up roughly 27,000 ha of the Terrebonne and Barataria basin fresh and brackish wetlands (Sasser et al., 1994). Thin mat floating marshes have a much shallower peat mat with a fragile root layer that is not well bound to the mat substrate and can only support the weight of a human during certain times of the year (Sasser et al. 1995a, 1996). Eleocharis baldwinii (Figure 5b) dominates thin mat marshes, which make up nearly 30,000 ha of the Terrebonne and Barataria basin wetlands. Thin mats can only maintain thicknesses of 25 cm max (Figure 6) and have a dry bulk density between 0.03 – 0.05 g/cm³ (Sasser et al., 1994).

Figure 6. Depth profiles of floating marsh types (Izdepski 2007, modified from Sasser 1994).

The deterioration of thick mat flotants to the weaker thin mat flotants may result in wetland losses during high-energy events, leading to the conversion of marshlands to open water. Understanding the structural integrity of the floating marsh mat is imperative to ensuring their preservation. The wetlands just south of Morgan City, Louisiana and to the east of the Atchafalaya River contain a large portion of the floating marshes in the west Terrebonne basin (Figure 3) (Sasser et al. 1994). This region is home to thin mat floating marshes that may be susceptible to
changes in the hydrological environment. Diversion projects are planned in this area to increase
the flow of freshwater, sediment, and nutrients into regions that have become hydrologically
isolated through anthropogenic and natural processes. Though diversion projects have many
benefits for wetlands that need fresh water, sediment and nutrients, there may be unintended
consequences. Because floating marshes rest on the water surface, they are susceptible to an
increase in flow velocities. It has been documented that during high-energy events such as
hurricane storm surge, flotants will be compressed, folded, torn, and displaced to other areas in the
region, or washed out completely (Chabreck and Palmisano 1973; Guntenspergen 1995; Cahoon
2006; Morton and Barras 2011.). Satellite imagery of a flood event through a floating marsh is
shown in Figure 7. This particular area is bordered by an intercostal waterway to the north with
connectivity to the nearby Atchafalaya River.

Figure 7. Stable Condition (left) and during floating marsh washout event (right). 29°35'16.12"N

To better evaluate the conditions required for floating marsh washout, the material properties
of the mats must be quantified along with how the flotant is anchored to any fixed location, if at
all. After enough information is gained through this research, design criterion for floating marsh
protection measures can be created.
2.2 Floating Marsh Environmental Stresses

Despite floating marshes possessing an evolutionary immunity from hydrostatic stressors due to their buoyant nature, many other challenges present themselves (Holm et. al. 2000). Almost all thick mat floating marshes exist in backwater wetlands with little to no salinity. Though Panicum hemitomon can tolerate salinities of up to 9ppt, it loses competitive advantage to other species in more oligohaline systems. Sagittaria lancifolia and Spartina patens begin to dominate as salinity increases in the wetland. As the influence from the Panicum hemitomon is reduced, the physical strength of the mat will decrease over time (Sasser et al., 1994). The root structures of the other species will not be able to maintain the same mat structural integrity with their below ground biomass, causing deterioration of the flotant. Figure 8 shows many potential transformations of floating marsh types due to various environmental stresses (Sasser et al., 1994).

Figure 8. Potential relationships between flotant types with imposed stresses (Sasser et al., 1994).
Based on these observations, a trend can be seen from free floating thick mat marsh to the more damped floating *Sagittaria lancifolia* dominated marsh with increasing salinity. It is possible that as salt water intrudes from the sea, floating marshes closer to riverine influences may be able to tolerate the stress better than other wetland types. Dense saltwater forms a wedge shape near the bed, away from the fresh water at the surface where the flotant exists.

The hydrological responses of the different types of flotants consist of five distinct floating regimes. Freely floating marshes are found to have a close correlation with the rise and fall of the open ambient water level in the channels and lakes around it. There is high connectivity between the water level within the marsh and the open water; many thick mat floating marsh sites are freely floating along with some thin mat (Sasser et al. 1994, 2005). The next floating regime is called damped floating. In this case, floating mats have a close correlation to the marsh water level and open water level but fluctuate in a slightly submerged state, just under the surface. Intermediate floating marshes containing both *Panicum hemitomon* and *Sagittaria lancifolia* will exhibit damped floating regimes while possessing a thicker mat. Impounded floating marshes do not show a high correlation with the open water level, which more than likely is due to an embankment disconnecting the flow between the marsh water and open water (Sasser et al., 1994). A buoyant mat exhibits a micro-floating regime when there is mat movement within the range of 5 cm. Micro-floating marshes are likely anchored to the attached substrate in some way and could conceivably become freely floating during exposure to elevated water levels. The final type of regime is non-floating, which is the result of a fully attached marsh (Sasser et al., 1994). Figure 9 contains graphical depictions of the floating regimes of various floating marsh types. Graphs 7a, 7b, and 7c are good examples of a *Panicum hemitomon* thick mat exhibiting freely floating behavior. Graph 7d shows an example of the damped floating regime of an intermediate floating marsh type.
containing *Sagittaria lancifolia*. The mat cannot rise past 40 cm despite flooding events; this mat may be near to an attached marsh or a solid embankment. Figure 9e depicts a seasonally buoyant thin mat floating marsh that was submerged in late spring but became freely floating during the growing season (Sasser et al., 1996).

![Time series of marsh mat elevations](image.png)

Figure 9. Time series of marsh mat elevations (bold line) and water level (dotted line). (Sasser et al., 1996).

Marshes that are seasonally floating (Figure 9e) may be prone to forming an attachment to the bottom substrate, which can prevent resurfacing during the warmer months. This can result in death of the marsh. The bulk density of seasonally floating marshes may be sensitive to sedimentation from a nearby riverine mineral source. Ideally, floating marshes remain buoyant all year long, which would remove the possibility of sediment deposition on top of the mat, however, *Eleocharis baldwinii* dominated thin mat floating marshes will receive sediment deposits during submerged periods (Carpenter 2007). Tests conducted to determine the impact of sedimentation
on thin mat marshes showed that even with high sediment deposition artificially imposed by the experiment, there were no significant changes to buoyancy. The typical dry bulk density of a thin mat floating marsh is near 0.050 $\frac{g}{cm^3}$, with high sedimentation, the bulk density rose to 0.068 $\frac{g}{cm^3}$ (Carpenter et al., 2007). This increase in bulk density due to higher a higher mineral-organic content ratio did not drastically reduce the buoyancy of the mat, in fact, the biomass was found to have increased due to nutrients provided by the sediment (Carpenter et al., 2007). A significant difference in the vertical movement between the edge of a floating marsh and the interior of a floating marsh has been measured from transect studies for wetlands near rivers. The interior of a floating marsh was found to have a significantly higher percentage of organic material than on the edge. A larger range of vertical movement measured in the interior marsh correlated with a smaller mineral content within the mat (Holm et al. 2000). It is conjecture that a higher mineral content may indicate closer proximity with an attached marsh to which flotants can anchor, damping their vertical movement.

Due to the drastic mid to late 20th century reduction in healthy *Panicum hemitomon* thick mat floating marshes, thin mat flotants have expanded (Visser et al., 1999). Thick mat floating marshes are more desirable for wetland conservation due to their more robust mat structures. It was found that *Panicum hemitomon* can be transplanted into thin mat flotants without fertilizer, perhaps indicating that improper nutrient loading may not necessarily be the issue. In fact, the most crucial parameter contributing to the success of the *Panicum* thick mat species is protection from nutria grazing (Sasser 2005). Unfortunately, this is not a problem that can be solved through engineering practice.

Hydrostatic, saline, sediment, nutrient, & grazing stresses on floating marshes have been studied and measured in great detail. However, the response of floating marshes to hydrodynamic stresses
has only been observed with few quantitative assessments. Though the adaptation to float has its advantages, flotants are extremely susceptible to high-energy events like hurricane winds, storm surge, and river floods. There is thorough documentation of mats being ripped from neighboring marsh and washed away (Chabreck and Palmisano, 1973). Figure 10 and Figure 11 depict how marsh mats will fold and buckle due to the shearing effect of hurricane winds and storm surge. When the mats are pushed to their material limits, ripping and tearing occurs which then yields new expanses of open water (Figure 12A) (Cahoon, 2006; Guntenspergen et al., 1995).

Figure 10. Aerial photo showing a folded marsh mat after Hurricane Andrew (A). Folds at marsh level (B) (Guntenspergen et al., 1995).

When the marsh becomes folded during a high-energy event, there can be a strengthening of the substrate. New ridges and folds sometimes compress into a more stable, elevated marsh; this is, of course, at the expense having a new area of open water (Cahoon, 2006; Guntenspergen et al. 1995). Perhaps thicker mats are better suited to respond in this way as opposed to thin mats, which may be more likely to washout. Material analysis of floating marsh mats will need to be conducted in order to more accurately estimate their response to a hydrodynamic forcing.
Figure 11: The elevation change from surge induced marsh compression (folding) along a transect (Guntenspergen et al., 1995.).

Figure 12. Hurricane Lily marsh destruction with folding (Cahoon, 2006.).
2.3 Floating Marsh Mat Soil Strength Properties

Floating marsh mats, as stated previously, are made up of a highly organic, peaty soil substrate that is reinforced by a network of plant roots and gas filled rhizomes. The organic peat soil is considered to have very low to negligible tensile strength capacity relative to the roots that reinforce the mat (Nearing, 1991; Jan Li, 2013). Therefore, it assumed that the roots within the mat are the principal tensile load-bearing element. However, connectivity between the roots and the substrate is also important; roots can be pulled out of the soil before the fibers themselves break, resulting in failure due to slippage. Preliminary understanding of root-soil matrix strength took into account only the tensile properties of the roots and their distribution throughout the soil (Wu et al., 1979; Pollen et al., 2005). Assumptions made in these root-soil reinforcement models require that perfect root-soil connectivity exists and that all roots fail at the same time. This can lead to strength overestimations by as much as 100% (Pollen et al., 2005; De Baets., 2008). In reality, roots will fail progressively as the weaker connections break, transferring the load to the remaining intact fibers. Additionally, roots may be more prone to failure due to slippage as moisture content in the soil increases (Pollen et al., 2005, De Baets., 2008). Root pullout studies have observed a 50% reduction in root-reinforced soil strength due to high moisture content as seen in Figure 13 (Osano, 2012).

![Figure 13. Root slippage resistance vs moisture content. Panicum virgatum (red) (Osano 2012)](image)
In the case of flotant marshes, the root-soil matrix is considered to have an extremely high soil saturation due to the fact that the mat is continuously floating in water, therefore the mode of failure is not guaranteed to be root breaking. Floating marshes also have a strength gradient with depth resulting from the decrease in below ground biomass density the further away from the mat surface as seen in Figure 14 (Sasser et al., 2005). In a previous study that examined the rotational shear strength of wetland soils, the thick mat floating marsh root-substrate interface provided enough connectivity for good data measurements. However, the thin mat floating marsh root-soil connection provided such a negligible fiber pull-out resistance that shear strength values could not be registered on the instrument (Sasser et al., 2013, Sasser personal observation).

Figure 14. Floating marsh soil shear strength varying with depth (Sasser et al., 2005)
Because of the high moisture content, a complicated root-soil matrix, and the natural floating state of flotant marsh mats, an in-situ method of testing tensile strength properties is desired. Only performing sample tests in a laboratory may not fully capture the material properties that the mats possess in their natural environment. Scaling up the physical dimensions of the samples tested will theoretically result in larger forces required to induce failure, thus leading to more effective data capture by the strength testing device on thin mats.

By treating the flotant mat as a material, a stress – strain profile can be achieved with a tensile strength test. The test is conduct by applying a pulling force on an object with known dimensions. The pulling force is said to be acting in “tensile” and the plane normal to the force vector direction is the cross-sectional area. Tensile stress is calculated in the form of Equation 1. The strain of a material is the ratio of deformation to initial length, as a result of a stress. In the case of tensile stress, the material will begin to stretch, so the length by which the object extends relative to the initial length is the tensile strain, taking the form of Equation 2. Figure 15 illustrates an idealized representation of a stress-strain relationship with both elastic and plastic deformation regions. From a stress – strain relationship, the Young’s modulus, or elasticity (E), of a material can be determined. Elasticity, for the purposes of this study, is the slope of the stress-strain relationship until a yield stress point, as defined by Equation 3 and visualized in Figure 15. A tensile strength test will reveal the elastic and plastic ranges of root-soil matrix yielding a better model of failure for floating marshes.

\[
\text{Stress} = \frac{\text{Force}}{\text{Cross sectional area}} \quad \text{Equation 1}
\]

\[
\text{Strain (unitless)} = \frac{\text{Final Length} - \text{Initial Length}}{\text{Initial Length}} \quad \text{Equation 2}
\]

\[
\text{Young’s Modulus (E)} = \frac{\Delta \text{Stress}}{\Delta \text{Strain}} \quad \text{Equation 3}
\]
2.4 Floating Mat Island Simple Model

Richard Heggen (2015) proposed a conceptual model and a force body diagram for the physics involved with floating islands. This work is applicable for a simple flotant conceptual model. In the case of floating marshes, equal distribution of weight can be assumed due to the small number of trees found growing on them. Case 1 in Figure 16 illustrates the simple load distribution assumption (Heggen, 2015).

The normal forces acting on a section of floating marsh are the weight of the mat, the buoyant force of the water, and the normal stress imposed by the current against the side of the mat. The bulk densities of biomass, soil, gasses, and water within substrate determine the mat buoyancy as seen in Figure 17 (Heggen, 2015). For this stage of research, the net force between the distributed weight and buoyancy is considered to be zero. No vertical movement is assumed and only the
stream-wise forces are considered. The normal stresses from current will be a function of the mat aspect ratio and the velocity of the approaching flow. Depending on the physical dimensions of the mat and its orientation to flow, shearing forces underneath may not contribute significantly to mat displacement relative to normal loads from the approaching current.

Figure 17. Visualization of the different volumes of mat components including soil, air, biomass, and water (Heggen, 2015).

\[ F_s = \text{Volumetric proportion of the soil component.} \]

\[ F_{\text{veg}} = \text{Volumetric proportion of the biomass component.} \]

\[ F_{\text{eg}} = \text{Volumetric proportion of the gas component.} \]

\[ F_{\text{ew}} = \text{Volumetric proportion of the pore water component.} \]

\[ F_{\text{vent}} = \text{Volumetric proportion of the vented water component.} \]

The total stream-wise loads on the floating marsh mats are idealized using simple the drag force calculation described in Equation 4

\[ F_{\text{drag}} = 0.5 C_d \rho V^2 A \]  

Equation 4

Where:

\[ F_{\text{drag}} = \text{Drag force} \]
$C_d = \text{Drag coefficient}$

$\rho = \text{Density of water}$

$V = \text{Approach velocity}$

$A = \text{Front face wetted area or (draft) \times bank to edge mat width } W_m.$

The numerical simulations performed in the scope of this thesis will roughly follow this simple model in order to quantify the stream-wise hydrodynamic loads on floating marsh mats. Various mat aspect ratios will be set into a modeled channel with one edge connected to the bank. The mats will essentially be idealized rectangular prisms or bluff bodies set at the water surface with little to no freeboard. The stream-wise loads will then be modeled for a range of approach velocities leading to the determination of mat-specific drag coefficients. The simulated forces from the CFD model will be used as inputs in a static FEA. Here, estimated vegetative root-soil matrix strength properties from a laboratory tensile strength test are incorporated into the static model. Further details regarding these material properties are described below in section 3 of this thesis.
3. Vegetation Strength Measurement Methodology

3.1 The Marsh Mat Tensile Strength Tester (MMTST)

3.1.1 General

Being that floating marshes possess many components within their mats (i.e. peat, roots, rhizomes), the ability to make an in-situ bulk strength measurement is crucial to the study. The design for a tensile strength testing instrument required a wetland-portable, waterproof, inexpensive device that was also light weight enough for single person operation. The Marsh Mat Tensile Strength Tester, or MMTST was created to measure bulk material properties of vegetative mats while also meeting the design criteria listed above. (Figure 18)

Materials:

1. Three 5 in long 2x4 wood sections
2. Two 24 in long 2x4 wood sections
3. Tension scale (50lb)
4. Two 12 in long tine manual tilling forks
5. Two 3 in U-Bolts
6. One 4 ft long tension cord or twine
7. One ratchet strap device
8. One tape measure

Figure 18. MMTST Broken down into its major components.
The major components of the MMTST are two large forks that will make contact with the sample, the scale that measures the tension applied to said forks, a ratchet that is used to apply incremental increases of tension, and the frame that holds full assembly together. Because the device innately has internal friction, a calibration of the MMTST was conducted. Figure 19 visualizes the calibration process, including the 3 regions of tension that were measured separately in order to identify the location of friction within the device.

![Figure 19](image_url)

**Figure 19.** Photo of full assembly (a). Colored schematic of calibration setup (b).

The reproducible steps for calibrating the MMTST device are numbered below for ease.

1. Apply the tension scale to known tensions as direct measurements by simply hanging the weights from scale. (Direct, in red)
2. Mark weights used for direct measurements for future reference, validation, and calibration.
3. Attach a weight to device forks via a pulley and twine/cord system.
4. Take a measurement of the horizontal tension between the two forks. (Non ratchet, in blue)
5. Then assemble the entire device and take tension measurement with the scale in its position on top of the tester. (Full, in green)
6. Locate any source of friction, and record any differences and observations.

7. Repeat for each increment of increasing weight.

8. Lubricate fork mount tracks and pulley system.

### 3.2.2 Methods and Rationale

Figure 19 includes the actual device calibration setup and illustrates the 3 tension measurement locations via color-coding. The red portion of the schematic in Figure 19b is showing the location of the “direct” tension value, which was measured by attaching the scale directly to the weight (just under the red pulley in schematic). The blue segment of the schematic is showing the tension value between the two forks, which would be the actual tension imposed on the device by the sample. This is how the “non ratchet” (N.R.) values were measured. The green segment of Figure 19b represents the tension measured by the scale while the device is completely assembled. The full assembly requires that the tension load be directed through the pulley system and translated through the scale to the ratchet. The tension scale physically rests on top of the device and is pulled left towards the ratchet during the test. The difference between the “direct” (red) and “non ratchet” (blue) tension is considered to be the friction added by the pulley in place between them. The difference between the “non ratchet” tension value and the “full” tension (green) measurement is considered to be the friction that is intrinsic to the device. This friction is created by the track bearings that guide the fork mount and the 2 pulleys that redirect the tension from the fork mount to the ratchet on top of the device. Taking tension measurements in the three segments allowed for the location of the source of added friction. Figure 20 displays the two sources of friction identified, the number 1 denotes the pulley system and number 2 denotes the fork mount tracks.
3.2.3 Calibration Procedure

The Rapala 50lb scale will be the primary tensile strength measuring instrument for research due to its larger range and possession of a maximum force indicator (Figure 21). For comparison, the instantaneous weight indicator is at rest on the “0” as no force is acting on the device. The sliding force indicator is on “20” allowing for easy observation and recall during sample testing.

Figure 21. Rapala 50lb scale

For the Rapala 50 lb scale Full data measurements, direct tensions between 2.5 – 25 lbs were applied. Resulting N.R. values ranged from 2.5 – 28lbs and Full values ranged from 3 – 27 lbs.
Using to Equation 5 find added friction, it was determined that the difference between N.R. and Full was not constant. From here, a relationship between the two measurements was investigated using Equation 6. Figure 22 is a plot of the N.R. vs. Full relationship; from this data, a calibration factor was created. In order to test the predictability of the calibration factor value 1.33, a predetermined Full measurement target of 27 lbs was calculated for a N.R. tension value of 20 lbs. Next, 20 lbs of weight was applied to the device. The Full tension measurement was in fact found to be 27 lbs for an applied N.R. load of 20 lbs, suggesting that the 33% of added tension is fairly consistent for the range tested. A linear relationship best describes the contribution of internal friction from the device. Equation 7, Table 1, Figure 23, & Figure 24 indicate that the percentage change of added friction vs increasing tension load is constant, thus a linear relation is sufficient for calibration.

\[
\text{Added Friction} = \text{Full Tension} - \text{Non Ratchet Tension} \quad \text{Equation 5}
\]
\[
\text{Percent Difference} = 100 \times \frac{\text{Non Ratchet lbs}}{\text{Full lbs}} \quad \text{Equation 6}
\]
\[
\text{Correct stress value} = \frac{\text{Full Tension measurement (Raw Data)}}{1.33 \text{ (Calibration factor)}} \quad \text{Equation 7}
\]

Table 1. Tabulated results of the MMTST calibration test.

<table>
<thead>
<tr>
<th>Weight lbs</th>
<th>Direct lbs</th>
<th>Non Ratchet lbs</th>
<th>Full lbs</th>
<th>Added friction lbs</th>
<th>% diff</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.5</td>
<td>2.5</td>
<td>2.5</td>
<td>3</td>
<td>0.5</td>
<td>83.33</td>
</tr>
<tr>
<td>5</td>
<td>5</td>
<td>5</td>
<td>6</td>
<td>1</td>
<td>83.33</td>
</tr>
<tr>
<td>7.5</td>
<td>7.5</td>
<td>7.5</td>
<td>9.5</td>
<td>2</td>
<td>78.95</td>
</tr>
<tr>
<td>10</td>
<td>10</td>
<td>10</td>
<td>14</td>
<td>4</td>
<td>71.43</td>
</tr>
<tr>
<td>12.5</td>
<td>12</td>
<td>13</td>
<td>17</td>
<td>4</td>
<td>76.47</td>
</tr>
<tr>
<td>15</td>
<td>15</td>
<td>15</td>
<td>21</td>
<td>6</td>
<td>71.43</td>
</tr>
<tr>
<td>17.5</td>
<td>17</td>
<td>18</td>
<td>23</td>
<td>5</td>
<td>78.26</td>
</tr>
<tr>
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<td>20</td>
<td>20</td>
<td>27</td>
<td>7</td>
<td>74.07</td>
</tr>
<tr>
<td>25</td>
<td>26</td>
<td>28</td>
<td>-</td>
<td>AVG</td>
<td>77.16</td>
</tr>
</tbody>
</table>
Figure 22. Relationship between tension applied (N.R.) vs tension recorded (Full) in lbs

\[ y = 1.3319x \]
\[ R^2 = 0.9931 \]

Figure 23. Amount of added friction as a function of tension applied on MMTST.

\[ y = 0.3319x \]
\[ R^2 = 0.9098 \]
In conclusion, the 50lb Rapala scale accurately measured the applied weight, matching the poundage on the test weights exactly. Internal friction, as a result of the device design (pulleys and tracks), is a linear function of the tension load on the system itself. The source of the friction was located and its effect was captured quantitatively with a calibration factor of 1.33. The calibration factor means that the tension measured by the device is 1.33 times larger than the actual tension being imposed on the forks. Data recorded by this device will need to be multiplied by 0.75 to get the true value of tensile stress.

Figure 24 The percent of the tension measured that is the applied tension. This percentage is constant over the range tested.
3.1.4 Test Procedure

After the MMTST device was calibrated, it was then used to measure the tensile strength of sod grass. Sod was chosen because of the rough similarity to a marsh mat, the relative ease of access to samples, and the presence of tensile strength data for comparison. Multiple test samples were measured for Y.M., Y.S., and U.S. in order to work out the most efficient and precise test procedures possible. Weaknesses in the device were identified and repaired or modified.

MMTST procedural sampling steps.

1) Once the device is completely assembled, attach the tape measure (Figure 25a) and set the initial length at 11.5 inches (the distance between forks).

2) Attach one end of sod sample to sliding fork while the sliding fork is attached to the device and slip the sample to the very top of the tines. (Note: leave a few inches between the penetration point and the end of the sample)

3) Detach the fixed fork from the device.

4) Then penetrate the sod sample roughly 11.5 inches away from the sliding fork.

5) Now re-attach the fixed fork with the sod on it back onto the device.

6) Eliminate any “sag” in the sample by working the sod down both forks while taking care to minimize added tension. Mitigate sag and initial tension (Figure 25b)

7) Check device to make sure all cords are on the pulley tracks and not jammed or caught. (Figure 25c)

8) Begin to ratchet the sample and take measurements every 0.25 -0.5 inches change in length of sample.

9) Take care to make sure the scale and sliding fork apparatus do not interfere with each other.

10) Record tension and length measurements until complete failure (Figure 25d).
11) If complete separation cannot be attained, record max stress and take note of any plastic deformation.

12) Record tension and length measurements until complete failure (Figure 25d).

13) If complete separation cannot be attained, record max stress and take note of any plastic deformation.

Figure 25. Step 1: Setting tape measure (a). Step 6: Sod sample sagging (b). Step 7: Jammed pulley (c). Step 10: Complete failure (d)

Figure 26 is a representative sod tensile strength test data plot. The data denoted by the orange markers shows the sod strength measurements recorded by the fully assembled MMTST. Due to added internal friction from the device, the data must be adjusted by the previously mentioned calibration factor shown in Figure 22. The data points denoted by the blue markers are the true
values of tensile stress. Per a qualitative observation, this sample was relatively dry (low moisture saturation); as a result, the elasticity is slightly inflated relative to the more saturated samples tested in the results.

![Stress vs. strain curve](image)

**Figure 26** Representative stress vs. strain curve for a sod strength test.

The use and main objective of this test is to determine three values, Young’s modulus, yield stress, and ultimate strength. Young’s modulus (E) is the elasticity of the vegetative mat or sod and ultimate strength is the maximum tensile stress the material can withstand before failure. In order to extract the linear E of the sample, a yield stress point needs to be determined, which in this case is close to the ultimate stress (Figure 26). All data points after the yield stress value are considered plastic, and the linear slope of the stress vs. strain relationship is determined, as seen in Figure 27.
Figure 27 Stress vs. strain relationship for linear region with strain adjustment.

The slope of the line in Figure 27 is the Young’s modulus of the sod sample. The design of the MMTST uses a base starting length of 11.5in from which the strain is measured. As a note, the installation of the sod onto the forks applies a small amount of stress to the material to eliminate the sag due to gravity. This “pre-stress” shifts the strain slightly towards the origin as seen in Figure 27. The calculation of the pre-stress involves extrapolating the slope of the linear region back to the tensile stress axis intercept. The slope of the elastic region is theoretically unaffected by this shift and the tensile stresses applied are true. Some post processing of the data is required to adjust the strain. For this reason, stress values are used for determination of the failure criterion applied later in the analysis.
3.2 Sod Tensile Strength Test Results

The MMTST was used in a larger sample set study with 9 pieces of sod. The goal of the test was to gather enough data in order to determine validity of the methods and the device by comparing the tensile strength results to previous sod strength studies. The validation literature chosen was a sod tensile strength study conducted by Neil Heckman et al (2001) where previously published sod strength values were converted from kg to units of stress (kPa) (Table 2) (Giese et al. 1997).

Table 2. Sod tensile strength data (Heckman et al 2001)

<table>
<thead>
<tr>
<th>Sod Tensile Strength</th>
<th>Nebraska</th>
<th>Texas</th>
<th>Nebraska</th>
<th>Texas</th>
</tr>
</thead>
<tbody>
<tr>
<td>Genotypes</td>
<td>Psi</td>
<td>kPa</td>
<td>Psi</td>
<td>kPa</td>
</tr>
<tr>
<td>609</td>
<td>1.29</td>
<td>8.08</td>
<td>8.9</td>
<td>55.7</td>
</tr>
<tr>
<td>315</td>
<td>13.31</td>
<td>0.61</td>
<td>91.7</td>
<td>4.2</td>
</tr>
<tr>
<td>378</td>
<td>0.07</td>
<td>-</td>
<td>0.5</td>
<td>-</td>
</tr>
<tr>
<td>NE 84-45-3</td>
<td>1.22</td>
<td>2.86</td>
<td>8.4</td>
<td>19.7</td>
</tr>
<tr>
<td>NE84-436</td>
<td>1.02</td>
<td>0.48</td>
<td>7</td>
<td>3.3</td>
</tr>
<tr>
<td>AZ-143</td>
<td>1.02</td>
<td>1.29</td>
<td>7</td>
<td>8.9</td>
</tr>
<tr>
<td>Prairie</td>
<td>-</td>
<td>3.40</td>
<td>-</td>
<td>23.4</td>
</tr>
<tr>
<td>Sharps Improved</td>
<td>0.35</td>
<td>-</td>
<td>2.4</td>
<td>-</td>
</tr>
<tr>
<td>NTDG-1</td>
<td>0.61</td>
<td>1.63</td>
<td>4.2</td>
<td>11.2</td>
</tr>
<tr>
<td>NTDG-2</td>
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<td>1.90</td>
<td>4.2</td>
<td>13.1</td>
</tr>
<tr>
<td>NTDG-3</td>
<td>0.20</td>
<td>0.28</td>
<td>1.4</td>
<td>1.9</td>
</tr>
<tr>
<td>NTDG-4</td>
<td>0.89</td>
<td>-</td>
<td>6.1</td>
<td>-</td>
</tr>
<tr>
<td>Bison</td>
<td>0.42</td>
<td>3.67</td>
<td>2.9</td>
<td>25.3</td>
</tr>
<tr>
<td>LSD</td>
<td>3.27</td>
<td>3.27</td>
<td>22.5</td>
<td>22.5</td>
</tr>
<tr>
<td>Average</td>
<td>2.16</td>
<td></td>
<td>14.85</td>
<td></td>
</tr>
<tr>
<td>Standard Deviation</td>
<td>2.90</td>
<td></td>
<td>19.95</td>
<td></td>
</tr>
</tbody>
</table>

These strength values were found by applying a tensile load to each sod sample until complete failure. The ruptured pieces were then placed back together and the new length was measured to get a value for strain. The reported tensile strength is the ultimate stress divided by the final strain (Heckman et al 2001. Giese et al. 1997). There are no intermediate data points used in the calculation. For consistency, the final stress and strain values (with strain adjustment) from the 9
sample sod test are used for comparative validation. The results from the experiment are presented in Table 3.

Table 3. Tabulated results from the 9 sample sod test.

<table>
<thead>
<tr>
<th>Trial</th>
<th>Strength [psi]</th>
<th>Strength [kPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>2.44</td>
<td>16.79</td>
</tr>
<tr>
<td>2</td>
<td>3.98</td>
<td>27.46</td>
</tr>
<tr>
<td>3</td>
<td>3.78</td>
<td>26.07</td>
</tr>
<tr>
<td>4</td>
<td>2.52</td>
<td>17.33</td>
</tr>
<tr>
<td>5</td>
<td>4.05</td>
<td>27.90</td>
</tr>
<tr>
<td>6</td>
<td>2.66</td>
<td>18.36</td>
</tr>
<tr>
<td>7</td>
<td>4.38</td>
<td>30.16</td>
</tr>
<tr>
<td>8</td>
<td>3.44</td>
<td>23.67</td>
</tr>
<tr>
<td>9</td>
<td>4.54</td>
<td>31.27</td>
</tr>
<tr>
<td>Average</td>
<td>3.53</td>
<td>24.33</td>
</tr>
<tr>
<td>Stdev</td>
<td>0.76</td>
<td>5.27</td>
</tr>
</tbody>
</table>

The average of the tensile strength values falls well within the large 0.5 – 91.7 kPa range, however, it is larger than the reported mean in Heckman et al (2001). This may have resulted from differences in the environmental growing conditions or a variation in sod moisture content for each data set, both of which are unknown. That being said, the average value of 24.33 kPa resides within <+0.5 standard deviation of data set from literature.

For this study, a full stress-strain profile is needed to extract the linear elasticity (Y.M.), yield stress, and ultimate strength (U.S.). These profiles are plotted in Figure 28. The Young’s modulus for each sample was determined from the slope of the linear region and averaged. Given the spread of values, Young’s moduli & U.S.’s were determined for +1 and -1 standard deviation of their respective means. These approximations will be used to determine the sensitivity of the material parameters on floating marsh mat failure due to loads from flow. Figure 29 depicts the Young’s modulus spread from the sod test. Table 4 and Table 5 are the tabulated results with mean, +1 Stdev, & -1 Stdev for Y.M. and U.S.
Figure 28. Stress vs strain relationship for 9 sod samples with strain adjustment.

Figure 29. Linear elasticity range (Y.M.). Maximum, +1 Stdev, Mean, -1 Stdev, Minimum
Table 4 Statistical analysis on sod tensile strength tests.

<table>
<thead>
<tr>
<th>Sample ID</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
<th>8</th>
<th>9</th>
<th>Mean</th>
<th>Stdev</th>
</tr>
</thead>
<tbody>
<tr>
<td>Y.M. [psi]</td>
<td>3.20</td>
<td>4.68</td>
<td>4.72</td>
<td>4.10</td>
<td>4.30</td>
<td>3.61</td>
<td>6.43</td>
<td>5.05</td>
<td>5.60</td>
<td>4.63</td>
<td>0.93291</td>
</tr>
<tr>
<td>U.S. [psi]</td>
<td>1.36</td>
<td>1.27</td>
<td>1.39</td>
<td>1.79</td>
<td>1.13</td>
<td>1.41</td>
<td>1.64</td>
<td>1.69</td>
<td>1.27</td>
<td>1.437966</td>
<td>0.207816</td>
</tr>
</tbody>
</table>

Table 5. Standard deviations & mean for Young’s modulus and ultimate strength.

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>+1 Stdev</td>
<td>5.566</td>
<td>1.646</td>
<td>38.37738</td>
<td>11.34727</td>
</tr>
<tr>
<td>Mean</td>
<td>4.633</td>
<td>1.438</td>
<td>31.94519</td>
<td>9.914429</td>
</tr>
<tr>
<td>-1 Stdev</td>
<td>3.700</td>
<td>1.230</td>
<td>25.51299</td>
<td>8.481585</td>
</tr>
</tbody>
</table>

The sod tensile strength tests show an average strain value for yield stress (Y.S.) is 0.3. All of the sod samples either completely failed at this point or transitioned into plastic deformation. Using the strain value of 0.3 for a yield point, the three yield stresses corresponding to the +1 Stdev, mean, & -1 Stdev Y.M. values are 11513 Pa, 9583 Pa, & 7653 Pa respectively. This yield stress range spans the U.S. range in Table 5, therefore Y.S. will be used for the failure criterion ($F_c$) later in this analysis. Additionally, a fourth value will be included in the set of $F_c$’s tested. 4468 Pa is a converted failure stress roughly based on the average rotational shear stress of a *Panicum hemitomon* marsh measured by Sasser et al (2013).
4. Modeling Methodology

4.1 ANSYS Fluent

4.1.1 General

ANSYS Fluent is the Computational Fluid Dynamics (CFD) software used for this study. Academic research versions 16.0 and 17.0 were available for use through LSU and with SuperMikEII High Performance Computer (HPC) cluster (LSU HPC, 2017). Fluent is a versatile program capable of simulating a multitude of situations including compressible & incompressible flows for varying levels of turbulence. In particular, the multiphase flow model was utilized for the purposes of this thesis. This provided the ability to simulate forces on floating marshes within 2-D & 3-D open channel simulations. Further details can be located in the Fluent user manual (2013).

4.1.2 Theoretical Background

4.1.2.1 Governing equations

ANSYS Fluent solves the continuity and momentum conservation equations for either 2-D or 3-D simulations. The differential form of the continuity equation (Equation 8) can be used for either compressible or incompressible flow (ANSYS, 2013).

\[ \frac{\partial \rho}{\partial t} + \nabla \cdot (\rho \vec{V}) = 0 \]  

Equation 8

Where \( \rho \) is fluid density, \( t \) is time, \( \nabla \cdot \) is the divergence operator, and \( \vec{V} \) is the velocity vector field. This thesis only considers incompressible flow for both the air phase and water phase which eliminates the density terms, reducing Equation 8 into the expanded form in Equation 9 with Cartesian directions \( x, y, \) & \( z \), \( u, v, \) & \( w \) are the flow velocities in the three Cartesian directions respectively.

\[ \frac{\partial u}{\partial x} + \frac{\partial v}{\partial y} + \frac{\partial w}{\partial z} = 0 \]  

Equation 9
The momentum conservation equation (Equation 10) is solved together with the continuity equation throughout the domain for steady or unsteady flows. This study utilizes transient simulations, which includes the unsteady time derivative term on the left-hand side (L.H.S). Equation 11 defines the stress tensor \( \tilde{\tau} \), which captures the viscous effects of the flow located on the right-hand side (R.H.S) of Equation 10 (ANSYS, 2013).

\[
\frac{\partial}{\partial t} (\rho \vec{V}) + \nabla \cdot (\rho \vec{V} \vec{V}) = -\nabla p + \nabla \cdot (\tilde{\tau}) + \rho \vec{g} + \vec{F} \tag{Equation 10}
\]

\[
\tilde{\tau} = \mu \left( \nabla \vec{V} + \nabla \vec{V}^T \right) - \frac{2}{3} \nabla \cdot \vec{V} I \tag{Equation 11}
\]

Where \( \nabla \) is the gradient operator, \( p \) is the local static pressure term, \( \vec{g} \) is the gravitational body force vector, \( \vec{F} \) is the external source term, \( \mu \) is the fluid’s viscosity, \( \vec{V}^T \) is the transposed velocity vector field, and \( I \) is the identity matrix. For clarity, the mass conservation equations (12, 13, 14) for the three Cartesian directions (x, y, & z) are expanded below along with the stress tensor terms.

\[
\rho \left( \frac{\partial u}{\partial t} + u \frac{\partial u}{\partial x} + v \frac{\partial u}{\partial y} + w \frac{\partial u}{\partial z} \right) = \frac{\partial p}{\partial x} + \frac{\partial}{\partial y} \left[ \mu \left( \frac{\partial u}{\partial y} - \frac{2}{3} \nabla \cdot \vec{V} \right) \right] + \frac{\partial}{\partial z} \left[ \mu \left( \frac{\partial u}{\partial z} + \frac{\partial w}{\partial x} \right) \right] + \rho \vec{g} + \vec{F}_x \tag{Equation 12}
\]

\[
\rho \left( \frac{\partial v}{\partial t} + u \frac{\partial v}{\partial x} + v \frac{\partial v}{\partial y} + w \frac{\partial v}{\partial z} \right) = \frac{\partial p}{\partial y} + \frac{\partial}{\partial x} \left[ \mu \left( \frac{\partial v}{\partial x} + \frac{\partial u}{\partial y} \right) \right] + \frac{\partial}{\partial z} \left[ \mu \left( \frac{\partial v}{\partial z} + \frac{\partial w}{\partial y} \right) \right] + \rho \vec{g} + \vec{F}_y \tag{Equation 13}
\]

\[
\rho \left( \frac{\partial w}{\partial t} + u \frac{\partial w}{\partial x} + v \frac{\partial w}{\partial y} + w \frac{\partial w}{\partial z} \right) = \frac{\partial p}{\partial z} + \frac{\partial}{\partial x} \left[ \mu \left( \frac{\partial w}{\partial x} + \frac{\partial u}{\partial z} \right) \right] + \frac{\partial}{\partial y} \left[ \mu \left( \frac{\partial w}{\partial y} + \frac{\partial v}{\partial z} \right) \right] + \rho \vec{g} + \vec{F}_z \tag{Equation 14}
\]

4.1.2.2 Turbulence

The simulations performed in this study consist of bluff body objects that disrupt the flow field in the domain, which does not allow for a true steady state. For this reason, a transient solution with a turbulence model is preferred. Fluent’s standard k-\( \epsilon \) turbulence model capacitates the convergence needed to adequately model the forces encountered by the mats. The k-\( \epsilon \) model is one
of the most widely used methods for turbulence in CFD by virtue of its computational efficiency and its applicability for a broad range of mesh resolutions. For many situations in Fluent, the computational grids are too coarse for directly resolving the turbulence numerically. Generally, the Navier-Stokes differential equations undergo Reynold’s averaging involving a separation of the pressure and velocity components respectively into their mean and fluctuation values, a process known as the Reynold’s decomposition (Equation 15 & 16) (ANSYS, 2013)

\[ P = \bar{p} + p' \]  \hspace{1cm} \text{Equation 15}

\[ u = \bar{u} + u' \]  \hspace{1cm} \text{Equation 16}

Here, \( \bar{u} \) represents the mean velocity and \( u' \) indicates the velocity fluctuations around the mean. Incorporating the Reynold’s decomposition into the Navier-Stokes differential equations and applying averaging rules (i.e., mean of \( \bar{u} = 0 \)) results in the Reynold’s averaged Navier-Stokes equations or RANS. In Equation 17, the RANS tensor form, a new term appears on the R.H.S. called the apparent stresses, \( \rho u'_i u'_j \), where \( i \) and \( j \) are vector indices and \( \delta_{ij} \) is the Kronecker delta.

\[
\rho \left( \frac{\partial u_i}{\partial t} + \bar{u}_j \frac{\partial u_i}{\partial x_j} \right) = -\frac{\partial p}{\partial x_j} + \frac{\partial}{\partial x_j} \left[ \mu \left( \frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} - \frac{2}{3} \delta_{ij} \frac{\partial u_l}{\partial x_l} \right) \right] - \rho u'_i u'_j + \rho \tilde{g} \]  \hspace{1cm} \text{Equation 17}

The issue with this new Reynold’s stress term is that now more PDE’s are required to close the system of equations, a closure problem. The k-\( \varepsilon \) model remedies a closure by solving a system of 2 additional second order PDE’s, one for turbulent kinetic energy (k) and the other for turbulent dissipation rate (\( \varepsilon \)). Finding \( k \) & \( \varepsilon \) allows for the calculation of turbulent eddy viscosity, \( \nu_t \) (Equation 18), which is then combined with a rate-of-strain tensor (Equations 19 & 20) to approximate the Reynolds stress term (Equation 21) (ANSYS, 2013).

\[ \nu_t = \frac{C_{\mu}k^2}{\varepsilon} \]  \hspace{1cm} \text{Equation 18}

\[ G_k = \nu_t S^2 \]  \hspace{1cm} \text{Equation 19}
\[ S \equiv \sqrt{S_{ij}S_{ij}} \]  

Equation 20

\[ G_k = -\rho u'_i u'_j \frac{\partial u'_j}{\partial x_i} \]  

Equation 21

Six PDEs are solved for the 6 unknowns: \( \ddot{u}_1, \ddot{u}_2, \ddot{u}_3, p, k, \varepsilon \). The two \( k-\varepsilon \) transport equations are defined in equations 22 and 23. \( G_k \) is the production term defined in Equation 21. \( G_b \) is the turbulent kinetic energy contribution from buoyancy. The constants \( C_\mu, C_{1\varepsilon}, C_{2\varepsilon}, C_{3\varepsilon} \) are empirical values 0.09, 1.44, 1.92, and -0.33 respectively along with the turbulent Prandtl numbers, \( \sigma_k \) and \( \sigma_\varepsilon \), with values of 1 & 1.3 (Fluent defaults). \( Y_M \) is the compressible turbulence term and \( S_k \) & \( S_\varepsilon \) are source input values.

\[
\frac{\partial (\rho k)}{\partial t} + \frac{\partial (\rho k \ddot{u}_i)}{\partial x_i} = \frac{\partial}{\partial x_j} \left[ \left( \mu + \frac{\nu_t}{\sigma_k} \right) \frac{\partial k}{\partial x_j} \right] + G_k + G_b - \rho \varepsilon - Y_M + S_k \quad \text{Equation 22}
\]

\[
\frac{\partial (\rho \varepsilon)}{\partial t} + \frac{\partial (\rho \varepsilon \ddot{u}_i)}{\partial x_i} = \frac{\partial}{\partial x_j} \left[ \frac{\nu_t}{\sigma_k} \frac{\partial \varepsilon}{\partial x_j} \right] + C_{1\varepsilon} \frac{\varepsilon}{k} (G_\varepsilon + C_3 G_b) - C_{2\varepsilon} \frac{\varepsilon^2}{k} + S_\varepsilon \quad \text{Equation 23}
\]

4.1.2.3 Open channel flow

Floating marshes exist within hydraulic systems open to the atmosphere, requiring open channel flow simulations. Fluent performs this task with a multiphase model. The multiphase modeling approach is utilized for situations with more than one medium such as: liquid-liquid, liquid-solid, and liquid-gas. The liquid-gas multiphase model is ideal for open channel systems that require a defined air-water interface at the free surface (ANSYS, 2013).

To model domains with a clearly defined air-water interface, Fluent offers a Euler-Euler approach called the Volume of Fluid (VOF) model. The underlying assumption in the Euler-Euler approach is that the volume of either phase cannot occupy the other and are mathematically interpreted as a smooth continuum. This is accomplished by having a defined volume fraction at
all locations within a given domain where the respective volume fractions of all phases sum up to a value of 1 (ANSYS, 2013).

The VOF model computes one set of momentum equations for two immiscible fluids by tracking the transport of the volume fraction throughout the mesh. Both steady and transient simulations are possible in conjunction with a pressure based solver. The volume fraction \(a_q\), where subscript \(q\) denotes the phase, for an air-water simulation is defined as follows:

\[ a_{\text{water}} = 0; \text{These cells contain no water and would be full of air (} a_{\text{air}} = 1) \]

\[ 0 < a_{\text{water}} < 1; \text{These cells contain portions of both phases and are defined as the interface.} \]

The continuity equation for the Volume of Fluid method (Equation 24) is modified by incorporating the volume fraction term. The computed density of a cell is calculated with Equation 25, subscripts are denoted by air and water for convenience.

\[
\frac{1}{\rho_q} \left[ \frac{\partial}{\partial t} (a_q \rho_q) + \nabla \cdot (a_q \rho_q \vec{v}_q) \right] = \sum_{p=1}^{n} (\dot{m}_{pq} - \dot{m}_{qp})
\]

\[
\rho = a_{\text{water}} \rho_{\text{water}} + (1 - a_{\text{water}}) \rho_{\text{air}}
\]

Where \(\dot{m}_{pq}\) represents the mass transfer between the \(p\) and \(q\) phases. The time discretization of the VOF simulation can be either implicit or explicit. For this research an implicit finite difference scheme is used for the transport of the volume fraction (Equation 26) (ANSYS, 2013).

\[
\frac{a_q^{n+1} \rho_q^{n+1} - a_q^n \rho_q^n}{\Delta t} V + \sum_f \left( \rho_q^{n+1} U_f^{n+1} a_{qf}^{n+1} \right) = V \sum_{p=1}^{n} \left( \dot{m}_{pq} - \dot{m}_{qp} \right)
\]

Where \(n+1\) & \(n\) are time step indexes, \(U_f\) is the volume flux through a face, \(V\) is cell volume, and \(a_{qf}\) is the volume fraction at a face. Behavior of the air-water interface (free surface) in an open channel system is defined by the dimensionless Froude number (Equation 27) which is the ratio of the fluid velocity to speed of pressure propagation. In situations where the velocity of the fluid
medium is greater than the rate at which a wave can propagate (defined by \( \sqrt{gy} \), where \( g \) is gravitational acceleration and \( y \) is flow depth), the flow is considered to be supercritical \((Fr > 1)\)

\[
Fr = \frac{v}{\sqrt{gy}}
\]

Equation 27

All cases considered in this thesis are sub critical flows, meaning \( Fr < 1 \). To generate this flow, mass flow rate and pressure outlet boundary conditions are employed. The mass flow rates for each phase are defined at the upstream boundary with Equation 28. A free surface elevation is also defined for both upstream and downstream boundaries with Equation 29. \( \mathbf{a} \) is the position vector for the free surface located at a cell face centroid. The direction of gravity is considered to be perpendicular to the horizontal plane of the water surface (ANSYS, 2013).

\[
\dot{m} = \rho_{phase}(Area_{phase})(Velocity)
\]

Equation 28

\[
Y_{local} = - (\mathbf{a} \cdot \mathbf{g})
\]

Equation 29

**4.2 ANSYS Static Structural**

**4.2.1 General**

ANSYS provides a Finite Element Analysis (FEA) package for structural problems. This study utilizes the static structural application available in ANSYS research versions 16.0 & 17.0 for material analysis of floating marshes. The modeling approach presented in this thesis takes advantage of the 3-D structural analysis provided by the software in order to predict the behavior of the mats under certain constraints and stream-wise loadings imposed by the flow. Determining the conditions required for failure of floating marshes is accomplished by discretizing the idealized floating marshes into many elements, applying boundary conditions, and interpolating the internal stresses and strains. A brief theoretical summary of 3-D material continuum mechanics used by ANSYS is included along with a simple explanation of the Finite Element Method (FEM).
4.2.2 Theoretical Background

4.2.2.1 Governing Equations

For materials in three dimensions, there are three normal stresses $\sigma_{x,y,z}$ that act perpendicular to a given face of the material in the x, y, or z directions. If the stress direction is into the material it is acting in compression, and if the stress is pulling outward it is considered to be acting in tensile. Sign convention dictates that all tensile stresses are positive (+) and all compressive stresses are negative (-). Additionally, there are external shear stresses that act parallel along a given face. Shear stress notation is defined with subscripts indicating both the face and direction. For instance, the shear stress acting in the y direction along a face perpendicular to the x axis will be $\sigma_{xy}$. A diagram of the 18 possible stresses on a 3-D is presented in Figure 30 (ANSYS 2013).

![Diagram of stresses](image)

Figure 30. Geometrical diagram of the normal and shear stresses on a body (ANSYS, 2013).

A material under stress will deform; this deformation is quantified as strain, $\varepsilon$. Strains are defined as the ratio of the displacement to the original length in a particular direction. A material that is squeezed or pulled will undergo a compressive (-) and tensile (+) strain ($\varepsilon_{x,y,z}$), respectively. Planar stresses acting parallel to a surface will result in shear strains $\varepsilon_{xy}$, $\varepsilon_{yz}$, & $\varepsilon_{xz}$. Stresses and
strains are related by a material’s elasticity, usually in the form of Young’s modulus $E_i$. Equation 30 defines this relationship between the 6-component stress and strain vectors (Equations 31 & 32) through the use of a 6 x 6 compliance matrix $[D]$ (Equation 33).

\[
\{\sigma\} = [D]\{\varepsilon\} \quad \text{Equation 30}
\]

\[
\{\sigma\} = [\sigma_x\sigma_y\sigma_z\sigma_{xy}\sigma_{yz}\sigma_{xz}]^T \quad \text{Equation 31}
\]

\[
\{\varepsilon\} = [\varepsilon_x\varepsilon_y\varepsilon_z\varepsilon_{xy}\varepsilon_{yz}\varepsilon_{xz}]^T \quad \text{Equation 32}
\]

The compliance matrix $[D]$ structures the direction dependent Young’s modulus values $E_i$, related Poisson’s ratios $v_{ij}$ (Equations 34, 35, 36), and shear moduli $G_{ij}$ (Equation 37) (ANSYS 2013).

\[
[D]^{-1} = \begin{bmatrix}
\frac{1}{E_x} & -\frac{v_{xy}}{E_x} & -\frac{v_{xz}}{E_x} & 0 & 0 & 0 \\
-\frac{v_{yx}}{E_y} & \frac{1}{E_y} & -\frac{v_{yz}}{E_y} & 0 & 0 & 0 \\
-\frac{v_{zx}}{E_z} & -\frac{v_{zy}}{E_z} & \frac{1}{E_z} & 0 & 0 & 0 \\
0 & 0 & 0 & \frac{1}{G_{xy}} & 0 & 0 \\
0 & 0 & 0 & 0 & \frac{1}{G_{yz}} & 0 \\
0 & 0 & 0 & 0 & 0 & \frac{1}{G_{xz}}
\end{bmatrix} \quad \text{Equation 33}
\]

\[
\frac{v_{yx}}{E_y} = \frac{v_{xy}}{E_x} \quad \text{Equation 34}
\]

\[
\frac{v_{zx}}{E_z} = \frac{v_{xz}}{E_x} \quad \text{Equation 35}
\]

\[
\frac{v_{zy}}{E_z} = \frac{v_{yz}}{E_y} \quad \text{Equation 36}
\]

\[
G_{ij} = \frac{E_i}{2(1+v_{ij})} \quad \text{Equation 37}
\]

Physically, the Poisson’s ratio is the proportion of material deformation transverse to load direction to material deformation along the load direction. Figure 31 illustrates the physical
meaning of this parameter with a 2-D case for a material body in tensile. Where dL’ is the distance the material shrinks in the direction transverse to the tensile load and dL is the distance the material is extended in the direction of the tensile load (ANSYS, 2013).

Figure 31. Physical interpretation of the Poisson’s ratio v.

In order to explicitly solve for the stresses, Equation 31 is expanded into the six stress relationships:

Equations 38, 39, 40, 41, 42, & 43. Where h is defined in Equation 37 (ANSYS, 2013).

\[
\sigma_x = \frac{E_x}{h} \left( 1 - (v_{yz})^2 \frac{E_z}{E_y} \right) \varepsilon_x + \frac{E_y}{h} \left( v_{xy} + v_{xz} v_{yz} \frac{E_z}{E_y} \right) \varepsilon_y + \frac{E_z}{h} (v_{xz} + v_{yz} v_{xy}) \varepsilon_z \quad \text{Equation 38}
\]

\[
\sigma_y = \frac{E_y}{h} \left( 1 - (v_{xz})^2 \frac{E_z}{E_x} \right) \varepsilon_y + \frac{E_x}{h} \left( v_{xy} + v_{xz} v_{yz} \frac{E_z}{E_x} \right) \varepsilon_x + \frac{E_z}{h} (v_{yz} + v_{xz} v_{xy}) \varepsilon_z \quad \text{Equation 39}
\]

\[
\sigma_z = \frac{E_z}{h} \left( 1 - (v_{xy})^2 \frac{E_y}{E_x} \right) \varepsilon_z + \frac{E_x}{h} \left( v_{xz} + v_{xy} v_{yz} \frac{E_z}{E_x} \right) \varepsilon_x + \frac{E_y}{h} (v_{yz} + v_{xz} v_{xy}) \varepsilon_y \quad \text{Equation 40}
\]

\[
\sigma_{xy} = G_{xy} \varepsilon_{xy} \quad \text{Equation 41}
\]

\[
\sigma_{yz} = G_{yz} \varepsilon_{yz} \quad \text{Equation 42}
\]

\[
\sigma_{xz} = G_{xz} \varepsilon_{xz} \quad \text{Equation 43}
\]

\[
h = 1 - (v_{xy})^2 \frac{E_y}{E_x} - (v_{yz})^2 \frac{E_z}{E_y} - (v_{xz})^2 \frac{E_z}{E_y} - 2 v_{xy} v_{yz} v_{xz} \frac{E}{E} \quad \text{Equation 44}
\]

To solve for the strains, Equation 23 can be rearranged into Equation 45 and expanded into the six strain relationships: Equations 46, 47, 48, 49, 50, 51 (ANSYS, 2013).

\[
\varepsilon = [D]^{-1} \sigma \quad \text{Equation 45}
\]

\[
\varepsilon_x = \frac{\sigma_x}{E_x} - \frac{v_{xy} \sigma_y}{E_x} - \frac{v_{xz} \sigma_z}{E_x} \quad \text{Equation 46}
\]
\[ \varepsilon_y = \frac{\sigma_y}{E_y} - \frac{v_{xy}\sigma_x}{E_x} - \frac{v_{yz}\sigma_z}{E_y} \quad \text{Equation 47} \]

\[ \varepsilon_z = \frac{\sigma_z}{E_z} - \frac{v_{yz}\sigma_y}{E_y} - \frac{v_{xz}\sigma_x}{E_z} \quad \text{Equation 48} \]

\[ \varepsilon_{xy} = \frac{\sigma_{xy}}{G_{xy}} \quad \text{Equation 49} \]

\[ \varepsilon_{yz} = \frac{\sigma_{yz}}{G_{yz}} \quad \text{Equation 50} \]

\[ \varepsilon_{xz} = \frac{\sigma_{xz}}{G_{xz}} \quad \text{Equation 51} \]

In this study, the floating marsh mats are considered to be isotropic materials for simplicity. In the case of isotropic materials, the elasticity, Poisson’s ratio, and the shear moduli (Equation 52) are constant in all directions. \((E_x = E_y = E_z; v_{xy} = v_{yz} = v_{xz})\)

\[ G_{xy} = G_{yz} = G_{xz} = \frac{E}{2(1+v)} \quad \text{Equation 52} \]

The static structural module available in ANSYS 16.0 is used for the structural analysis of the mats. Here, boundary conditions and forces are applied to the body in order to determine the response and failure of the floating marsh as a material. The discretization and numerical method involved in 3D structural analysis is known as the Finite Element Method (FEM) or Finite Element Analysis (FEA). The FEM is a numerical approach that is efficient for computers. In this study, a given simulated mat is discretized into smaller tetrahedral or triangular prism elements. The FEM process for a simple unit tetrahedral body is briefly explained below.

For static analysis, ANSYS solves the relationship in Equation 53, which relates the applied loads \( \{F\} \) to the displacements \( \{u\} \) using the stiffness matrix \( [K] \).

\[ \{F\} = [K] \{u\} \quad \text{Equation 53} \]
The stiffness matrix is formulated with the following process. Consider the unit tetrahedral body element with Cartesian coordinates \([x, y, z]\) and tetrahedral coordinates \([\zeta_1, \zeta_2, \zeta_3, \zeta_4]\), and a volume of 1/6 (Figure 32).

![Figure 32. The unit tetrahedron with cartesian coordinates [left] & tetra coordinates [right].](image)

The sum of the tetrahedral coordinates is equal to 1 (Equation 54) for any point within the body. Each nodal coordinate \(\zeta_i\) will have a value of 1 at the node \(i\) location and a value of 0 at any point on the opposite face. For instance, \(\zeta_1\) will have a value 0 at on the face shared by nodes 2, 3, & 4. The centroid of the tetrahedron with have the coordinates \([\frac{1}{4}, \frac{1}{4}, \frac{1}{4}, \frac{1}{4}]\) These tetrahedral coordinates are also used as shape functions which interpolate the solution between the nodes for stresses, strains, and displacements (U. Colorado, 2011).

\[\zeta_1 + \zeta_2 + \zeta_3 + \zeta_4 = 1\]  \hspace{1cm} \text{Equation 54}

The tetrahedral coordinates are related to their Cartesian counterparts with the \([B]\) 6 x12 matrix defined in Equation 55.

\[
[B] = \frac{1}{6V} \begin{bmatrix}
a_1 & 0 & 0 & a_2 & 0 & 0 & a_3 & 0 & 0 & a_4 & 0 & 0 \\
0 & b_1 & 0 & 0 & b_2 & 0 & 0 & b_3 & 0 & 0 & b_4 & 0 \\
0 & 0 & c_1 & 0 & 0 & c_2 & 0 & 0 & c_3 & 0 & 0 & c_4 \\
0 & b_1 & a_1 & 0 & b_2 & a_2 & 0 & b_3 & a_3 & 0 & b_4 & a_4 \\
0 & c_1 & b_1 & 0 & c_2 & b_2 & 0 & c_3 & b_3 & 0 & c_4 & b_4 \\
c_1 & 0 & a_1 & c_2 & 0 & a_2 & c_3 & 0 & a_3 & c_4 & 0 & a_4 \\
\end{bmatrix}
\]

\hspace{1cm} \text{Equation 55}

Where: \[6V \frac{\partial \zeta_i}{\partial x} = a_i, \quad 6V \frac{\partial \zeta_i}{\partial y} = b_i, \quad 6V \frac{\partial \zeta_i}{\partial z} = c_i, \quad i = 1, 2, 3, 4.\]
The \([B]\) matrix is used in conjunction with the 6 x 6 linear isotropic elasticity matrix \([E]\) (Equation 56) in order to construct the 12 x 12 stiffness matrix \([K]\) recalled from Equation 53.

\[
[E] = \frac{E}{(1+v)(1-2v)} \begin{bmatrix}
1 - \nu & \nu & 0 & 0 & 0 \\
1 - \nu & \nu & 0 & 0 & 0 \\
1 - \nu & 0 & 0 & 0 & 0 \\
\frac{1}{2} - \nu & 0 & 0 & 0 & 0 \\
\frac{1}{2} - \nu & \frac{1}{2} - \nu & & & \\
\end{bmatrix} \text{ symm.}
\]

Equation 56

\[
[K] = \int_B B^T E B dV
\]

Equation 57

Once evaluated, the relation simplifies into Equation 58, where the 12 x 12 stiffness matrix is found by multiplying together the body volume, the 12 x 6 transpose \([B^T]\), the 6 x 6 \([E]\), and the 6 x 12 \([B]\).

\[
[K] = V [B^T] [E] [B]
\]

Equation 58

The expanded \([K]\) matrix is provided in Equation 59 for clarity.

\[
K = \hat{E} \begin{bmatrix}
4 - 6\nu & 1 & -2\bar{\nu} & -\bar{\nu} & -\bar{\nu} & -2\nu & 0 & -\bar{\nu} & 0 & -2\nu \\
4 - 6\nu & 1 & -2\bar{\nu} & -\bar{\nu} & 0 & -\bar{\nu} & -2\bar{\nu} & -\bar{\nu} & 0 & -2\bar{\nu} \\
4 - 6\nu & -2\nu & 0 & -\bar{\nu} & 0 & -2\nu & -\bar{\nu} & -\bar{\nu} & -\bar{\nu} & -2\bar{\nu} \\
2\bar{\nu} & 0 & 0 & 2\nu & 0 & 0 & 0 & 2\nu \\
\bar{\nu} & 0 & 0 & \bar{\nu} & 0 & 0 & 0 & 0 \\
\bar{\nu} & 0 & 0 & \bar{\nu} & 0 & 0 & 0 & 0 \\
2\bar{\nu} & 0 & 0 & 2\nu \\
\bar{\nu} & 0 & \bar{\nu} & 0 \\
\bar{\nu} & 0 & 0 \\
\bar{\nu} & 0 \\
2\bar{\nu}
\end{bmatrix}
\]

Equation 59

Where: \(\hat{E} = \frac{E}{12(1-2\nu)(1+\nu)}\); \(\bar{\nu} = 1 - 2\nu\); \(\bar{\nu} = 1 - \nu\)
Now that the stiffness matrix is constructed, it is used to relate the nodal displacement vector \{u\} and the applied external nodal load vector \{F\}, expanded from Equation 53 into Equation 60.

\[
\begin{bmatrix}
F_{x1} \\ F_{y1} \\ F_{z1} \\ F_{x2} \\ \vdots \\ F_{z4}
\end{bmatrix} = [K] \begin{bmatrix}
u_{x1} \\ u_{y1} \\ u_{z1} \\ u_{x2} \\ \vdots \\ u_{z4}
\end{bmatrix}
\]

Equation 60

This is a boundary value problem (BVP), where some known forces and displacements are the necessary boundary conditions required for solution of the system of equations. For example, a support boundary at node 1 would take the form of \(u_{x1} = 0\), restricting displacement in the x direction. Once all of the nodal displacements are determined, then the internal strains can be calculated using Equation 61 (ANSYS, 2013; U. Colorado, 2011).

\[
\varepsilon = Bu
\]

Equation 61

The nodal displacement vector is expanded in Equation 62. Multiplying the displacements by the 6 \times 12 \{B\} matrix results in the 6 \times 6 internal strain vector \{\varepsilon\} defined in Equation 63.

\[
u^T = [u_{x1} \quad u_{x1} \quad u_{x1} \quad u_{x1} \quad u_{x1} \quad u_{x1} \quad u_{x1} \quad u_{x1} \quad u_{x1} \quad u_{x1}]
\]

Equation 62

\[
\varepsilon^T = [\varepsilon_{xx} \quad \varepsilon_{yy} \quad \varepsilon_{zz} \quad 2\varepsilon_{xy} \quad 2\varepsilon_{yz} \quad 2\varepsilon_{zx}]
\]

Equation 63

These strains are then used to find the stresses of the material by multiplying the strain vector by the 6 \times 6 isotropic elasticity matrix, Equation 64. This results in the stress vector defined in Equation 65.

\[
\sigma = E\varepsilon
\]

Equation 64

\[
\sigma^T = [\sigma_{xx} \quad \sigma_{yy} \quad \sigma_{zz} \quad \sigma_{xy} \quad \sigma_{yz} \quad \sigma_{zx}]
\]

Equation 65

These directional stresses, strains, and displacements are linearly interpolated within the element through the use of shape functions, \(N_i\). In this case, the shape functions are related to the nodal
coordinate values (i.e. \( N_i = \zeta_i \)). In order to determine the displacements at any point within the tetrahedron the relation in Equation 66 is used (ANSYS, 2013; U. Colorado, 2011).

\[
\begin{bmatrix}
1 \\
x \\
y \\
z \\
u_x \\
u_y \\
u_z
\end{bmatrix}
= 
\begin{bmatrix}
1 & 1 & 1 & 1 \\
x_1 & x_2 & x_3 & x_4 \\
y_1 & y_2 & y_3 & y_4 \\
z_1 & z_2 & z_3 & z_4 \\
u_{x1} & u_{x2} & u_{x3} & u_{x4} \\
u_{y1} & u_{y2} & u_{y3} & u_{y4} \\
u_{z1} & u_{z2} & u_{z3} & u_{z4}
\end{bmatrix}
\begin{bmatrix}
\zeta_1 \\
\zeta_2 \\
\zeta_3 \\
\zeta_4
\end{bmatrix}
\text{Equation 66}

Consider a simple line element with 2 nodes (Figure 33). The boundary conditions include a fixed support at node 1 and an externally applied load in the x direction at node 2. The displacement at node 2 can be explicitly solved with the Hooke’s law relationship, \( F = -kx \), where the spring constant \( k \) is analogous the linear elasticity found in the tetrahedron. In order to determine the displacements for all points along the element, a shape function is used for the interpolation. The line plot of displacement with respect to distance along x represents the internal displacement of the element. No displacement occurs at the fixed boundary at node 1 and the maximum displacement is found at node 2 (ANSYS, 2013; U. Colorado, 2011).

![Figure 33. A 2-node line element demonstrating the linearly interpolate displacements \( U_x \).](image)

Now that the stresses, strains, and displacements can be found for any point within the body, it is useful to combine these values into “Principle” and “Von Misses” stresses and strains. These scalar quantities offer utility in that they combine all of the directional stresses and strains into a
single parameter that can be compared to failure criteria. Failure criteria are simply the physical limits of stresses and strains that a given material can tolerate before failing (ANSYS, 2013).

The principal strains are determined by the eigenvalues of a cubic equation in the form of Equation 67, where the determinant equals 0. There will be three eigenvalues corresponding to the three principal strains: \( \varepsilon_1, \varepsilon_2, \& \varepsilon_3 \). Where \( \varepsilon_1 \) is the most tensile principal strain and \( \varepsilon_3 \) is the most compressive (directionally). The principle stresses \( \sigma_1, \sigma_2, \sigma_3 \) are determined in a similar fashion as shown in Equation 68 and are ordered from most tensile to most compressive (ANSYS, 2013).

\[
\begin{bmatrix}
\varepsilon_{xx} - \varepsilon_i & \frac{1}{2} \varepsilon_{xy} & \frac{1}{2} \varepsilon_{xz} \\
\frac{1}{2} \varepsilon_{xy} & \varepsilon_{yy} - \varepsilon_i & \frac{1}{2} \varepsilon_{yz} \\
\frac{1}{2} \varepsilon_{xz} & \frac{1}{2} \varepsilon_{yz} & \varepsilon_{zz} - \varepsilon_i
\end{bmatrix} = 0 
\]  
Equation 67

\[
\begin{bmatrix}
\sigma_{xx} - \sigma_i & \sigma_{xy} & \sigma_{xz} \\
\sigma_{xy} & \sigma_{yy} - \sigma_i & \sigma_{yz} \\
\sigma_{xz} & \sigma_{yz} & \sigma_{zz} - \sigma_i
\end{bmatrix} = 0 
\]  
Equation 68

Both the principle stress and principal strains (\( \sigma_1, \sigma_2, \sigma_3, \varepsilon_1, \varepsilon_2, \varepsilon_3 \)) are commonly used throughout structural analysis. However, for 3-D structural problems involving ductile materials, the Von-Mises stress and strain (or equivalent stress and strain) tend to be a more accurate parameter for predicting failure (Christensen, 2007). The Von-Mises strain and stress parameters are a combination of the three principal values into a scalar, defined by Equations 69 & 70 respectively (ANSYS, 2013).

\[
\varepsilon_e = \frac{1}{1+v} \left( \frac{1}{2} \left[ (\varepsilon_1 - \varepsilon_2)^2 + (\varepsilon_2 - \varepsilon_3)^2 + (\varepsilon_3 - \varepsilon_1)^2 \right] \right)^{\frac{1}{2}} 
\]  
Equation 69

\[
\sigma_e = \left( \frac{1}{2} \left[ (\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 \right] \right)^{\frac{1}{2}} 
\]  
Equation 70
4.4 Fluent Model Validation

4.4.1 Background

The modeling approach presented in this thesis considers the floating marsh mats as individual bluff bodies in the shape of rectangular cylinders (boxes with sharp corners). These mats are semi submerged within the water column and assumed to have minimal to negligible freeboard. The vast majority of bluff body flow research considers objects that are entirely immersed in the fluid medium (air or water) or extending from the bed with slight penetration of the free surface. Considering that floating marshes mats are not attached to the bed and entirely subject to the flow near the surface, experimental literature investigating flow around a “suspended” bluff body is required for proper comparison. Appropriate flume experimentation for this validation was not available at LSU, ergo published flume literature was chosen. The work performed by Arslan et al. (2013) is an in-depth turbulence study on flow around a semi-submerged rectangular cylinder bluff body. This paper contains experimentally measured and numerically predicted (LES) drag coefficients. LES models are more computationally expensive than k-ε models and are generally considered to be more accurate for predicting complex flows. This modeling approach utilizes the computationally efficiency of the k-ε model due to the multitude of cases being performed. This increase in computational efficiency sacrifices a degree of accuracy, therefore, quantifying this decrease in accuracy with an experimental and LES comparison is needed.

The experiment was conducted in a 5m x 0.5m flume channel with a 0.25m x 0.18m x 0.06m (transverse length, stream-wise length, height) rectangular cylinder was located 3.5m downstream of the inflow and 0.14m above the flume channel bottom (Arslan et al., 2013). The experimental geometry is presented in Figure 34. Drag coefficients were determined for various submergence
ratio’s (h*) between 0.2 and 0.9. h* is defined as the ratio of the draft of the cylinder to the total height (Equation 71).

\[ h^* = \frac{h_M - h_b}{s} \tag{Equation 71} \]

Where \( h_M \) is the depth of the water, \( h_b \) is the distance between the bottom of the cylinder and the bed, and \( s \) is the height of the cylinder.

Figure 34. Schematic of model domain. Iso-view (top), stream-wise view (bottom).
For the physical experiment, the bluff body is outfitted with two temporal force measuring devices called dynamometers (Figure 35). It is important to note that although the body spans the entire width of the flume (0.5m), the force measurements only take into account the central portion spanning 0.25m (d/2). These direct force measurements are used to calculate the drag coefficients using the submerged frontal face area and the inlet stream velocity. The flow is set at a Reynold’s number of 1.2x10^4 with s as the characteristic length, resulting in a flow velocity of 0.2 m/s. The inlet water levels are 0.152m, 0.164m, 0.172m, 0.188m, and 0.194m for h* values of roughly 0.2, 0.4, 0.6, 0.8, 0.9 respectively (Arslan et al., 2013).

![Figure 35. The experimental setup performed by Arslan et al 2013.](image)

The computational setup for the LES model used in the Arslan et al. (2013) work and the k-ε model in for this study have slight differences. The k-ε model was built to resemble the experimental setup, a 5m long flume with the bluff body 3.5m downstream of the inlet and a transverse width of 0.25m. The LES model places the bluff body 1.8m away from the inlet and uses a width of 0.5m. The LES model grid is also extremely refined in order to accurately predict the shedding vortices and wake regions (Figure 36). As mentioned previously, this thesis is not an in-depth investigation into the behavior of turbulence around a solid structure, thus a coarser grid
is implemented for the k-\(\varepsilon\) model and is refined so that drag forces can be predicted within a suitable range of error while minimizing computational cost.

Figure 36. The LES high-resolution grid implemented by Arslan et al. (2013).

For the k-\(\varepsilon\) model, a structured hexahedral grid is used with a smooth increase in resolution towards the air-water interface (Figure 37). This domain contains 794,209 elements with a resolution of 0.004m at the air-water interface and 0.019m at the top and bottom boundaries.

Figure 37. The k-\(\varepsilon\) grid with increased local grid resolution near the expected location of the air-water interface.
The LES model employs a Pressure Implicit with Splitting of Operator (PISO) scheme for pressure-velocity coupling (PVC), whereas the k-\(\epsilon\) model utilizes a SIMPLE PVC scheme (Arslan, et al 2013). Both models utilize implicit temporal schemes (bounded second order for k-\(\epsilon\)). The momentum, turbulent energy, & turbulent dissipation schemes are second order. The global courant number for the k-\(\epsilon\) model is 2 with variable time marching resulting in time steps of size 0.03s – 0.5s.

4.4.2 Results

Figure 38 depicts the water levels in the form of volume fractions for individual k-\(\epsilon\) cases, while Figure 39 shows the velocity profiles. Submergence ratios of 0.4, 0.6, 0.8, & 0.9 were simulated and drag coefficients were calculated using the same definitions used in Arslan et al. (2013). These transient simulations are run for 200s in order to provide enough time for the flow to reach steady state. The drag coefficients are averaged over a period of the final 10 time steps.

Figure 38. Water levels for cases 0.4(a), 0.6(b), 0.8(c), & 0.9(d). k-\(\epsilon\) model.
Figure 39. Stream-wise velocity profiles for cases 0.4(a), 0.6(b), 0.8(c), & 0.9(d). k-ε model.

The stream-wise velocity contours in Figure 39 are extracted from the centerline plane of the modeled flume. As the submergence ratio increases (increasing water level), larger velocities appear as higher flowrates are channeled underneath the bluff body. Larger submergence ratios lead to larger wake regions present behind the bluff body, which is the main source of the stream-wise drag force.

The validation results presented in Figure 40 show good agreement between drag coefficients produced by the k-ε model and the LES & experimental flume test. For a submergence ratio of 0.4 the k-ε model slightly over predicts the values obtained from the LES and flume test. For an h* of 0.6, the k-ε drag coefficients is in between the LES C_d value of 1.2 and the experimental C_d value close to 1.05 (experimental h* is 0.58). Larger submergence ratios of 0.8-0.9 yield slightly under predicted C_d values for the k-ε model with respect to flume experimental C_d values. Due to slightly differing values for h* (0.82 & 0.9 for experimental; 0.78 & 0.88 for k-ε), a linear interpolation
between data points is used for an estimation of error of the k-ε drag coefficients with respect to the experimental results.

Figure 40. Comparison between exp. data, an LES model, and the k-ε model (Arslan et al 2013).

For k-ε submergence ratios of 0.78 and 0.88 the k-ε under predicts the experimental results by 8.09% and 7.64% respectively. This level of agreement in drag coefficients for high submergence ratios is acceptable for further implementation of the k-ε model in the more complex scenarios of this study. Tabulated results from the validation study are presented in Table 6.

Table 6. Tabulated results from validation study (Including Arslan et al., 2013).

<table>
<thead>
<tr>
<th>Exp. h*</th>
<th>Exp. C_d</th>
<th>LES h*</th>
<th>LES C_d</th>
<th>k-ε h*</th>
<th>k-ε C_d</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.2</td>
<td>0.67</td>
<td>0.2</td>
<td>0.7</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>0.424</td>
<td>1.02</td>
<td>0.4</td>
<td>0.95</td>
<td>0.385</td>
<td>1.034</td>
</tr>
<tr>
<td>0.58</td>
<td>1.05</td>
<td>0.6</td>
<td>1.2</td>
<td>0.58</td>
<td>1.17</td>
</tr>
<tr>
<td>0.82</td>
<td>1.5</td>
<td>0.8</td>
<td>1.57</td>
<td>0.78</td>
<td>1.31</td>
</tr>
<tr>
<td>0.9</td>
<td>1.81</td>
<td>-</td>
<td>-</td>
<td>0.88</td>
<td>1.60</td>
</tr>
</tbody>
</table>
4.5 Fluent Setup

4.5.1 2-D & 3-D model Domain

In order to numerically model the various floating marsh scenarios, a flat-bed rectangular channel domain is used. In the case of 2-D, a channel slice is modeled for various lengths of rectangular mats. For 3-D, mats ranging in width are placed halfway along the test channel at the water surface and attached at one bank. General dimensions and nomenclature are defined in Figure 41.

Figure 41. General schematic of the model domain with defined nomenclature.
Where:

\( W_m \) = Mat width (bank to edge)

\( W_c \) = Channel width (bank to bank)

\( L_m \) = Stream-wise length of mat

\( L_c \) = Stream-wise length of Channel

\( D_m \) = Depth underneath mat

\( D_c \) = Depth of channel

\( T_m \) = Mat thickness (top to bottom)

**4.5.2 2-D Setup**

The goal of the 2-D simulations is to quantify the contribution of mat length \((L_m)\) on the stream-wise forces experienced by the floating marshes. It is too computationally expensive to simulate all mat shapes in 3-D for each velocity. Instead, a simple channel slice is constructed with five boundaries: Inflow, Outflow, Bed, Ambient (atmosphere), & the Mat. This geometry is in the form of the “bank view” illustration in Figure 41. In order to mitigate this, five mat lengths are evaluated in 2-D: 4m, 6m, 8m, 10m, & 12m. A general schematic with boundary labels is presented in Figure 42.

![Figure 42. Domain geometry schematic with boundary labels.](image)

The dimensions are as follows: channel length is 30m, channel depth is 3m, and mat thickness is 0.5m. The flow velocities tested are 0.5 m/s, 0.75 m/s, 1 m/s, & 1.25 m/s (1.75 m/s did not
achieve stability). The inlet is a mass flow rate inlet, the outlet and ambient are treated as pressure outlets, and the bed an mat surfaces are treated as no slip wall boundaries. For convenience, the setup with the location of the air-water interface is presented in Figure 43.

![Figure 43. Full domain with air-water interface (green). Air phase (red) Water phase (blue)](image)

The 2-D grids use a structured quad-dominant mesh with a uniform 0.1m resolution. For channel velocities of 0.75 m/s, 1 m/s, & 1.25 m/s, a local grid refinement of 0.05m was applied for the cells nearest the mat boundary (local grid refinement for the 0.5 m/s case did not yield stable results). The five case meshes are presented below in Figure 44. They are ordered from top to bottom by increasing mat length Lm.

![Figure 44. Five 2-D meshes ordered top to bottom (4m, 6m, 8m, 10m, 12m)](image)
These simulations utilize an implicit temporal scheme, with 2\textsuperscript{nd} order momentum, turbulent kinetic energy, and turbulent dissipation schemes. The total flow time for each case varies depending on duration needed to reach a steady state. The drag coefficients for each mat are recorded over time and averaged over a period of time steps to get a mean $C_d$.

4.5.3 3-D Setup

The test channel dimensions for 3-D analysis are as follows: channel length ($L_c$) is 30 meters, channel width ($W_c$) is 15 meters, and channel depth ($D_c$) is 3 meters. Being that the simulations make use of the VOF model, an ambient air phase is included in the domain with a height of 0.5 meters above the water surface bringing the total domain height to 3.5 meters. Figure 45 depicts the general model schematic developed in ANSYS Design Modeler with relevant dimensions and regions labeled.

![Model domain of a single mat case with dimensions and phase regions.](image)

Five separate mat shapes are directly modeled in the 3-D domain with varying mat widths ($W_m$): 4m, 6m, 8m, 10m, and 12m (Figure 46). Mat thickness ($T_m$) is fixed at 0.5m for all cases,
representing the upper limit of mat thickness found by Sasser et al. (1996). The justification being that the relatively large \( T_m \) will result in measuring the largest possible loads experienced by the floating marshes in nature due to the maximized frontal area \( (A_F = T_m \times W_m) \). This approach provides a conservative measurement until smaller mat thicknesses can be subsequently modeled. The mat length \( (L_m) \) modeled for the 3-D cases is set at 4m where the contribution of varying \( L_m \) on the drag force will be found using a 2-D approach.

![3-D model domain for the 5 floating marsh cases with varying \( W_m \). Top view (a) Isometric view (b) and stream-wise view (c)](image)

The five channels are treated as independent systems with separate inlets and outlets. The velocity within the domain is controlled by a mass flow inlet boundary condition upstream and pressure outlet downstream. The channel depth is determined by also setting a stage at both boundaries. The channel banks and mats are considered to be no-slip wall boundaries and the
ambient air pressure is controlled by a pressure outlet condition at top face. The boundary prescriptions are tabulated in Table 7. The turbulence intensity and turbulent viscosity ratio values were Fluent defaults.

Table 7. Boundary Attributes.

<table>
<thead>
<tr>
<th>Boundary</th>
<th>Type</th>
<th>Stage (m)</th>
<th>Turbulence Intensity (%)</th>
<th>Turbulent Viscosity Ratio</th>
<th>Wall Treatment</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inlet</td>
<td>Mass Flow Rate</td>
<td>3.01</td>
<td>5</td>
<td>10</td>
<td>-</td>
</tr>
<tr>
<td>Outlet</td>
<td>Pressure outlet</td>
<td>2.99</td>
<td>5</td>
<td>10</td>
<td>-</td>
</tr>
<tr>
<td>Ambient</td>
<td>Pressure outlet</td>
<td>-</td>
<td>5</td>
<td>10</td>
<td>-</td>
</tr>
<tr>
<td>Bed</td>
<td>Wall</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>No-Slip</td>
</tr>
<tr>
<td>Banks</td>
<td>Wall</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>No-Slip</td>
</tr>
<tr>
<td>Mats</td>
<td>Wall</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>No-Slip</td>
</tr>
</tbody>
</table>

The investigation into the impact of flow on the mats focuses primarily on the behavior of the flow near the air-water interface. Because floating marshes are buoyant, the surface velocities interacting with the mat will be the main parameter of interest in this study. Determining a relationship between surface velocities and hydrodynamic loads on the mat allows for simplification of the problem until further investigation of how varying channel parameters affects the outcome. The effect of varying bed roughness, wall roughness, and bank slope are not considered at this phase of research, therefore no-slip conditions are applied to the rectangular channel boundaries. The depth of 3 meters was chosen to minimize blockage effects in the vertical direction without extending the computational domain to an inefficient degree or beyond typical depths found in nature. Blockage ratios beyond 0.14 – 0.16 can slightly influence the drag coefficients for immersed bluff bodies (Chu et al 2016, West et al, 1982). The blockage ratio in the vertical direction is between 0.15 – 0.16 for these simulations.
In order to accurately capture the air-water interface, a local vertical refinement near the water surface is required. A computational grid study was conducted to: 1) optimize the grid for computational efficiency in terms of virtual flow time and HPC wall time (real time) and 2) determine the sensitivity of the vertical refinement on stability and the calculation of the drag coefficients. Figure 47 presents two sample grids with an air-water interface (near mat) local resolution of 0.0625m (a) and 0.02m (b).

Figure 47. Two sample grids of 0.0625m (a) and 0.02m (b) near mat vertical resolution.

A total of 5 grids were tested ranging from a coarse 0.16m resolution to a finer 0.02m resolution for a 4m x 4m x 0.5m mat (W_m x L_m x T_m). The drag coefficient was the chosen parameter for the comparison of results. Each case was run in transient until steady state was reached, ranging in duration from 695s – 1000s with variable time stepping. All numerical discretization schemes from the validation study were utilized (2nd order transient, momentum, turbulent energy, & turbulent dissipation). The boundary conditions from Table 7 were applied for a channel velocity of 1 m/s.

The transient simulations require a certain amount of time for a steady state to be achieved. The convergence of the drag coefficients is observed for the duration of the runs. The C_d’s are time
averaged over a period of 10s and marched, a moving average. The moving average is then compared to the final mean drag coefficients as a percentage. Figure 48 is a plot of this steady state evaluation.

![Graph showing 10s moving average as a percentage of the final mean drag coefficients.](image)

**Figure 48.** 10s moving average as a percentage of the final mean drag coefficients.

Through this process, oscillations occur due to the presence of small linear waves within the channel and vortices that typically develop in the wake of immersed bluff bodies (Malavasi et al. 2008). To quantify the amplitudes of the oscillations, the standard deviation (Stdev) of the drag coefficients is calculated over a duration of 10 seconds and taken as a percentage of the final mean drag coefficients. The oscillations should reduce over time as the simulation stabilizes, converging onto a steady state where a final mean drag coefficients can be determined (Figure 49). The coarsest mesh produced a poor convergence and reduced stability, likely resulting from the relatively low refinement of the water surface. When the air-water interface is not finely resolved,
the air volume fraction is inflated within regions that would otherwise be occupied by water. This will result in a reduced stream-wise drag force on the mat. All other meshes used by the simulations provided good stability.

Figure 49. 10s moving standard deviation as a percentage of final mean $C_d$.

In order to evaluate the rate at which each mesh in the grid study converges onto a mean $C_d$, the flow time required for both the moving average and moving Stdev to reach the threshold of 1% mean $C_d$ is recorded. The duration needed for steady state convergence positively correlated with mesh resolution (Larger resolution took longer to converge), however the duration needed for $C_d$ stability was inversely related (sans the 0.167m case which did not achieve stability). The results are presented in Table 8 along with the computation time.
Table 8. Parameters for mesh computational efficiency and convergence rate.

<table>
<thead>
<tr>
<th>Vertical Resolution [m]</th>
<th>Flow Time [s]</th>
<th>Time Steps #</th>
<th>Wall Time [s]</th>
<th>1% Stdev/ Final Mean Cd [s]</th>
<th>1% Average/ Final Mean Cd [s]</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.167</td>
<td>1000</td>
<td>4333</td>
<td>6829</td>
<td>-</td>
<td>650</td>
</tr>
<tr>
<td>0.0714</td>
<td>1000</td>
<td>4604</td>
<td>22079</td>
<td>138</td>
<td>266</td>
</tr>
<tr>
<td>0.0416</td>
<td>1000</td>
<td>6698</td>
<td>11849</td>
<td>261</td>
<td>187</td>
</tr>
<tr>
<td>0.0263</td>
<td>835</td>
<td>7000</td>
<td>39233</td>
<td>285</td>
<td>162</td>
</tr>
<tr>
<td>0.0208</td>
<td>695</td>
<td>7000</td>
<td>27562</td>
<td>413</td>
<td>155</td>
</tr>
</tbody>
</table>

Figure 50 indicates that a near mat vertical resolution (NMVR) < 0.05m has minimal influence on the change of $C_d$. Furthermore, Figure 51 shows that increasing the number of elements for a given channel domain beyond 1.8e5 does not significantly impact the final result. The plot data is presented in Table 9 for convenience. The impact of the near mat vertical resolution and element count on computational efficiency is further evaluated using a 10s moving standard deviation approach of $C_d$ over time.

![Figure 50. Drag coefficients vs near mat vertical resolution.](image-url)
Figure 51. Drag coefficients vs # of elements.

Table 9. Relevant parameters for grid resolution study.

<table>
<thead>
<tr>
<th>Mat Vertical Layers</th>
<th>Near Mat Vertical Res. [m]</th>
<th># of Elements</th>
<th>Cd</th>
</tr>
</thead>
<tbody>
<tr>
<td>3</td>
<td>0.167</td>
<td>52218</td>
<td>0.89</td>
</tr>
<tr>
<td>8</td>
<td>0.0714</td>
<td>124542</td>
<td>1.12</td>
</tr>
<tr>
<td>13</td>
<td>0.0416</td>
<td>186522</td>
<td>1.15</td>
</tr>
<tr>
<td>20</td>
<td>0.0263</td>
<td>236214</td>
<td>1.15</td>
</tr>
<tr>
<td>25</td>
<td>0.0208</td>
<td>288144</td>
<td>1.14</td>
</tr>
</tbody>
</table>

With parallel calculation utilizing 32 processors, the wall times & number of time steps for each simulation are compared. Of the 4 stable cases, the 0.0416m resolution grid required the least amount of wall time. Taking into account that a NMVR of 0.0416m lies beyond the grid independence threshold of <0.05m NMVR and produces a stable $C_d$ measurement in the shortest flow time (of cases <0.05m resolution), it was determined that grids within 0.05m>NMVR>0.0416m are suitable. Moreover, the small amount of additional flow time needed for the moving average $C_d$ to converge towards the final mean $C_d$ did not offset the computational cost.
From here, the full domain containing the 5 channels with varying mat sizes was constructed. The mesh properties are outlined in Table 10 and Figure 52 shows the grid. Mesh orthogonality and skewness are quality metrics that quantify the warping of the elements relative to a perfectly equilateral element. An orthogonality value close to 1 and a skewness value close to 0 indicate good quality (ANSYS, 2013).

Table 10. Full domain mesh characteristics

<table>
<thead>
<tr>
<th># of Nodes</th>
<th># of Elements</th>
<th>Method</th>
<th>Orthogonality</th>
<th>Skewness</th>
<th>NMVR [m]</th>
</tr>
</thead>
<tbody>
<tr>
<td>6611755</td>
<td>1591840</td>
<td>Hexahedral</td>
<td>1</td>
<td>1.70E-06</td>
<td>0.045</td>
</tr>
</tbody>
</table>

Figure 52. Full domain mesh. Iso-view (a), Iso-view zoom (b), Bank view with mat outline (c)
4.6 ANSYS Structural Setup (FEA)

The structural domain consists of 25 individual floating marsh mats. The mats are arranged in five sets of five, sorted by A.R.. Referencing Figure 53, the rows range from 4m to 12m mat width, \( W_m \), (top to bottom) and the columns range from 12m to 4m mat length, \( L_m \), (left to right). Future notation will appear in the format of \( W_m \cdot L_m \cdot T_m \). For instance, the top left corner mat can be referred to as “4.12.05”. As mentioned previously, this initial analysis only considers a \( T_m \) of 0.5m.

![Figure 53. The floating marsh mat aspect ratios used for FEA.](image)

The general approach of the material analysis is accomplished by extracting the net stream-wise force acting on the bodies from the Fluent results, applying the boundary conditions (fixed support at the bank, net stream-wise load applied at the front face), and evaluating the von-Mises stress at the bank connection. The material properties of the mats are taken from the sod tensile strength performed with the MMTST (Y.M. & yield stress, shown in section 3.2) and values from literature (Poisson’s ratio for saturated soils) until direct field measurements can be performed (Cloete, 2003). Due to the nature of the problem, the stress along the bank is not uniform. A
significant stress concentration occurs at the leading edge of the bank, referred to as the “corner stress”. The stress at this area of the mats is the main focus of the analysis and requires special treatment when meshing.

Considering the geometry of the idealized mats (rectangles with sharp corners) and the presence of an edge where the applied boundary conditions meet (Figure 54), a development of a stress singularity is investigated. Stress singularities are unrealistic values of stress that result from the ever-decreasing area (i.e. increasing grid resolution) through which a distributed load is applied (Sinclair, 2004). They are not physical, rather, a symptom of the numerical analysis.

![Figure 54. Location of B.C.’s. Stream-wise distributed load (red) & fixed support (purple)](image)

It is common for stress singularities to form in the presence of sharp corners where multiple loads meet at a point. In most instances, they occur far enough away from the area of interest to affect the result (Toupin, 1965). However, the area of interest in this analysis is the stress concentration at the leading edge of the bank where a stress singularity appears. A mesh resolution study was conducted with the stress singularity in mind, as well as a remediating solution.

Consider a 4.4.05 mat with a coarse structured mesh (Figure 55). There is a stress concentration at the corner, which is physically expected. However, as the mesh resolution is increased, the
corner von-Mises stress does not converge towards a grid independent value. Applying five distributed loads ranging from 200 N to 4395 N and steadily increasing the resolution, the divergent behavior of a stress singularity can be observed (Figure 56).

![Figure 55. Coarse mesh for 4.4.05 mat showing corner stress concentration.](image)

![Figure 56. Visualization of the divergent stress singularity vs resolution.](image)
To mitigate this issue, the corner is smoothed with a fillet (Figure 57). From here, the mats are discretized into an unstructured mesh with increasing resolution near the aforementioned corner of interest. The corner von-Mises stress was measured at the fillet and at the location of a nearby stress singularity for various resolutions to distinguish the physical results from the unrealistic (Figure 58).

Figure 57. Location and shape of fillet on leading edge of bank (4.4.05 mat)

Figure 58. Comparison between von-Mises stress at fillet (dark) and nearby singularity (light)
The corner von-Mises stress at the fillet converges onto a grid independent solution at resolutions <0.01 m. This behavior is in contrast to the stress value measured at a nearby singularity which continues to diverge as the resolution changes (light lines in Figure 58). The number of layers in the T_m direction does not significantly impact the result. A T_m resolution of 0.125m is chosen for computational efficiency. The discretized grid for all 25 mats is constructed using a corner resolution of 0.005m (Figure 59) Various mesh statistics are tabulated in Table 11.

![Figure 59. Constructed mesh for all 25 floating marsh mats.](image)

<table>
<thead>
<tr>
<th># Nodes</th>
<th># Elements</th>
<th>Method</th>
<th>Orthogonality</th>
<th>Skewness</th>
</tr>
</thead>
<tbody>
<tr>
<td>275091</td>
<td>85224</td>
<td>Multi-zone Prism</td>
<td>0.96904</td>
<td>0.125</td>
</tr>
</tbody>
</table>

For convenience, the grids for three individual mats are presented in Figure 60. Mats 4.12.05 (a), 12.12.05 (b), & 12.4.05 (c) are shown with the localized resolution increase at the corner of interest. The top boundary of each mat is fixed which represents the mat connection to a solid
bank. The net stream-wise loads are applied as evenly distributed pressures along the front face and are increased incrementally.

Figure 60. Sample meshed mat bodies. 4.12.05 (a), 12.12.05 (b), 12.4.05 (c).

The max corner von-Mises stresses are measured at the fillet for each incremental load applied. As the stream-wise load is increased, the mat undergoes deformation and experiences internal stresses. The load is recorded where the von-Mises stress exceeds a failure criterion ($F_c$).

Figure 61. Sample von-Mises contour showing stress concentration at corner exceeding $F_c$. 76
5. Results

5.1 Fluent

The 2-D simulations were run for 5 mat lengths ($L_m$ from 4m to 12m) to determine a relationship between mat length and the relative increase in drag coefficients from the 4m case. This increase in the drag coefficients is due to the increase in contact area underneath the mat. Figure 62 shows the velocity contours for an approach velocity of 0.75 [m/s] for $L_m$’s of 4m (a), 8m (b), & 12 (c). The wake region behind the mats does not significantly extend as $L_m$ increases. The flow pattern at the front face of the mat is also unaffected. Therefore, it can be concluded that the frontal area is the dominant feature for $C_d$.

![Stream-wise velocity contours for $L_m$ of 4m (a), 8m (b) & 12m (c).](image)

The relative increase in drag coefficients due to an increase in mat length, $L_m$, is quantified as a percentage increase in $C_d$ (from $L_m$=4m case) per change in mat length. The data are fitted to a linear trend ($C_d$ vs $L_m$) and an average slope is determined (Figure 63). Table 12 presents the slope, $R^2$, and percent change per meter $L_m$ for each 2-D case. There is a high mean “goodness” of fit for
the linear regressions as the average of the $R^2$ values is greater than 0.9. The shift in drag coefficients with respect to approach velocity does not appear to have a clear trend. However, the contribution of $L_m$ to drag force has a consistent linear increase with mat length between $1 - 1.2\%$ per meter $L_m$ added. The values for 1.75 m/s was not included as a sufficiently stable result was not reached.

![Graph showing drag coefficients change with respect to mat length](image)

**Figure 63.** Drag coefficients change with respect to mat length ($L_m$). (2-D)

**Table 12.** Tabulated results for $C_d$ change with $L_m$.

<table>
<thead>
<tr>
<th>Velocity (m/s)</th>
<th>Slope</th>
<th>$R^2$</th>
<th>$% C_d/m$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5</td>
<td>0.0147</td>
<td>0.8017</td>
<td>1.119</td>
</tr>
<tr>
<td>0.75</td>
<td>0.0124</td>
<td>0.955</td>
<td>1.026</td>
</tr>
<tr>
<td>1</td>
<td>0.0138</td>
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</tr>
<tr>
<td>1.25</td>
<td>0.0138</td>
<td>0.997</td>
<td>1.142</td>
</tr>
<tr>
<td>Mean</td>
<td>0.0136</td>
<td>0.918</td>
<td>1.121</td>
</tr>
<tr>
<td>Stdev</td>
<td>0.0008</td>
<td>0.073</td>
<td>0.091</td>
</tr>
</tbody>
</table>

Direct simulations of the five 3-D cases (4.4.05, 6.4.05, 8.4.05, 10.4.05, & 12.4.05) are used as the base level mats. These cases all have an $L_m$ of 4m, from which the percent increase in $C_d$
due to mat length is applied using the 2-D simulated contribution of $L_m$ (1.121% per m). This process results in the 25 unique drag coefficients for each 3-D test velocity (0.5, 0.75, 1, 1.25, & 1.75 m/s), 125 drag coefficients in total (Table 13).

<table>
<thead>
<tr>
<th>Wm [m]</th>
<th>V [m/s]</th>
<th>Re</th>
<th>Cd</th>
<th>Lm [m]</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5</td>
<td>2.00E+06</td>
<td>1.125</td>
<td>1.149</td>
<td>1.173</td>
</tr>
<tr>
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<td>1.177</td>
</tr>
<tr>
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<td>1.174</td>
<td>1.198</td>
</tr>
<tr>
<td>1.25</td>
<td>5.00E+06</td>
<td>1.179</td>
<td>1.203</td>
<td>1.227</td>
</tr>
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<td>7.00E+06</td>
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<td>1.257</td>
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<td>0.75</td>
<td>4.50E+06</td>
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<td>1.214</td>
<td>1.238</td>
</tr>
<tr>
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<td>6.00E+06</td>
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<td>1.315</td>
</tr>
<tr>
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<td>1.572</td>
<td>1.596</td>
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</tbody>
</table>

In order to relate the drag coefficients to the approach velocity and mat A.R., a regression with Reynolds number was used to determine a trend. Figure 64 illustrates the significant impact of mat width on the wake region behind the mats. For this reason, $W_m$ is a suitable dimension for the characteristic length in the formulation of Re values. A similar analysis performed by ALDEN
labs involving floating vegetative mats also used mat width as the characteristic length, providing some degree of verification for this determination (ALDEN, n.d.).

Figure 64. Sample stream-wise near surface velocity contours for 3 mat aspect ratios. Approach velocity 0.75 m/s (left) & 0.5 m/s (right).

Individual $C_d$ vs Re relationships were constructed for each mat length, resulting in five 2nd order polynomial regressions (Figure 65) with $R^2$ values of 0.958. The stratification of the five relationships is simply due to the added contribution of $L_m$ to the drag coefficients. This “$L_m$ correction” (described previously as a percentage increase in $C_d$) is represented by the polynomial intercepts. A general equation for $C_d$ (Equation 72) is formulated by incorporating the mat Re number (Equation 73) and the linear $L_m$ correction (Figure 66) as a parameter “$\alpha$” (Equation 74). The drag coefficients are increasing (at a decreasing rate) with Reynolds number. This $C_d$ behavior is typical among a range of geometrical shapes for $Re > 5e5$ (Tritton, 1988).
Figure 65. Drag coefficients vs Reynold’s number for stratified by $L_m$ (4m – 12m).

Figure 66. The linear $L_m$ correction
\[ C_{d,m} = -3E(-16)(Re_m^2) + 3E(-8)(Re_m) + \alpha \]  
Equation 72

\[ Re_m = \frac{v_{approach} W_m}{v} \]  
Equation 73

\[ \alpha = 0.0121 L_m + 0.9723 \]  
Equation 74

The general equation for the floating marsh mat drag coefficients \((C_{d,m})\) is plotted vs Reynold’s number and \(L_m\) in Figure 67. Estimated values for \(C_{d,m}\) are tabulated in Table 14. The errors between these values and \(C_d\)’s extracted from the Fluent results are evaluated in Table 15.
Table 14. \(C_{d,m}\) values estimated by the general relationship: Equation 72

<table>
<thead>
<tr>
<th>Wm [m]</th>
<th>V [m/s]</th>
<th>Re</th>
<th>4</th>
<th>6</th>
<th>8</th>
<th>10</th>
<th>12</th>
</tr>
</thead>
<tbody>
<tr>
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<td>1.236</td>
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<td>1.187</td>
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<td>1.236</td>
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</table>

Table 15. Errors between the Fluent values and the predicted values from Equation 72.

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<tr>
<th>V [m/s]</th>
<th>% error</th>
<th>Lm [m]</th>
<th>% error</th>
<th>Wm [m]</th>
<th>% error</th>
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<tr>
<td><strong>Total Mean % error</strong></td>
<td><strong>Total % error Stdev</strong></td>
<td><strong>Total RMSD</strong></td>
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</tbody>
</table>
The overall mean error between the two data sets is 2.33%. Approach velocities of 0.5 m/s and 1.75 m/s produced the largest mean errors of 2.83% & 2.73% respectively. An approach velocity of 1.25 m/s yielded the best prediction being the only value below the total mean % error. The $L_m$ % errors decreased monotonically with mat length, though this is likely a consequence of using the linear $L_m$ correction estimated from the 2-D simulations. Mat width values of 4m, 8m, and 12m produced % errors below the total mean. Furthermore, the best $C_{d,m}$ prediction for a given mat was for the 4.12.05 case with a mean error of 1.86 %, while the greatest mean error of 2.81% was for the 6.4.05 case (less than 0.5 Stdev away from the total mean % error). More specifically, the 6.4.05 case for an approach velocity of 1.25 m/s ($Re = 1.05e7$) produced the largest overall error value of 4.25%. The lowest overall error was 0.22% for the 10.10.05 & 10.12.05 cases with an approach velocity of 1.25 m/s ($Re = 1.25e7$). Based on these error values for prediction of $C_{d,m}$ using Equation 72, the proposed general formula for estimating drag force on the various mats is sufficient enough for further use in this study. The stream-wise force vs approach velocity profiles for the 25 mat aspect ratios is plotted in Figure 68.

![Figure 68. Stream-wise force vs approach velocity for the 25 mat A.R.’s. Grouped by $W_m$.](image-url)
The force profiles are grouped by $W_m$ ranging from 4m (Dark blue) to 12m (light blue). The slight stratification in the $W_m$ groups pertains to the value of $L_m$, where the longest mats experience the highest stream-wise forces of the group. These forces will be hydraulic loads applied to the front face boundary in ANSYS static structural where various failure criteria will be evaluated.

5.2 ANSYS Static Structural

25 floating marsh mats were constructed in ANSYS Static Structural FEA solver. The mats were individually meshed with increasing resolution near the leading corner connected to the bank (Figure 60). A fixed support boundary condition is applied along the bank (along $L_m$) and the stream-wise force is applied as uniform pressure at the front face (along $W_m$). The load is incrementally increased and the corner von-Mises stress is recorded for each increment. This process was repeated for various Y.M. values to observe the effect on displacement (Figure 69).

Figure 69. von-Mises stress contour at failure for 12.4.05 (left) & 4.12.05 (right) for two Y.M.
The two mats in Figure 69 are super imposed with results containing separate Y.M. values from the sod strength test (mean = 31945 Pa, -1 Stdev = 25512 Pa). The stresses within the mats were not affected by the change in Y.M. (static model), however a slight change in the deflection can be seen. The 12.4.05 case deflects stream-wise which effectively “shortens” frontal area normal to the approaching flow. Conversely, the 4.12.05 case appears to extend in the transverse ($W_m$) direction. These variations caused by different Y.M. values may alter the $C_d$ for 12.4.05 (reduced drag) and 4.12.05 (increased drag). That being said, the full impact of Y.M. cannot be fully appreciated at this stage of research. A more dynamic or a 2-way fluid-structure interaction (FSI) model is needed for further analysis in this area. There is a clear stress concentration near the leading edge of the bank, as expected (Figure 70).

![Figure 70. Corner von-Mises stress zoom for 12.4.05 showing concentration.](image)

The assumed mode of failure is a “ripping” process. As the stress concentration exceeds a failure threshold, that portion of the mat becomes separated from the bank. Once this area disconnects, the stress on the remaining bank connection is now applied through a reduced cross-sectional area, amplifying the stress concentration even further. Each mat A.R. has a unique
relationship between the applied load and corner von-Mises stress. This relationship, in conjunction with an estimated failure criterion, is then used to determine a critical velocity ($V_c$). The corner von-Mises stress vs approach velocity profiles are plotted in Figure 71. The mat A.R. has a profound effect on the velocity required for failure, the mats with larger $W_m/L_m$ aspect ratios appear to fail sooner (generally) than mats with lower aspect ratios. The larger the frontal areas induce stronger moments around the bank connection while larger $L_m$'s provide more supportive counter moments. For example, the two cases depicting failure in Figure 69 have different approach velocities (12.4.05 at 0.5m/s, 4.12.05 at 1.5m/s) for a failure criterion of 11513 Pa.

![Figure 71. Corner von-Mises stress [Pa] vs approach velocity [m/s] for 25 mat aspect ratios.](image)

The four failure criterion ($F_c$) used for development of an A.R.-$V_c$ relationship are: 11513 Pa, 9583 Pa, 7653 Pa, & 4468 Pa (four lines in Figure 71). The order of failure (ranked from 1 to 25) is plotted in Figure 72 with respect to the mat aspect ratio [$W_m/L_m$]. There is clear trend indicating that a higher aspect ratio will lead to a relative increase in the likelihood of a mat
failing. That being said, it is not a 1-to-1 relationship between $W_m$ and $L_m$. The length by which the mat extends into the channel contributes more to mat failure than the support provided by an equal $L_m$.

To investigate the proper ratio between $W_m$ and $L_m$, a modified aspect ratio $\beta$ (Equation 75) is constructed and regressed with $V_c$ for the four $F_c$ (Figure 73). The parameter “$k$” is varied until an optimal regression fit is achieved for each $F_c$ tested (Figure 74). The highest average $k$ for the four curves was found to be 1.625 leading to a general expression for $V_c$, in the form $V_c = f(\beta, F_c)$. This formulation is a natural log relationship between $V_c$ and $\beta$ (Equation 76) that is stratified by $F_c$ via the coefficients $\phi$ & $C$ (Equation 77 & 78). The optimized formula for the modified aspect ratio $\beta$ is presented in Equation 79 for clarity. The velocity required for mat failure can now be directly estimated from mat A.R. given a measured $F_c$. 

\[
\beta = \frac{W_m^k}{L_m} \quad \text{Equation 75}
\]
Figure 73. Relationship between modified aspect ratio $\beta$ and $V_c$ for different $F_c$.

Figure 74. Goodness of fit vs $k$ for each $F_c$. 

$y = -0.31 \ln(x) + 1.6616$
$R^2 = 0.9744$

$y = -0.284 \ln(x) + 1.5256$
$R^2 = 0.9722$

$y = -0.255 \ln(x) + 1.3814$
$R^2 = 0.9716$

$y = -0.204 \ln(x) + 1.077$
$R^2 = 0.9518$
\[ V_c = \varphi \ln(\beta) + C \]  

Equation 76

\[ \varphi = 2E(-10)F_c^2 + 2E(-5)F_c - 0.1565 \]  

Equation 77

\[ C = 0.021F_c^{0.4561} \]  

Equation 78

\[ \beta = \frac{W_m^{1.625}}{L_m} \]  

Equation 79

The performance of Equation 76 was evaluated with a RMSD of the \( V_c \) vs \( \beta \) relationships (varied by \( F_c \) (Table 16). The RMSD generally decreased with \( F_c \), though this is due to the decreased \( V_c \) values intrinsic to lower \( F_c \)’s. To more accurately capture the performance of each line, the RMSD was normalized with the average \( V_c \) value for each curve. The normalized values are presented as percentages and indicate that the best performance was for an \( F_c \) of 9583. The NRMSD generally increased with decreasing \( F_c \) which indicates that the model is slightly less accurate for lower \( F_c \) values. The total mean percentage error for all \( V_c \) values with respect to raw values is 3.33% with a standard deviation of the percent error of 2.71%.

<table>
<thead>
<tr>
<th>Fc [ Pa]</th>
<th>11513</th>
<th>9583</th>
<th>7653</th>
<th>4468</th>
<th>Mean</th>
</tr>
</thead>
<tbody>
<tr>
<td>RMSD [m/s]</td>
<td>0.0087</td>
<td>0.0068</td>
<td>0.0075</td>
<td>0.0061</td>
<td>0.0073</td>
</tr>
<tr>
<td>NRMSD %</td>
<td>0.84</td>
<td>0.71</td>
<td>0.86</td>
<td>0.91</td>
<td>0.83</td>
</tr>
<tr>
<td>Total mean % error</td>
<td>3.33</td>
<td>Total mean % error Stdev</td>
<td>2.71</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

The best overall prediction of \( V_c \) was for the 6.10.05 case (\( F_c = 9583 \) Pa) with an error of 0.045% while the worst prediction was for 10.4.05 (\( F_c = 4468 \)) with 11.06% error. 83% of the predicted \( V_c \) values are below the +1 standard deviation of the total mean percentage error (6% error), & 61% are below the total mean percentage error (3.33%). These errors likely result from applying a singular \( k \) for all \( F_c \)’s. Nonetheless, the ability of Equation 76 to predict the critical velocities based on the mat A.R. is sufficient enough for further analysis.

Lastly, in order to estimate the amount of acreage loss for a floating marsh wetland, a
cumulative distribution approach for washout is proposed. As a demonstration, five arbitrary wetlands are analyzed, each with a unique distribution of mat sizes binned into groups by the modified aspect ratio, $\beta$. The summed acreage of the mats contained in each bin are converted into percentages of total wetland area as shown in Figure 75. The $W_m$ and $L_m$ values range from 4m to 12m and the $\beta$ values range from 0.79 to 14.17. For the unimodal distribution wetland, 50% of the acreage has a $\beta$ value between 5 & 9. All mat size bins for the uniformly distributed wetland possess the same amount of acreage. 50% of the low $\beta$ skewed wetland acreage has a $\beta$ value of less than 4 while 50% of the high $\beta$ skewed wetland acreage has a $\beta$ value greater than 11. The bimodal distribution wetland contains two groups where 45% of the total acreage have $\beta$ values greater than 11 and less than 4. These various distributions can be transformed into washout percentages via conversion of the binned $\beta$ values into critical velocities using a failure criterion.

![Figure 75. Various potential $\beta$ distributions for a given floating marsh wetland area.](image-url)
A cumulative distribution function is applied to the set of binned $V_c$’s to achieve the resulting washout percentage plot shown in Figure 76. The applied $F_c$ value for this example is 9583 Pa.

![Figure 76. Percentage of floating marsh acreage loss due to washout based on $\beta$ distributions.](image)

The five arbitrary floating marsh wetlands have distinct washout regimes graphed in Figure 76 and tabulated in Table 17. The sets containing more high-$\beta$ acreage will be less resilient to increased flow velocities than sets containing more relatively low-$\beta$ acreage.

Table 17. Tabulated results for the acreage washout percentages vs velocity (Figure 76).

<table>
<thead>
<tr>
<th>Percent Acreage Loss</th>
<th>Velocity [m/s]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Unimodal</td>
</tr>
<tr>
<td>10</td>
<td>0.56</td>
</tr>
<tr>
<td>25</td>
<td>0.66</td>
</tr>
<tr>
<td>50</td>
<td>0.74</td>
</tr>
<tr>
<td>75</td>
<td>0.84</td>
</tr>
</tbody>
</table>

This approach may be advantageous upon further improvements and expansion to the study as it only requires the horizontal measurements of the mats within a wetland area. These values ($W_m$ &
L_m) can be found remotely using satellite imagery, leading to a more easily reached estimation of the vulnerability of floating marsh wetlands. Constructing floating marsh washout in the form of % land loss vs velocity provides a simplified means for presenting the predicted outcomes of such an event. Complementary cumulative distributions have been utilized in the field of ecology in the form of survival functions and could have applicability in this area upon subsequent investigation into the fatigue of floating marshes during a washout event (Muenchow 1986). Additionally, it may be possible to retroactively determine the characteristic velocities through a given wetland by examining the floating marsh mat shapes. Theoretically, the highest β values from the set of mat sizes would correspond to the highest velocities intrinsic to that particular wetland. Further discussion of the results and suggestions for improvement are presented in the next section.
6. Conclusions and Recommendations

6.1 Conclusions

The ultimate objective for this thesis was 2-pronged: design an in-situ method for quantifying the material properties of floating marshes (Young’s modulus, yield strength, ultimate strength) & develop a means for prediction of floating marsh washout (critical velocities). The tensile strength device (MMTST) was fabricated out of cheap, lightweight, waterproof materials and designed to be functionally operated by one user in the field. The range of values measured by the MMTST (within 1 Stdev) was 38.377 – 25.512 kPa for Y.M. 11.347 – 8.4815 kPa for U.S., and 11.513 – 7.653 kPa for Y.S. The device performed reasonably well for determining the ultimate tensile strength of a vegetative “material” (sod) when compared to the small amount of available published data (Giese, 1997; Heckman et al., 2001). However, little is known about the full tensile stress-strain profile of a root-soil matrix like sod or floating marshes, making a true validation of the device’s ability to measure Y.M. and Y.S. for future work. The literature describing root-soil strength properties primarily focus on shear strength and uprooting resistance.

The proper determination of a failure criterion is critical to the estimation of floating marsh survivability. A common engineering approach in evaluating the failure of homogenous ductile materials is to identify the yield point stress. Unfortunately, a true evaluation of the tensile strength of floating marshes could not be achieved in the duration of this study due to permitting issues involved with the proposed study site. Therefore, the sod strength data was used as a proxy in the development of the methods and results of this thesis. The sod tensile strength tests showed that the average strain value for yield stress was 0.3. All of the sod samples either completely failed at this point or transitioned into plastic deformation. Additionally, the U.S. range of 11.347 kPa – 8.4815 kPa (1 standard deviation) falls within the range of Y.S. values used for the failure criterion,
allowing for the application of either in determination of $F_c$. Estimation of strength properties in the case of vegetation is extremely difficult due to level of variation among “biological” materials. This variability is compounded by the multiple modes of failure present in a root-soil matrix (slippage & root fracture). To capture the full range of root-soil matrix bulk tensile strength properties, a larger sample size is needed.

Prediction of the potential hydrodynamic loads on the floating marsh mats required 2-D and 3-D modeling performed with ANSYS Fluent. The dimensions of mat width, $W_m$, and mat length, $L_m$, were prioritized at this stage of the study (fixed mat thickness, $T_m$) as they have the most impact on the planar response of the floating marsh structure (horizontal plane). The mat thickness of 0.5m (largest natural mat thickness) was chosen to provide a conservative estimate on the forces impacting the marsh structure (largest forces). That being said, there are multitudes of possible orientations, geometries, and arrangements that need to be considered for a more accurate picture of how likely floating marshes are to washout. The mats considered in this thesis are highly idealized as perfect rectangular prisms with a single angle of attack (Flow normal to mat surface, highest force). Mats with tapered or rounded geometries experiencing flows from different approach angles and depths may drastically expand the range of critical velocities. Altering these parameters undoubtedly adds orders of magnitude more complexity and time required for analysis. Therefore, these geometrical and channel flow constraints were imposed to achieve a conservative baseline conclusion that can be expanded upon with subsequent study.

The successful performance of the numerical models was highly contingent on the control of the numerical instabilities in the form of waves. In order for the 3-D VOF model to accurately predict the forces on the mat, the air-water interface needs a certain level of refinement to minimize the “smoothing” transition between phases. Coarser vertical resolution near the interface can lead
to slight under-prediction of the drag force. In the smoothing zone, elemental volume fraction contains a portion of both phases. If the air phase is over represented in the element, it will lead to lower impact forces on the boundary. For the 2-D VOF simulations, increasing the vertical resolution led to higher instabilities in the model. Because of this issue, the vertical refinement near the air-water interface was reduced to achieve model stability. Although this may have come at a cost of reduced accuracy on front face drag force estimation, the focus of the 2-D simulations was on the contribution of mat length to drag force. The boundary elements on the underside of the mats were fully immersed with no air phase pollution. Instead of vertically resolving the mesh for the entire water surface (to avoid instability), an inflation layer was added to the mat boundary elements. The near wall resolution was reduced from 0.1m to 0.05m. This reduced the predicted drag coefficients for the mats, however, it did not have a significant impact on the average slope of $C_d$ vs $L_m$, which was the goal of the 2-D analysis. It is important to note: 1) stability for the 2-D 1.75 m/s case was not achieved, therefore the average slope from the other four 2-D cases was assumed for the 1.75 m/s case. 2) The 0.05m inflation layer was only applied to the 0.75m/s, 1m/s, and 1.25m/s cases as the 0.5m/s and 1.75m/s cases did not achieve sufficient stability at this resolution. The 0.5m/s case is only resolved to 0.1m at the mat wall boundary. Further investigation into the grid resolution effects on the 2-D mats is needed.

The goal of the 3-D simulations was to measure the effect of $W_m$ on the drag coefficients. Mat width had the largest impact on the drag forces imposed on the mats. The pressure components of the drag contribute >90% of the total mat drag force relative to the viscous forces from the surfaces underneath and on the sides (Table 12). The 3-D simulations are constrained to one channel geometry with varying flow rates, acting as a large “virtual flume”. This thesis focused on the influence of surface approach velocities (away from bed) on mat drag force to allow for the broader
application of the results to other geometrical situations with similar (but not exact) dimensions. The limits of this applicability are not known, especially for cases with extremely small depths where boundary effects at the bed and blockage ratio play a larger role. The channel width was also fixed (to conserve computational effort) which may have led to some blockage for the larger mat widths. An analysis of this blockage should be performed to determine a non-dimensionalized relationship between blockage ratio (Wm/Channel width) and drag coefficients. Varying bed slope and roughness were not evaluated. Coastal channel and wetland beds are relatively flat, especially on length scale for the mats modeled in this study.

A clear trend between the drag coefficients and Reynold’s number was found using Wm as the characteristic length. Different combinations of Tm and Lm for the length scale did not yield better results. Therefore, individual Cd vs Re relationships were constructed for each Lm and a general formula for estimating drag was constructed in form of \( Cd = f(W_m, L_m, V) \). The average percent error between the calculated Cd’s and the simulated Cd’s was 2.33%. The limitation of this formula results from the fixing of Tm. The effect of smaller mat thicknesses on drag coefficients should be investigated further.

The finite element analysis on the floating marshes as a material revealed a clear connection between horizontal mat aspect ratio and flow conditions required for failure. Varying Y.M. did not have an effect on the internal stresses of the mat, however it did alter the deflection of the marsh. A 2-way fluid structure interaction model may better capture the effect of Y.M. on the outcome of the critical velocity values. Larger mat widths (larger surface area in contact with the flow), subject the mats to larger forces, while longer bank connections (Lm) provide more support. Wm was found to be a more dominant parameter in the calculation of critical velocities, an effect captured with a modified horizontal aspect ratio, \( \beta \). A general relationship between this modified horizontal aspect
ratio and critical velocity was constructed in the form $V_c = f(\beta, F_c)$. This average percent error between the $V_c$ values predicted by Equation 76 and the modeled values is 3.33%. The hope is that this formula can be useful to engineers and scientists evaluating floating marsh survivability.

6.2 Recommendations

As mentioned previously, there are many assumptions and limitations involved in results of this thesis. Suggestions for further analysis will be outlined in this section.

1) Direct field measurements of the material properties intrinsic to floating marshes need to be conducted using the MMTST. In particular, the tensile stress-strain profiles should be measured at different locations on a mat (i.e. Y.M., Y.S., & U.S. measured at the mat-bank connection, the middle of the mat, and the far edge). This will ascertain any variation in floating marsh strength. This study assumes fully homogenous mat strength properties.

2) Further modeling should be conducted to quantify the effect of $T_m$ on the drag coefficients. Smaller mat thicknesses reduce the surface area impacted by the approaching flow, altering the drag coefficients. Additionally, smaller mat thicknesses narrow the bank connection area. The reduction in bank connection area will lessen the structural integrity of the mats, lowering the critical velocities. Also, a relationship between non-dimensionalized areas (aspect ratios) and $C_d$ will allow for broader application of this analysis.

3) A flume study validation should be performed with semi-submerged bluff bodies attached to the wall complete with dynamometers for force measurement. The published flume experimental data used in the validation was for lower Reynold’s numbers (1E4) than those simulated in this study (1E6 – 1E7).

4) The floating marsh root-soil matrix is more comparable to a fibrous composite material rather than the simplified homogenous material modeled here. There are two separate
Young’s modulus values for floating marsh mats: a tensile Y.M. for the roots, and a compressive Y.M. for the peaty substrate. A more sophisticated FEA analysis should be conducted to evaluate the impact this has on the structural response of the mats.

5) The response of floating marsh mats to hydrodynamics is inherently a 2-way fluid structure interaction problem. This study uses a simplified 1-way approach by estimating the drag forces on fixed bluff bodies via predicted C_d’s and applying those loads to the front face boundary on a static body. A coupling between a transient structural model and a CFD simulation will yield more accurate results.
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Google Maps Imagery, (9/15/2013) Lake Naivasha, Kenya. 0°42'58.73"S, 36°21'30.48"E Eye alt 6000 feet. CNES. Astrium.


Vita

Jason Haydel Collins III is native to south Louisiana and received a bachelor’s in Environmental Engineering at Louisiana State University in 2014. He joined the graduate program for Coastal and Ecological Engineering in hopes of contributing to the state wide concerted effort to mitigate flood problems and coastal land loss. A strong interest in computational fluid dynamics was realized through the process of this research; an intrigue he plans to cultivate through future work and study.