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Advanced Fluid–Structure Interaction Techniques for Modeling Ringsail Parachutes

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Abstract

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The Team for Advanced Flow Simulation and Modeling (T*AFSM) at Rice University specializes in developing fluid–structure interaction (FSI) modeling techniques for several classes of challenging problems including geometrically complex parachutes. Current modeling technologies are expanded upon with emphasis placed on more realistic FSI modeling of the Orion spacecraft ringsail parachutes. A method for generating a starting condition that matches NASA drop test data and allows for a fair comparison of design variations is introduced. The effect of the geometric porosity distribution on parachute performance and stability is analyzed for three parachute configurations. Rotationally periodic computations that model flow past the complex canopy geometry are presented. Fabric and geometric porosity coefficients are calculated for an improved FSI porosity model. A spatially multiscale technique is used to compare fabric stresses with and without a vent hoop.
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Chapter 1

Introduction

Many real-world engineering problems involve internal or external fluid flow past a structure. Fluid unknowns may include velocities, pressures and densities, from which fluid tractions can be calculated. Unknown structural displacements can then be calculated from the fluid tractions on the surface of the structure. Structural stresses can be calculated from the constitutive relationship with the now known structural displacements. In the case of a structure with prescribed spatial and temporal displacements, a stand-alone fluid calculation with boundary conditions based on the prescribed structural displacements will yield the fluid unknowns. However, when the structural displacements are dependent on the fluid forces, the fluid and structural unknowns become coupled and must be solved simultaneously. For example, at any moment in time the structural displacements depend on the fluid tractions at the fluid–structure interface and vice versa. This coupled interaction between fluid and structure, termed fluid–structure interaction (FSI), becomes even more pronounced in the case of lightweight structures that readily move under the influence of fluid forces. Aerodynamic decelerators, made from textiles to achieve the low weight and volume required in aerospace applications, are a prime example of such a FSI problem.

Analytical approaches for complex FSI problems are not feasible, as closed-form solutions to the unsteady Navier–Stokes equations are limited to specific cases, all
with simple geometry. Experimental approaches provide valuable empirical data but are not as amenable to in-depth analysis. This is because experimental quantities may be difficult to measure over all areas of interest and the desired test environment may be difficult to establish or control, especially for full-scale testing of large aerodynamic decelerators. Reliable numerical techniques can accurately solve FSI problems and allow in-depth analysis since fluid and structure unknowns are computed for the entire problem domain over a desired time period. Motived by that capability of numerical techniques, this research focuses on using computational FSI techniques to accurately model ringsail parachutes.

Ringsail parachutes were chosen as the test case for several reasons. Firstly, because of good drag characteristics and inflation reliability, ringsail parachutes are used in many critical descent and recovery applications. Better understanding of ringsail parachute performance will improve effectiveness and reliability in these critical applications. Secondly, FSI modeling of ringsail parachutes is inherently challenging due to their complex geometry and highly-coupled nature. As such, special modeling techniques have been developed to alleviate these challenges. This thesis expands upon these special techniques by presenting improvements that increase computational accuracy. Thirdly, the Team for Advanced Flow Simulation and Modeling (T*AFSM) [tafsm.org] at Rice University has extensive experience with FSI modeling of parachutes. This previous experience creates a strong foundation for the advanced ringsail parachute computations detailed in this research.

The specific ringsail parachute chosen for modeling is expected to be used by NASA to recover the Orion Crew Exploration Vehicle (CEV). The Orion CEV, Ares I crew launch vehicle, and Ares V cargo launch vehicle constitute the hardware of NASA’s Constellation program. The goal of the Constellation program is to not only replace the venerable Space Shuttle’s earth orbit transport capability but also to further expand the limits of manned space exploration. As such, the Orion CEV was
designed as a crew capsule to transport humans to the International Space Station, moon, Mars and other astronomical points of interest beyond earth orbit. The Orion CEV can house up to six crew and sits atop the Ares I rocket used to deliver the CEV to earth orbit. While the Constellation program returns to the conventional spacecraft architecture from which the Space Shuttle so radically departed, the technology behind this architecture is vastly improved upon. The Orion CEV and Ares launch vehicles were designed using lessons learned from the Space Shuttle and Apollo programs while incorporating state-of-the-art technology and materials. Like the Apollo capsule, the Orion CEV will be recovered after reentry with a cluster of three main ringsail parachutes, as illustrated in Figure 1.1. However, the Orion CEV is heavier, necessitating increased parachute performance with a minimal increase in parachute weight.

Figure 1.1: Artistic rendering [1] of Orion CEV descending under three main ringsail parachutes.
1.1 Ringsail Parachute Description

The main components of a parachute system are the canopy, suspension and riser elements, and payload. Drag is generated by the canopy and this force is transferred through the suspension and riser elements to decelerate the payload. Although parachute canopy designs vary based on the payload and intended application [4, 8], structural efficiency is always a design consideration. Lightweight fabric that creates drag is sewn into a “framework” of heavier high-strength elements that bear canopy loads, seen in Figure 1.2 for the Orion CEV ringsail parachute. Since the heavier elements occupy only a small fraction of the canopy, weight is minimized while achieving drag and strength requirements. High-strength textile elements, such as radial lines, vent lines and vent hoop, bear the large radial loads that occur during payload deceleration. Radial lines start at the canopy’s apex, or crown, and continue to its bottom, or skirt, where each radial line is connected to a suspension line. If the crown has a vent to aid inflation, the radial lines begin at the vent edge and are connected to vent lines or a vent hoop. Vent lines and vent hoops, discussed in greater detail in

Figure 1.2: Illustration of Orion CEV ringsail canopy structure showing fabric (orange/white) sewn into framework of higher-strength cable elements.
Section 6.1, are both designed to bear the total parachute load in the region where all radial lines converge. Many canopy designs also have medium-strength textile elements, such as a vent band, skirt band, and intercostal tapes, to bear the smaller circumferential loads that occur during pressurization. The vent band also increases the tear strength of the vent edge and preserves the parachute shape in the case of a split gore. The skirt band performs a similar function at the skirt and also aids inflation since it increases the skirt inlet area by increasing skirt bending stiffness. Intercostal tapes, which traverse radial lines, bear small pressure loads and serve as a ripstop.

Ringsail parachutes are distinguished by the presence of rings and sails that create numerous gaps and slits in the canopy. The rings are located near the crown and the sails start below these and continue to the skirt. Since the airflow is from skirt to crown, the edge of each ring and sail closest to the skirt is referred to as the leading edge while the edge closest to the apex is referred to as the trailing edge. Ring and sail radial edges are sewn into the radial lines while the leading and trailing edges remain free. The leading and trailing edges of all rings are spaced along the radial lines to create gaps while the leading and trailing edges of adjacent sails are sewn into radial lines without this spacing. However, sails are manufactured so that the leading edge length is greater than the distance between the unstressed radials. This leading edge “fullness” creates crescent-shaped slits between adjacent sails. Figure 1.3 shows the rings and sails with corresponding ring gaps and sail slits for the Orion CEV ringsail parachute. For a more detailed description of general ringsail parachute design, see [3, 4, 8].

As previously mentioned, the Orion CEV will be recovered by a cluster of three main ringsail parachutes. Each “main” parachute is identical and has a nominal diameter ($D_o$) of approximately 120 ft and a quarter spherical shape in its unstressed state. A vent with vent hoop is included in the design. The canopy has eighty
Figure 1.3: Rings with gaps (left) and sails with slits (right) shown for Orion CEV ringsail parachute.

radial lines and thus eighty gores, where a gore is the slice of canopy between two adjacent radials. Each gore contains four rings and nine sails from the vent to the skirt, as shown in Figure 1.4. The canopy includes a vent band and skirt band and

Figure 1.4: Gore layout of Orion CEV ringsail canopy (not to scale)

has intracostal tapes on the leading and trailing edges of selected rings and sails near the crown. Eighty suspension lines are connected to the radial lines terminated at the skirt. All eighty suspension lines meet at a confluence point where a single riser begins. Each suspension line has a length of approximately 130 ft and the riser length is approximately 100 ft. For a single main configuration, the single riser attaches directly to the payload from the suspension line confluence. The total weight of the CEV is approximately 16,700 lbs. All material properties for the ringsail parachutes, including the distributions of canopy fabric porosity and sail fullness, were provided
by NASA JSC and Irvin Aerospace. An unstressed, single main parachute assembly, which includes the canopy, suspension lines, riser and payload mass, is shown in Figure 1.5. Note the unstressed quarter-spherical shape and the lack of fullness on the bottom sail that promotes flow separation at the skirt.

![Orion CEV unstressed, single main parachute assembly.](image)

Figure 1.5: Orion CEV unstressed, single main parachute assembly.

### 1.2 Content Overview

Chapter 2 presents the governing equations and details the finite element formulations used to obtain the computational results in this thesis. Chapter 3 describes several special fluid–structure interaction techniques used to deal with the geometric
complexity of ringsail parachutes and generate a favorable starting condition for FSI computations.

In Chapter 4, several parachute designs are compared to determine how changes in geometric porosity can affect horizontal gliding and descent speed oscillations caused by a canopy breathing motion. A method for creating similar starting conditions for a fair comparison of different parachute configurations is also described.

An improved n-gore model that utilizes rotationally periodic boundary conditions is introduced in Chapter 5. This chapter also includes a thorough examination of periodic n-gore conditions which affect convergence of the fabric and geometric porosity coefficients calculated using the HMGP-FG technique. Fabric and geometric porosity values applicable for FSI computations of parachutes in the full open stage are given. The pressure dependence of the HMGP and HMGP-FG porosity coefficients is examined, highlighting the superior modeling capability of the new HMGP-FG technique.

Chapter 6 outlines the spatially multiscale method used to calculate more accurate fabric stresses based on existing FSI data. The stress-relieving effect of a vent hoop on crown fabric stresses is also examined.

Conclusions are presented in Chapter 7.
Chapter 2

Governing Equations and Finite Element Formulations

This chapter presents the system of equations governing the parachute structure and surrounding fluid and then details the finite element formulations based on these equations. The Navier-Stokes equations of incompressible flow, which govern the flow regime since the parachute velocity is well below the compressible limit, are introduced first. The structural mechanics equations of motion governing the membrane and cable deformation of the parachute structure are then introduced. These fluid and structural mechanics governing equations are the basis of the finite element formulations used to obtain the computational results presented in this thesis. These finite element formulations are described in detail after the governing equations are presented.

2.1 Fluid Mechanics Equations

Let $\Omega_t \subset \mathbb{R}^{n+m}$ be the spatial domain with boundary $\Gamma_t$ at time $t \in (0, T)$. The subscript $t$ is used to denote the time-dependence of the spatial domain and its boundary. The Navier–Stokes equations of incompressible flows are written on $\Omega_t$ and $\forall t \in (0, T)$
where $\rho$, $\mathbf{u}$ and $\mathbf{f}$ are the density, velocity and the external force, respectively. The stress tensor $\mathbf{\sigma}$ is defined as

$$
\mathbf{\sigma}(\rho, \mathbf{u}) = -\rho \mathbf{I} + 2\mu \mathbf{\varepsilon}(\mathbf{u}),
$$

with

$$
\mathbf{\varepsilon}(\mathbf{u}) = \frac{1}{2} \left( (\nabla \mathbf{u}) + (\nabla \mathbf{u})^T \right).
$$

Here $p$ is the pressure, $\mathbf{I}$ is the identity tensor, $\mu = \rho \nu$ is the viscosity, $\nu$ is the kinematic viscosity, and $\mathbf{\varepsilon}(\mathbf{u})$ is the strain-rate tensor. The essential and natural boundary conditions are represented respectively as

$$
\mathbf{u} = \mathbf{g} \text{ on } (\Gamma_t)_g, \quad \text{(2.5)}
$$

$$
\mathbf{n} \cdot \mathbf{\sigma} = \mathbf{h} \text{ on } (\Gamma_t)_h, \quad \text{(2.6)}
$$

where $(\Gamma_t)_g$ and $(\Gamma_t)_h$ are complementary subsets of the boundary $\Gamma_t$, $\mathbf{n}$ is the unit normal vector, and $\mathbf{g}$ and $\mathbf{h}$ are given functions. A divergence-free velocity field $\mathbf{u}_0(\mathbf{x})$ is specified as the initial condition.

### 2.2 Structural Mechanics Equations

Let $\Omega_t \subset \mathbb{R}^{n_{xd}}$ be the spatial domain with boundary $\Gamma_t^s$, where $n_{xd} = 2$ for membranes and $n_{xd} = 1$ for cables and the superscript $s$ is used to denote the structure. The parts
of $\Gamma_i^s$ corresponding to the essential and natural boundary conditions are represented by $(\Gamma_i^s)_g$ and $(\Gamma_i^s)_h$ respectively. The equations of motion are written as

$$\rho^s \left( \frac{d^2 \mathbf{y}}{dt^2} + \eta \frac{d\mathbf{y}}{dt} - \mathbf{f}^s \right) - \nabla \cdot \mathbf{\sigma}^s = 0 ,$$

(2.7)

where $\rho^s$, $\mathbf{y}$, $\mathbf{f}^s$ and $\mathbf{\sigma}^s$ are the material density, structural displacement, external force and Cauchy stress tensor, respectively. Here $\eta$ is an artificial mass-proportional damping coefficient, which is nonzero only in computations where time accuracy is not required. Time independent computations are used to obtain deformed structure shapes based on specified forces, typically fluid mechanics forces from a previous computation. Utilizing artificial damping in such computations improves structural convergence, especially for highly refined structure meshes, thereby promoting a more robust structural mechanics solution. Artificial mass-proportional damping is used for the shape determination method in Section 3.4 and spatially-multiscale fabric stress calculations in Section 6.3.

Under the assumption of large displacements and rotations, small strains, and no material damping, the membranes and cables are characterized with linearly-elastic material properties. Stresses are expressed in terms of the second Piola–Kirchhoff stress tensor $\mathbf{S}$, which is related to the Cauchy stress tensor through a kinematic transformation. For membranes, under the assumption of plane stress,

$$\mathbf{S}^{ij} = (\ddot{\mathbf{\lambda}}^s G^{ij} G^{kl} + \mu^s (G^{il} G^{jk} + G^{ik} G^{jl})) E_{kl} ,$$

(2.8)

where for the case of isotropic plane stress $\ddot{\mathbf{\lambda}}^s = 2 \lambda^s \mu^s / (\lambda^s + 2 \mu^s)$. Here, $E_{kl}$ are the components of the Green–Lagrange strain tensor, $G^{ij}$ are the contravariant components of the metric tensor in the original configuration, and $\lambda^s$ and $\mu^s$ are the material Lamé constants. For cables, under the assumption of uniaxial tension, $\mathbf{S}$ becomes $S^{11} = E_c G^{11} G^{11} E_{11}$, where $E_c$ is the Young’s modulus for the cable.
2.3 DSD/SST Formulation of Fluid Mechanics

In the Deforming-Spatial-Domain/Stabilized Space–Time (DSD/SST) method [15, 19, 20, 16, 22], the finite element formulation is written over a sequence of \( N \) space-time slabs \( Q_n \), where \( Q_n \) is the slice of the space–time domain between the time levels \( t_n \) and \( t_{n+1} \). At each time step, the integrations are performed over \( Q_n \). The space–time finite element interpolation functions are continuous within a space–time slab, but discontinuous from one space–time slab to another. The notation \((\cdot)_n^-\) and \((\cdot)_n^+\) will denote the function values at \( t_n \) as approached from below and above. Each \( Q_n \) is decomposed into elements \( Q_e^n \), where \( e = 1, 2, \ldots, (n_e)_n \). The subscript \( n \) used with \( n_e \) is for the general case where the number of space–time elements may change from one space–time slab to another. The essential and natural boundary conditions are enforced over \((P_n)_g\) and \((P_n)_h\), the complementary subsets of the lateral boundary of the space–time slab. The finite element trial function spaces \((S^h_u)_n\) for velocity and \((S^h_p)_n\) for pressure, and the test function spaces \((V^h_u)_n\) and \((V^h_p)_n = (S^h_p)_n\) are defined by using, over \( Q_n \), first-order polynomials in space and time.

The DSD/SST formulation (as presented in [16]) is written in the following manner: given \((u^h)_n^-\), find \( u^h \in (S^h_u)_n \) and \( p^h \in (S^h_p)_n \) such that \( \forall w^h \in (V^h_u)_n \) and \( \forall q^h \in (V^h_p)_n \):

\[
\begin{align*}
\int_{Q_n} w^h \cdot \rho \left( \frac{\partial u^h}{\partial t} + u^h \cdot \nabla u^h - f^h \right) dQ + \int_{Q_n} \varepsilon(w^h) : \sigma(p^h, u^h) \ dQ \\
- \int_{(P_n)_h} w^h \cdot h^h dP + \int_{Q_n} q^h \nabla \cdot u^h dQ + \int_{\Omega_n} (w^h)_n^+ \cdot \rho ((u^h)_n^+ - (u^h)_n^-) d\Omega \\
+ \sum_{e=1}^{(n_e)_n} \int_{Q_e^h} \frac{1}{\rho} \left[ \tau_{\text{SUPG}} \left( \frac{\partial w^h}{\partial t} + u^h \cdot \nabla w^h \right) + \tau_{\text{PSPG}} \nabla q^h \right] \cdot [L(p^h, u^h) - \rho f^h] dQ \\
+ \sum_{e=1}^{(n_e)_n} \int_{Q_e^h} \nu_{\text{SIC}} \nabla \cdot w^h \rho \nabla \cdot u^h dQ = 0 ,
\end{align*}
\]  

(2.9)
where
\[
\mathbf{L}(q^h, w^h) = \rho \left( \frac{\partial w^h}{\partial t} + u^h \cdot \nabla w^h \right) - \nabla \cdot \sigma(q^h, w^h) .
\] (2.10)

This formulation is applied to all space-time slabs \( Q_0, Q_1, Q_2, \ldots, Q_{N-1} \), starting with \( (u^h)_0 = u_0 \). Here \( \tau_{\text{SUPG}}, \tau_{\text{PSPG}} \) and \( \nu_{\text{LSIC}} \) are the SUPG (Streamline-Upwind/Petrov-Galerkin), PSPG (Pressure-Stabilizing/Petrov-Galerkin) and LSIC (least-squares on incompressibility constraint) stabilization parameters. Several different options exist to define these stabilization parameters. Here one finds the definitions given in [16]:

\[
\tau_{\text{SUPG}} = \left( \frac{1}{\tau_{\text{SUPG}12}^2} + \frac{1}{\tau_{\text{SUPG}3}^2} \right)^{-\frac{1}{2}},
\] (2.11)

\[
\tau_{\text{SUPG}12} = \left( \sum_{a=1}^{n_{en}} \left| \frac{\partial N_a}{\partial t} + u^h \cdot \nabla N_a \right| \right)^{-1},
\] (2.12)

\[
\tau_{\text{SUPG}3} = \frac{h_{\text{RGN}}^2}{4\nu},
\] (2.13)

\[
h_{\text{RGN}} = 2 \left( \sum_{a=1}^{n_{en}} |r \cdot \nabla N_a| \right)^{-1},
\] (2.14)

\[
r = \frac{\nabla |u^h|}{\| \nabla |u^h| \|},
\] (2.15)

\[
\tau_{\text{PSPG}} = \tau_{\text{SUPG}},
\] (2.16)

\[
\nu_{\text{LSIC}} = \tau_{\text{SUPG}} \| u^h - v^h \|^2,
\] (2.17)

where \( n_{en} \) is the number of (space–time) element nodes, \( N_a \) is the space–time shape function associated with the space–time node \( a \), and \( v^h \) is the mesh velocity. An alternative method to that shown in Eqs. (2.11)–(2.12) for determining \( \tau_{\text{SUPG}} \) was presented in [22]. These options for determining \( \tau_{\text{SUPG}} \) are based on separate definitions for the advection-dominated and transient-dominated limits and are given as
follows:

\[
\begin{align*}
\tau_{\text{SUPG}} &= \left( \frac{1}{\tau_{\text{SUGN1}}} + \frac{1}{\tau_{\text{SUGN2}}} + \frac{1}{\tau_{\text{SUGN3}}} \right)^{-\frac{1}{2}}, \\
\tau_{\text{SUGN1}} &= \left( \sum_{a=1}^{\text{ren}} \left| (u^h - v^h) \cdot \nabla N_a \right| \right)^{-1}, \\
\tau_{\text{SUGN2}} &= \frac{\Delta t}{2}.
\end{align*}
\] (2.18)

(2.19)

(2.20)

Note that partitioning \( \tau_{\text{SUGN12}} \) into its advection-dominated and transient-dominated components as given by Eqs. (2.19)-(2.20) is equivalent to excluding the \( \left( \frac{\partial N_a}{\partial t} \right|_\xi \) part of \( \left( \frac{\partial N_a}{\partial t} \right) \) in Eq. (2.12), making that the definition for \( \tau_{\text{SUGN1}} \), and accounting for the \( \left( \frac{\partial N_a}{\partial t} \right|_\xi \) part in the definition for \( \tau_{\text{SUGN2}} \) given by Eq. (2.20). Here \( \xi \) is the vector of element (parent-domain) coordinates. Additional methods for calculating \( \tau_{\text{SUPG}} \), \( \tau_{\text{PSPG}} \) and \( \nu_{\text{LSIC}} \), can be found in [21, 16, 17, 18]. The Discontinuity-Capturing Directional Dissipation (DCDD) stabilization, which can also be found in references [16, 17, 18], was introduced as an alternative to the LSIC stabilization.

Several of the remarks from [22] concerning this chapter are relevant and are reproduced in this thesis as Remarks 1–6.

**Remark 1** As an alternative to the way the SUPG test function is defined in Eq. (2.9), we propose the SUPG test function option of replacing the term \( \left( \frac{\partial w^h}{\partial t} + u^h \cdot \nabla w^h \right) \) with \( (u^h - v^h) \cdot \nabla w^h \). This replacement is equivalent to excluding the \( \left( \frac{\partial w^h}{\partial t} \right|_\xi \) part of \( \left( \frac{\partial w^h}{\partial t} \right) \). We call this option “WTSE”, and the option where the \( \left( \frac{\partial w^h}{\partial t} \right|_\xi \) term is active “WTSA”.

**Remark 2** With the function spaces defined in the paragraph preceding Eq. (2.9), for each space-time slab velocity and pressure assume double unknown values at each spatial node. One value corresponds to the lower end of the slab, and the other one to the upper end. The option of using double unknown values at a spatial node will be called “DV” for velocity and “DP” for pressure. In this case, we use two integration
points over the time interval of the space-time slab, and this time-integration option will be called “TIP2”. This version of the DSD/SST formulation, with the options set DV, DP and TIP2, will be called “DSD/SST-DP”.

**Remark 3** We propose here the option of using, for each space-time slab, a single unknown pressure value at each spatial node, and we will call this option “SP”. With this, we propose another version of the DSD/SST formulation, where the options set is DV, SP and TIP2, and we will call this version “DSD/SST-SP”. Because the number of unknown pressure values is halved, the computational cost is reduced substantially.

**Remark 4** To reduce the computational cost further, we propose the option of using only one integration point over the time interval of the space-time slab, and we call this time-integration option “TIP1”. With this, we propose a third version of the DSD/SST formulation, where the options set is DV, SP and TIP1, and we will call this version “DSD/SST-TIP1”.

### 2.4 Semi-discrete Formulation of Structural Mechanics

Assuming that the trial function space, $y^h$, and the test function space, $w^h$, come from appropriately defined spaces, the semi-discrete finite element formulation of the structural mechanics equations (see [9, 2, 12]) is written as

$$
\int_{\Omega^h_t} w^h \cdot \rho \frac{d^2 y^h}{dt^2} d\Omega^s + \int_{\Omega^h \delta} w^h \cdot \eta \rho \frac{d y^h}{dt} d\Omega^s + \int_{\Omega^h \delta} \delta E^h : S^h d\Omega^s = \int_{\Omega^h_t} w^h \cdot (t^h + \rho \Phi^s) d\Omega^s .
$$

(2.21)

The fluid mechanics forces acting on the structure are represented by vector $t^h$. This force term is geometrically nonlinear and thus increases the overall nonlinearity of
the formulation. The left-hand-side terms of Eq. (2.21) are referred to in the original configuration and the right-hand-side terms in the deformed configuration at time $t$. A nonlinear system of equations emerges from this formulation at every time step. An incremental form is used to solve that nonlinear system with an iterative method (see [9, 2, 12, 6]). This form is expressed as

$$\left[ \frac{M}{\beta \Delta t^2} + \frac{(1 - \alpha) \gamma C}{\beta \Delta t} + (1 - \alpha) K \right] \Delta d^i = R^i . \quad (2.22)$$

Here $M$ is the mass matrix, $C$ is the artificial-damping matrix, $K$ is the consistent tangent matrix associated with the internal elastic forces, $R^i$ is the residual vector at the $i^{th}$ iteration, and $\Delta d^i$ is the $i^{th}$ increment in the nodal displacements vector $d$. The artificial-damping matrix $C$, as mentioned in Section 2.2, is used only in computations where time-accuracy is not required, and for spatially-constant $\eta$ it can be written as $C = \eta M$. All of the terms known from the previous iteration are lumped into the residual vector $R^i$. The parameters $\alpha, \beta, \gamma$ are part of the Hilber-Hughes-Taylor [5] scheme, which is the time-integration technique used here. For all computations reported in this thesis, the structural mechanics mass matrix is lumped. This is consistent with other parachute computations performed by the T*AFSM.

### 2.5 Stabilized Space–Time Fluid–Structure Interaction (SSTFSI) Method

The description of the SSTFSI method given here is based on the finite element formulations given by Eqs. (2.9) and (2.21), with a slight change of notation and with a clarification of how the fluid–structure interface conditions are handled. In this notation, subscripts 1 and 2 will refer to fluid and structure respectively. Furthermore, while subscript $I$ will refer to the fluid–structure interface, subscript $E$ will refer to "elsewhere" in the fluid and structure domains or boundaries. Then the equations
representing the SSTFSI method are written as follows:

\[
\int_{Q_n} w_{1E}^h \cdot \rho \left( \frac{\partial u^h}{\partial t} + u^h \cdot \nabla u^h - f^h \right) dQ + \int_{Q_n} \varepsilon(w_{1E}^h) : \sigma(p^h, u^h) dQ \\
- \int_{(P_n)_h} w_{1E}^h \cdot h_{1E}^h dP + \sum_{e=1}^{(n_e)_n} \int_{Q_n^e} q_{1E}^h \nabla \cdot u^h dQ + \int_{\Omega_n} (w_{1E}^h)^+ \cdot \rho ((u^h)^+ - (u^h)^-) d\Omega \\
+ \sum_{e=1}^{(n_e)_n} \int_{Q_n^e} \frac{1}{\tau_{SUPG}} \left( \frac{\partial w_{1E}^h}{\partial t} + u^h \cdot \nabla w_{1E}^h \right) + \tau_{SUPG} \nabla q_{1E}^h \cdot [L(p^h, u^h) - \rho f^h] dQ \\
+ \sum_{e=1}^{(n_e)_n} \int_{Q_n^e} \nu_{LSC} \nabla \cdot w_{1E}^h \rho \nabla \cdot u^h dQ = 0 , \quad (2.23)
\]

\[
\int_{Q_n} q_{1E}^h \nabla \cdot u^h dQ + \sum_{e=1}^{(n_e)_n} \int_{Q_n^e} \frac{1}{\tau_{SUPG}} \left[ \tau_{SUPG} \nabla q_{1E}^h \right] \cdot [L(p^h, u^h) - \rho f^h] dQ = 0 , \quad (2.24)
\]

\[
\int_{(\Gamma_n)_{REF}} (w_{1i}^h)_{n+1}^- \cdot ((u_{1i}^h)_{n+1}^- - u_{1i}^h) d\Gamma = 0 , \quad (2.25)
\]

\[
\int_{(P_{11})_n+1} (w_{11}^h)_{n+1}^- \cdot h_{11}^h dP = - \int_{(P_{11})_n+1} (w_{11}^h)_{n+1}^- \cdot p n dP + \int_{Q_n} 2 \mu \varepsilon((w_{11}^h)_{n+1}^-) : \varepsilon(u) dQ \\
+ \int_{Q_n} (w_{11}^h)_{n+1}^- \cdot \nabla \cdot (2 \mu \varepsilon(u)) dQ , \quad (2.26)
\]

\[
\int_{(\Omega_{21})_{REF}} w_{21}^h \cdot (h_{21}^h + (h_{11}^h)_A + (h_{11}^h)_B) d\Omega = 0 , \quad (2.27)
\]
\[ \int_{\Omega_0} w^h \cdot \rho_2 \frac{d^2 y^h}{dt^2} \, d\Omega + \int_{\Omega_0} w^h \cdot \eta \frac{dy^h}{dt} \, d\Omega + \int_{\Omega_0} \delta E^h : S^h \, d\Omega \]

\[ = \int_{\Omega_2} w^h \cdot \rho_2 f^h_2 \, d\Omega + \int_{\Omega_{2E}} w^h_{2E} \cdot h^h_{2E} \, d\Omega + \int_{\Omega_{21}} w^h \cdot h^h_{21} \, d\Omega. \]  \tag{2.28}

Here \((\Gamma_{21})_{REF}\) and \((\Omega_{21})_{REF}\) represent some reference configurations of \(\Gamma_{21}\) and \(\Omega_{21}\), respectively. To bridge the slight disconnect between the slightly modified notation used here with the notation used in Eqs. (2.9) and (2.21), one should note that \(\rho_2 = \rho^s\), \(f^h_2 = f^s\), \((\Omega_0)_0 = \Omega_0^s\), \(\Omega_0 = \Omega_i\), and \(\Omega_{21}\) and \(\Omega_{2E}\) indicate the partitions of \(\Omega_2\) corresponding to the interface and “elsewhere”. It should also be noted that \(h^h_{21} = t^h\), and \((h^h_{11})_A\) and \((h^h_{11})_B\) represent the values of \(h^h_{11}\) associated with the fluid surfaces above and below the membrane structure. The symbol \(h^h_{2E}\) denotes the prescribed external forces acting on the structure in \(\Omega_{2E}\), which is separate from \(f^h_2\). In this formulation, \((u^{h}_{11})_{n+1}\), \(h^h_{11}\) and \(h^h_{21}\) (the fluid velocity, fluid stress and structural stress at the interface) are treated as separate unknowns, and Eqs. (2.25), (2.26) and (2.27) can be seen as equations corresponding to these three unknowns, respectively. The structural displacement rate at the interface, \(u^h_{21}\), is derived from \(y^h\).

The formulation above is based on allowing for cases when the fluid and structure meshes at the interface are not identical. If they are identical, the same formulation can still be used. If the structure is represented by a 3D continuum model instead of a membrane model, the formulation above would still be applicable if the domain integrations over \(\Omega_{2E}\) and \(\Omega_{21}\) in the last two terms of Eq. (2.28) are converted to boundary integrations over \(\Gamma_{2E}\) and \(\Gamma_{21}\). In such cases, \(h^h_{2E}\) would represent the prescribed forces acting “elsewhere” on the surface of the structure.

Note that, for constant viscosity, the term \(\nabla \cdot (2\mu \mathbf{e}(\mathbf{u}))\) in Eq. (2.26) vanishes for tetrahedral elements and in most cases can be neglected for hexahedral elements. The same statement can be made also in the context of that term being a part of the
expression \( L(p^h, u^h) \) appearing in Eqs. (2.23) and (2.24).

In computations which account for the porosity of the membrane fabric, Eq. (2.25) is replaced with the following one:

\[
\int_{\Gamma_{\text{tri}}} (w_{\text{tri}}^h)^+ \cdot ((u_{\text{tri}}^h)^+ - u_{\text{tri}}^h + k_{\text{PORO}} \cdot (n \cdot h_{\text{tri}}^h) \cdot n) \, d\Gamma = 0 , \tag{2.29}
\]

where \( k_{\text{PORO}} \) is the porosity coefficient. This coefficient is typically given in units of “CFM” meaning “cubic feet of air per minute per square foot at a pressure differential of half an inch of water”. To elaborate on this unit of measurement, when a fabric with a porosity coefficient of 1 CFM is subjected to a pressure differential of \( \frac{1}{2} \) in H\(_2\)O, a volumetric flowrate of 1 ft\(^3\)/min occurs across a unit surface area of 1 ft\(^2\), which translates to a normal velocity of 1 ft/min. In the current implementation of Eq. (2.29), only the pressure component of \( h_{\text{tri}}^h \) is taken into account.

**Remark 5** In FSI computations with membranes and shells, the pressure at the interface has split nodal values corresponding to the fluid surfaces above and below the membrane or shell structure. Split nodal values for pressure are also used at the boundaries (i.e. edges) of a membrane structure surrounded by the fluid. Computations show that this provides additional numerical stability for the edges of the membrane.

**Remark 6** The versions of the SSTFSI method corresponding to the DSD/SST-DP, DSD/SST-SP, and DSD/SST-TIP1 formulations (see Remarks 2–4) will be called “SSTFSI-DP”, “SSTFSI-SP”, and “SSTFSI-TIP1”, respectively.
Chapter 3

Special Modeling Techniques

The techniques described below are special modeling techniques developed specifically for ringsail parachutes by T*AFSM. Some were developed to simplify modeling of the complex structure geometry while others were developed to generate a favorable starting condition.

3.1 Geometric Smoothing Technique

Ringsail parachute canopies have a very complex geometry due to the rings gaps and numerous sail slits described in Section 1.1. To avoid transferring the geometric complexity of the structure to the fluid mechanics mesh, incompatible meshes are used at the fluid-structure interface. The structure interface mesh represents the actual parachute geometry, with all the ring gaps and sail slits present. This geometrically true mesh is used to accurately determine parachute deformations and stresses. The fluid interface mesh represents a “smoothened” surface without gaps or slits based on the radial line nodes, or “valley” nodes, of the structure. This geometrically simplified mesh only includes the geometric apertures of the vent and missing sails (see Chapter 4), not the many ring gaps and sail slits. The structure interface mesh is more refined and composed of four-node quadrilateral elements while the fluid
interface mesh is less refined and composed of three-node triangular elements. Four-gore slices of the structure and fluid interface meshes are shown in Figures 3.1 and 3.2, respectively. These meshes curve into the paper towards the skirt, so that the element aspect ratios are actually better than they appear.

Figure 3.1: Four-gore slice of structure interface mesh (top) with close-up of rings (bottom)

Generation of the fluid interface mesh and mapping between incompatible interfaces is carried out according to the FSI Directional Geometric Smoothing Technique (FSI-DGST) [22, 24] where all fluid interface nodes are colocated with structure valley nodes. The circumferential and radial spacing of fluid interface nodes vary in the radial direction in order to maintain favorable vent geometry and element aspect ratios. The fluid mechanics computations only see the smoothened parachute represented by the fluid interface mesh. The structural mechanics computations retain the true ring and sail geometry, as represented by the structure mesh. In coupled
fluid and structural mechanics computations, nodal quantities are projected between these incompatible interfaces. Fluid interface nodal values, such as displacements and displacement rates, are mapped directly from the colocated structure nodes. When transferring values from the coarser fluid interface to the structure, mapped structure nodes receive the value of the colocated fluid interface node and the remaining structure nodal values are calculated with a weighted average.

### 3.2 Separated Stress Projection

The Separated Stress Projection (SSP) technique was introduced in [24] as another way of projecting stresses from the fluid interface mesh to the structure interface mesh. Previously, a total stress vector was calculated for each fluid interface node and then transferred to the structure interface as described at the end of Section 3.1.
Using the SSP technique, the viscous stress vector and scalar pressure are calculated for each fluid interface node and then transferred to the structure interface separately. Stress vectors corresponding to the pressure component are then found by multiplying the scalar pressures on the structure with the corresponding nodal unit normal vectors. Total stress vectors for the structure are finally calculated by combining the viscous stress vector with the stress vector corresponding to the pressure component. By using structure rather than fluid interface normal vectors to calculate the pressure component of the stress vector, the SSP technique can more accurately model pressurized sail shapes. Fluid stresses projected without and with the SSP technique are depicted in Figure 3.3.

To accommodate this new stress projection, a new version of the SSTFSI technique given in Section 2.5 was introduced in [24], with the pressure and viscous parts of the interface stress vectors separated. In that new version, which is denoted with the option key \( -\text{SSP} \), the symbols \( h^f_{1i} \) and \( h^s_{2i} \) used in Section 2.5 would denote only the viscous parts of the stresses acting on the fluid and structure interfaces, respectively. Furthermore, in Section 2.5, the first term on the right-hand-side of Eq. (2.26) would be dropped, a scalar version of Eq. (2.27) would be added for projecting \( p^h_{2i} \) from \( p^h_{1i} \), in Eq. (2.28) \( h^f_{2i} \) would be replaced with \(-p^h_{2i}n + h^s_{2i}\), and in Eq. (2.29) \( h^s_{1i} \) would be replaced with \(-p^h_{1i}n + h^h_{1i}\).

### 3.3 Homogenized Modeling of Geometric Porosity (HMGP)

The distribution of geometric porosity is responsible for many of the performance characteristics of ringsail parachutes. To obtain accurate results, the geometric porosity lost by not including ring gaps and sail slits in the fluid mechanics computations must be accounted for. This is accomplished through the Homogenized Modeling of
Figure 3.3: An illustration of the Separated Stress Projection (SSP) technique. The purple surface on the left half of the canopy is the fluid interface and the blue surface on the right is the structure interface. The arrows on the left represent the structure interface stresses obtained by transferring the total fluid interface stress vector directly to the structure. The arrows on the right represent the structure interface stresses obtained with the SSP technique.

Geometric Porosity (HMGP) technique, first introduced in [24]. The HMGP technique uses an “equivalent”, locally-varying fabric porosity for the smoothened fluid interface. This equivalent fabric porosity varies in the radial direction and is approximately equal to the combined fabric and geometric porosity in that region. The method for calculating this equivalent HMGP porosity is described in Section 3.3.1. The HMGP technique is improved in the HMGP Fabric/Geometric (HMGP-FG)
technique, first described in [14]. The HMGP-FG technique separates fabric and geometric porosity contributions to volumetric flowrate as linear and non-linear pressure contributions. The details of the HMGP-FG technique are presented in Section 3.3.2.

### 3.3.1 HMGP Technique

The HMGP equivalent porosity values are calculated by performing a one-time fluid mechanics computation of the actual structure with all ring gaps and sail slits included. This computation is done for a single canopy shape, typically the average full open shape. Increased mesh resolution is required to adequately represent the complex geometry of the structure. To avoid exorbitant computational costs, only a four-gore slice of the parachute is considered. This four-gore structure interface is divided into 12 concentric patches so that locally-varying equivalent porosities can be determined. Patch 1 includes Ring 1 and the top half of Ring 2 and Patch 12 includes the bottom half of Sail 8 and all of Sail 9. Each remaining patch includes a gap (or slit) and half of the sail or ring on either side of the gap (or slit). The structure interface patch locations are depicted in Figure 3.4. The smoothened fluid interface is divided into 12 concentric patches at the same radial locations as the structure interface.

Figure 3.4: Patch locations on the structure interface for the HMGP technique (not to scale)

The stand-alone fluid mechanics computation is carried out until the flow field becomes developed, as shown in Figure 3.5. The homogenized porosity value for
patch $J$, $(k_{PORO})_J$, is then calculated from the expression

$$\frac{\dot{V}_J}{(A_1)_J} = -(k_{PORO})_J \frac{\Delta F_J}{(A_2)_J},$$

(3.1)

where $(k_{PORO})_J$ is expressed in CFM (see end of Section 2.5). $\dot{V}_J$ denotes the volumetric flowrate crossing patch $J$. This accounts for the flow passing through the gap (or slit) and the flow crossing the fabric due to its porosity. $(A_1)_J$ denotes the area of patch $J$ calculated from the smoothened fluid interface and $(A_2)_J$ denotes the area of patch $J$ calculated from the structure interface neglecting gap and slit areas. Thus, if $(A_G)_J$ represents gap (or slit) area and $(A_F)_J$ represents fabric area, both calculated for patch $J$ of the structure interface, then $(A_2)_J = (A_F)_J$. The force differential for

Figure 3.5: Fluid mechanics four-gore computation with geometrically true interface.
patch \( J \), \( \Delta F_J \), is calculated by integrating the nodal pressure differentials, \( \Delta p \), over the area of patch \( J \) on the structure interface:

\[
\Delta F_J = \int_{(A_2)_J} \Delta p \, dA.
\]  

(3.2)

The equivalent porosity values calculated from the homogenization process are then applied to the smoothened fluid interface for FSI computations. Patch interior nodes are assigned the homogenized porosity value for that patch while nodes shared by two patches are assigned an average porosity value. The structure interface with innate geometric porosity and material fabric porosity is shown along with the smoothened fluid interface with homogenized porosity values in Figure 3.6 for an example case. The normal velocity crossing the fluid interface, \( u_n \), is calculated nodally using the following equation:

\[
u_n = -(k_{PORO})_J \Delta p.
\]  

(3.3)

Figure 3.6: Structure interface colored by material fabric porosity shown on left and smoothened fluid interface colored by HMGP equivalent porosity shown on right.
3.3.2 HMGP-FG Technique

The HMGP-FG technique [14] improves upon the HMGP technique by separating the fabric and geometric porosity contributions to the total homogenized porosity. It also incorporates changes in porosity due to varying gap and slit areas during the breathing cycle. The HMGP-FG fabric and geometric porosity values are calculated using data from a stand-alone fluid mechanics computation improved from that of the earlier HMGP computations. The differences between those earlier computations and the HMGP-FG fluid mechanics computations are described in Section 5.1. Furthermore, Patches 1 and 12 used in the earlier HMGP computations are each divided into two patches, yielding a total of 14 patches. The new HMGP-FG patch divisions are illustrated in Figure 3.7.

![Figure 3.7: Patch locations on the structure interface for the HMGP-FG technique (not to scale)](image)

The equations used to calculate fabric porosity, $k_F$, and geometric porosity, $k_G$, for patch $J$ are

$$\frac{\langle \dot{V}_F \rangle_J}{(A_1)_J} = -(k_F)_J \frac{(A_F)_J}{(A_1)_J} \Delta p_J,$$  \hspace{1cm} (3.4)

$$\frac{\langle \dot{V}_G \rangle_J}{(A_1)_J} = -(k_G)_J \frac{(A_G)_J}{(A_1)_J} \text{sgn}(\Delta p_J) \sqrt{\frac{\Delta p_J}{\rho}},$$  \hspace{1cm} (3.5)

where $(k_F)_J$ is expressed in CFM (see end of Section 2.5) and $(k_G)_J$ is non-dimensional. Here $\langle \dot{V}_F \rangle_J$ is the volumetric flowrate through the fabric of patch $J$ and $\langle \dot{V}_G \rangle_J$ is the...
volumetric flowrate through the gap (or slits) of patch $J$, where $\left(V_F\right)_J$ and $\left(V_G\right)_J$ sum to $\bar{V}_J$ from the HMGP technique. The average pressure differential across the structure for patch $J$ is

$$\Delta p_J = \frac{\Delta F_J}{(A_2)_J}.$$  

(3.6)

The normal velocity crossing the fluid interface is modeled nodally using the following expression:

$$u_n = -\left(k_F\right)_J \frac{A_F}{A_1} \Delta p - \left(k_G\right)_J \frac{A_G}{A_1} \text{sgn}(\Delta p) \sqrt{\frac{|\Delta p|}{\rho}},$$  

(3.7)

where $\left(k_F\right)_J$, $\left(k_G\right)_J$, $A_F$, $A_G$ and $A_1$ can be seen as “material properties”, calculated for each node by area-weighted averaging of the “material properties” of the (triangular) fluid interface elements sharing that node. Each (triangular) fluid interface element belongs to a “material properties” group. Each structure interface (membrane) element and each gap (or slit) also belongs to a “material properties” group. Each group is associated with a patch $J$. The values of $\left(k_F\right)_J$ and $\left(k_G\right)_J$ for a group come from the patch, $J$, it is associated with. The symbols $A_F$, $A_G$ and $A_1$ represent for a group the total instantaneous area of the fabric, the sum of the instantaneous areas of the gap(s) and the sum of the areas of the (triangular) fluid interface elements. “Material properties” groups are defined based on the 14 patches shown in Figure 3.7. Radially, each group spans one patch. Circumferentially, each group spans 4 gores in Patch 1, 2 gores in Patch 2–5, and 1 gore in Patch 6–14.

### 3.4 Shape Determination

When performing FSI computations, it is important to have a good starting condition that prevents computational instability. A consistent starting condition also
allows a fair comparison of different models, as is the case for the computations in Chapter 4. In parachute computations, the starting condition is defined by the deformation and velocity of the parachute and a developed flow field. Starting with the quarter-spherical constructed canopy shape, shape determination [27, 28] iterations are performed to determine these components of the starting condition. First, a "zeroth" parachute shape is determined with a stand-alone structural mechanics computation using a uniform canopy pressure equal to the stagnation pressure at an estimated descent speed of 25 ft/s. Next a stand-alone fluid mechanics computation is performed with this zeroth shape and estimated descent speed. The fluid interface forces from this computation are then circumferentially "symmetrized" (see Section 3.5 for details) and applied to the structure in another stand-alone structural mechanics computation to determine a new parachute shape. The descent speed is updated for the next fluid mechanics computation and iterations between fluid and structural mechanics computations continue until the parachute shape settles. At this point, a final fluid mechanics computation is performed to obtain a developed flow field.

Newton–Raphson iterations are used to update the descent speed based on

\[ W - F_D = 0 , \]  

(3.8)

\[ F_D = \frac{1}{2} C_D \rho U^2 A . \]  

(3.9)

Here \( W, F_D, C_D, \rho, U \) and \( A \) are the total weight, drag force, drag coefficient, air density, descent speed and projected area (based on the diameter measured at the parachute skirt). The Newton–Raphson iterations are based on first writing

\[ \frac{1}{2} C_D \rho \left( 2 U^i A^i \Delta U^i + (U^i)^2 \frac{\partial A}{\partial U} \Delta U^i \right) = W - F_D^i . \]  

(3.10)
where $i$ is the iteration counter and $A^i$ is the area corresponding to the parachute shape used in computing $F_D^i$. Assuming that $C_D$ does not change in the range of Reynolds numbers experienced, Eq. (3.9) can be rewritten as

$$\frac{1}{2} C_D \rho = \frac{F_D^i}{(U^i)^2 A^i}$$  \hspace{1cm} (3.11)

and based on that rewrite Eq. (3.10) as

$$\frac{F_D^i}{(U^i)^2 A^i} \left( 2U^i A^i \Delta U^i + (U^i)^2 \frac{\partial A}{\partial U} \Delta U^i \right) = W - F_D^i,$$  \hspace{1cm} (3.12)

which simplifies to

$$\left( 2A^i + U^i \frac{\partial A}{\partial U} \right) \Delta U^i = \frac{U^i A^i (W - F_D^i)}{F_D^i},$$  \hspace{1cm} (3.13)

where

$$\frac{\partial A}{\partial U} = \frac{A^{i+1} - A^i}{U^i - U^{i-1}}.$$  \hspace{1cm} (3.14)

This process starts with $A^1$ and $U^1$, using Eq. (3.14) for $i \geq 2$, and setting $\frac{\partial A}{\partial U} \mid_1 = 0$.

The parachute shape, descent speed and the developed flow field obtained with the sequence of stand-alone computations described above are used as the starting point for the symmetric FSI step used to obtain the results in Chapter 4. The shape and descent speed were also the basis of the four-gore model used to generate the HMGP equivalent, locally-varying fabric porosities listed in Table 4.1.

### 3.5 Symmetric FSI

The “symmetric FSI” technique [27, 28] is rather helpful in generating a good starting point for fully coupled parachute computations. The symmetric FSI step is similar
to fully coupled FSI, except that a circumferentially-averaged fluid interface stress is projected to the structure based on \((h^h_{ii})_{AVE}\) (see Section 2.5 for subscript notation). This helps build a good starting point, which can be a lengthy process, with a periodic breathing motion but without unsymmetric parachute deformation or gliding. After the symmetric FSI period, the circumferentially-averaged stress projection is ramped to a spatially accurate projection using \((1 - r_S)h^h_{ii} + r_S(h^h_{ii})_{AVE}\), where \(r_S\) is gradually varied from 1.0 to 0.0. This “desymmetrization” period occurs over approximately 7 s, during which \(r_S\) varies from 1.0 to 0.0 in a Cosine form. For expedited implementation, the computations reported in this thesis use only the pressure component of the interface stress, \(-p^h_i n\), for the symmetrization of the interface stress projected to the structure and the desymmetrization with the parameter \(r_S\). This expedited implementation was motivated by the SSP technique described in Section 3.2.

A symmetric FSI computation would of course benefit from having a good starting point of its own. The shape and descent speed components of the starting point can be obtained from the alternating sequence of stand-alone structural mechanics and fluid mechanics computations described in Section 3.4. The developed-flow component of the starting point is obtained with a stand-alone fluid mechanics computation based on the starting shape and descent speed.

### 3.6 Rotational Periodicity

Periodic boundary conditions can be used to reduce computational costs in problems with geometric and solution periodicity. The circular ringsail parachute exhibits such periodicity in the circumferential direction. Modeling only a small slice of the entire domain with rotationally periodic boundary conditions reduces the computational cost to such a level that significant mesh refinement can be afforded. The rotationally periodic boundaries are generated by applying a rotational transformation to one boundary to yield a secondary periodic boundary with the the same element
connectivity and equivalent nodal coordinates after rotation. Scalar quantities are equivalent at mapped nodes on the periodic boundaries. Vector quantities are transferred between mapped nodes using the same rotational transformation applied to the periodic boundary geometry. The rotational transformation matrix is

\[
R_z(\alpha) = \begin{pmatrix}
\cos \alpha & \sin \alpha & 0 \\
-\sin \alpha & \cos \alpha & 0 \\
0 & 0 & 1
\end{pmatrix}
\]  

(3.15)

where \(\alpha\) is the counter-clockwise angle between the mapped periodic boundaries about the \(z\)-axis.

### 3.7 Spatially Multiscale Sequentially-Coupled FSI

The spatially multiscale techniques described herein were developed to reduce the computational cost of FSI computations where highly refined meshes are not required, but obtain more accurate fluid or structural mechanics results with refined meshes when needed. This is accomplished by performing FSI computations with a fluid and structure mesh sufficient for accurately determining the behavior of the parachute system and relevant aerodynamic quantities. When more accurate fluid (or structural) mechanics results are sought, the fluid (or structure) mesh is refined and structure (or fluid) data from the previously carried out FSI computation is used as prescribed input.

A spatially multiscale Sequentially-Coupled FSI (SCFSI) technique for fluid mechanics, designated “SCFSI M1C”, was introduced in [25, 26]. A similar spatially multiscale SCFSI technique was developed for structural mechanics in [27, 28] and designated “SCFSI M2C”. In the SCFSI M2C technique, a fully coupled FSI computation is first performed with a relatively coarser structure mesh to capture the time-dependent flow field. The time-dependent interface stresses from this flow field...
are then used in a stand-alone structural mechanics computation with a more refined structure mesh. In this manner, more accurate structural mechanics results can be computed as occasionally needed without regularly burdening the FSI computations with an overly refined structure mesh.
Chapter 4

PA, PM5, PM11 Design

Comparison

The descent of the main parachute during the full open stage is dynamic and an important dynamic feature in this stage of parachute flight is the canopy breathing motion. This breathing motion causes oscillations in the projected drag area of the canopy and thus oscillations in descent speed. The goal of this investigation is to reduce oscillations in the descent speed of the payload and also reduce the horizontal gliding of a single parachute. The oscillations in descent speed directly affect the maximum descent speed of the payload, which must be limited to ensure the landing force is within acceptable limits for the crew. The horizontal gliding of a single parachute has cluster performance implications, as excessive gliding is indicative of large coning angles and parachute crashing and fly-out in the cluster configuration, both of which cause drag reductions. Reconfiguring the canopy by changing the distribution of geometric porosity impacts both stability and drag performance of the parachute. In this study, the relationship between the distribution of geometric porosity and parachute stability and drag performance is analyzed after removing various sails from the canopy.
4.1 Design Variations

A total of three cases are investigated, including a baseline and two alternate canopy configurations. The two alternate configurations are “missing” the 5th and 11th sail, respectively, when numbered starting from the canopy apex with rings included. The three parachutes are referred to as “PA” (all sails are in place), “PM5” (missing the 5th sail) and “PM11” (missing the 11th sail). Since only one parachute of the three parachute cluster is modeled, a point mass representing $\frac{1}{3}$ of the CEV mass is used as the payload. The total system weight, including the parachute, for PA, PM5 and PM11 is approximately 5,725 lbs, 5,720 lbs and 5,715 lbs, respectively.

The homogenized porosity distribution used here is based on the HMGP approximation for a computation reported earlier by the T*AFSM in [28]. The homogenized porosity coefficients for the 12 patches of the parachute are given in Table 4.1. For cases with a missing sail, the gap created by removing the sail is modeled in the fluid interface. The porosity coefficient at the edges facing a missing sail is set to the material fabric porosity at that location, with porosity linearly progressing to the homogenized value at the adjacent patch. Figure 4.1 shows the homogenized porosity distribution at the fluid interface each of the three cases.

<table>
<thead>
<tr>
<th>Patch</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>10</th>
<th>11</th>
<th>12</th>
</tr>
</thead>
<tbody>
<tr>
<td>CFM</td>
<td>314</td>
<td>278</td>
<td>201</td>
<td>157</td>
<td>59</td>
<td>66</td>
<td>62</td>
<td>79</td>
<td>107</td>
<td>145</td>
<td>150</td>
<td>149</td>
</tr>
</tbody>
</table>

Table 4.1: HMGP equivalent porosity coefficients for the 12 patches of the parachute.

4.2 Computational Conditions

All computations reported here are carried out in a parallel computing environment, using PC clusters. In all cases, the fully-discretized, coupled fluid and structural mechanics and mesh-moving equations are solved with the quasi-direct coupling technique (see Section 5.2 in [22]). In solving the linear equation systems involved at
every nonlinear iteration, the GMRES search technique [11] is used with a diagonal preconditioner. The meshes are partitioned to enhance the parallel efficiency of the computations. Mesh partitioning is based on the METIS [7] algorithm. The computations are carried out using SSTFSI-TIP1 technique (see Remarks 4 and 6), with the SUPG test function option WTSA (see Remark 1). The stabilization parameters used are those given in Eqs. (2.18)–(2.19) and (2.13)–(2.17), with the $\tau_{\text{SUPG}}$ term dropped from Eq. (2.18). The interface-stress projection is based on the SSP technique described in Section 3.2. The time-step size is 0.0232 s. The number of nonlinear iterations per time step is 6, and the number of GMRES iterations per nonlinear iteration is 90 for the fluid and structural mechanics parts and 30 for the mesh moving part. Selective scaling (see [22]) is used, with the scale for the structure part set to 10. All computations are carried out at a Re value of approximately 18 million and use properties of air at standard sea-level conditions.

In addition to moving the reference frame vertically with a reference descent speed, as originally proposed in [28], the domain moves horizontally and vertically based on the average displacement rate for the structure. The ability of the domain to translate horizontally becomes particularly helpful when the parachute glides significantly. With a domain that moves horizontally, the velocity form of the free-stream conditions are used at the lateral boundaries in addition to the inflow boundary. To prevent
the specified free-stream velocity on the lateral boundary from influencing flow near the parachute, the computational domain is expanded laterally compared to previous domains with slip conditions on the lateral boundaries [24, 23]. The dimensions of this larger computational domain, in ft, are 1,740×1,740×1,566. This approach reduces mesh stretching in the horizontal direction when the parachute glides significantly and therefore reduces the need for remeshing. All computations reported here were completed without any remeshing. Missing sails are modeled by removing the appropriate fluid interface elements and creating six fluid volume elements across the span of the missing-sail gap. The number of nodes and elements for the PA, PM5 and PM11 meshes are given in Table 4.2. Figure 4.2 shows the structure mesh for each configuration at its average canopy shape.

<table>
<thead>
<tr>
<th></th>
<th>PA</th>
<th>PM5</th>
<th>PM11</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Structure</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td><em>nn</em></td>
<td>30,722</td>
<td>28,642</td>
<td>28,082</td>
</tr>
<tr>
<td>Membrane <em>ne</em></td>
<td>26,000</td>
<td>24,080</td>
<td>23,600</td>
</tr>
<tr>
<td>Cable <em>ne</em></td>
<td>12,521</td>
<td>11,401</td>
<td>12,121</td>
</tr>
<tr>
<td>Payload <em>ne</em></td>
<td>1</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Interface <em>nn</em></td>
<td>29,200</td>
<td>27,120</td>
<td>26,560</td>
</tr>
<tr>
<td><em>ne</em></td>
<td>26,000</td>
<td>24,080</td>
<td>23,600</td>
</tr>
<tr>
<td><strong>Fluid</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Volume <em>nn</em></td>
<td>178,270</td>
<td>192,412</td>
<td>180,917</td>
</tr>
<tr>
<td><em>ne</em></td>
<td>1,101,643</td>
<td>1,192,488</td>
<td>1,119,142</td>
</tr>
<tr>
<td>Interface <em>nn</em></td>
<td>2,140</td>
<td>2,060</td>
<td>2,060</td>
</tr>
<tr>
<td><em>ne</em></td>
<td>4,180</td>
<td>3,860</td>
<td>3,860</td>
</tr>
</tbody>
</table>

Table 4.2: Number of nodes and elements for the PA, PM5 and PM11 parachute configurations. Here *nn* and *ne* are number of nodes and elements, respectively. The structural mechanics mesh consists of four-node quadrilateral membrane elements, two-node cable elements and one-node payload element. The structure interface mesh consists of four-node quadrilateral elements. The fluid volume mesh consists of four-node tetrahedral elements, while the fluid interface mesh consists of three-node triangular elements.
4.3 Starting Condition

A consistent starting condition is essential for making accurate comparisons in many FSI applications. Starting conditions are especially important when investigating unsteady features. A number of techniques for building FSI starting conditions for parachutes are reported in [23, 28]. These techniques mostly focus on starting the FSI computations gently. The purpose of further improving the starting condition with the methods introduced here is primarily related to making the starting conditions consistent and matching NASA drop test observations.

To build an appropriate starting point for comparing performance of various
parachute designs, data from a single main parachute drop test conducted by NASA was analyzed and compared with earlier FSI computations [28]. Based on this analysis, a fully inflated parachute with all sails behaves as follows: the parachute exhibits a periodic breathing motion with vortex shedding, and this dynamic change in parachute drag area results in a fluctuating descent speed. Furthermore, during drop tests the wind direction and magnitude is dependent on altitude, meaning the parachute experiences a variable wind field as it descends. In steady wind conditions, however, the parachute settles to a nearly constant gliding direction after a few seconds. Moreover, when this is the case, the speed of the parachute relative to the surrounding air is the dominant parameter for the flow condition, and any lateral force the parachute sees from the wind is only a function of the relative speed.

The computation reported in [28] does not include any side wind, but the parachute exhibits a relative velocity comparable to the drop test because the parachute is gliding. Table 4.3 provides a comparison of the drop test data and the earlier computations reported in [28].

<table>
<thead>
<tr>
<th></th>
<th>$V_D$ (ft/s) $V_{RH}$ (ft/s) $T_B$ (s) $T_S$ (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Test data</td>
<td>21.5</td>
</tr>
<tr>
<td></td>
<td>5 to 15</td>
</tr>
<tr>
<td></td>
<td>7.3</td>
</tr>
<tr>
<td></td>
<td>16.2</td>
</tr>
<tr>
<td>Computation</td>
<td>22.0</td>
</tr>
<tr>
<td></td>
<td>4 to 12</td>
</tr>
<tr>
<td></td>
<td>7.0</td>
</tr>
<tr>
<td></td>
<td>NA</td>
</tr>
</tbody>
</table>

Table 4.3: Comparison between test data and computational result reported in [28]. Symbols $V$ and $T$ denote velocities and periods. Subscripts “D” and “RH” denote descent and relative horizontal. Subscripts “B” and “S” denote breathing and swinging. NA: not applicable.

Based on the argument above, it is inferred that the main influence of an unsteady side wind is to contribute to the payload swing. Instead of adding a variable wind to the computation, an instantaneous “payload swing” effect is introduced by giving the payload a one-time horizontal velocity hike, in a way that not only accounts for the variable wind contribution but emulates the aggregate swinging motion observed in the drop tests. The magnitude of the horizontal velocity hike is determined by
computing with several hike magnitudes and comparing the results with drop test data, which results in a suitable horizontal velocity hike magnitude of 20 ft/s. Figure 4.3 shows that introducing a 20 ft/s horizontal velocity hike to the payload more accurately models the flight conditions experienced by the payload.

Figure 4.3: Payload horizontal speed shown for single parachute drop test and FSI computations with and without 20 ft/s payload horizontal velocity hike.

More details are now provided on the sequence for arriving at a starting point for FSI computations. Shape determination iterations are used to find the starting canopy shape, descent speed, and flow field. These conditions are used as a starting point for symmetric FSI computations, described in Section 3.5. After 100 s of symmetric FSI, the parachute reaches a settled periodic breathing stage that is consistent between the various parachute designs computed. The payload and the parachute have no horizontal speed at the end of the symmetric FSI step, which does not match what is observed in the drop test. To emulate the swinging motion observed in drop tests, the horizontal speed of the payload is instantaneously increased by 20 ft/s.
Simultaneously, the desymmetrization (see [28]) begins, using a Cosine form which lasts for one breathing period (7 s). Although the vortex shedding pattern behind the parachute is not exactly the same in each computation, the momentum added to the payload with this horizontal velocity hike is consistent. By emulating the payload momentum in this fashion, the starting condition for fully coupled FSI represents actual flight conditions with reasonable closeness.

4.4 Results

A more detailed description of the periodic canopy breathing motion is given here based on the PA baseline results shown in Figure 4.4. At minimum projected area in the breathing cycle, the drag is also at a minimum due to the low projected area of the canopy. The descent speed is relatively high as the parachute is accelerating due to the decreasing drag. As the canopy expands and drag increases, the descent speed reaches a maximum value. The drag reaches a maximum value just as the descent speed begins to decrease and as the projected area continues to build. This is because drag is a function of projected area and descent speed, with descent speed predominant, based on Eq. (3.9). As the descent speed rapidly decreases, causing a large reduction in drag, the maximum skirt diameter is achieved and flow separating near the skirt creates a vortex ring. The low pressure above the canopy generated by the vortex and high pressure under the canopy create a large pressure differential at the skirt. As the vortex ring advects downstream, the descent speed and projected area continue to decrease. At approximately the minimum descent speed and average projected area of the cycle, the large pressure differential created by the vortex ring travels high enough on the canopy to exert a large drag force in the vertical direction. This secondary drag peak introduced by the shedding vortex ring offsets the low dynamic pressure contributed by the minimum descent speed, thus delaying minimum drag until the projected area is also at a minimum and descent speed is increasing.
The breathing cycle then repeats as the canopy once again expands.

Figure 4.4 depicts the aforementioned relationship between canopy vertical projected area, vertical drag component, and descent speed for the baseline PA case. The projected area is calculated by integrating the vertical projected areas of structure interface elements with positive vertical normal components. Elements with negative vertical normal components are not included to prevent adding area already within the projected area if the skirt diameter is less than the maximum canopy diameter, as is the case for the last sail of PA in Figure 4.2. Drag is calculated by integrating pressure on the structure interface, with only the vertical component graphed. Canopy descent speed is calculated by averaging the descent speed of all structure interface nodes. Figure 4.5 depicts the breathing motion by showing the pressure field, canopy pressure differential, and canopy displacement over one breathing cycle.

With a more detailed understanding of the relationship between canopy projected area, drag, and descent speed, results from the three parachute configurations are now presented. Figure 4.6 shows the parachute and flow field for each configuration when the tilt angle, as measured between the vertical axis and a line connecting the payload and vent, is at a maximum. Table 4.4 provides a comparison between test data and results for the three parachute configurations. The PA results very closely match

<table>
<thead>
<tr>
<th></th>
<th>$V_D$ (ft/s)</th>
<th>$V_{RH}$ (ft/s)</th>
<th>$T_B$ (s)</th>
<th>$T_S$ (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Test data</td>
<td>21.5</td>
<td>5 to 15</td>
<td>7.3</td>
<td>16.2</td>
</tr>
<tr>
<td>PA</td>
<td>21.4</td>
<td>4 to 13</td>
<td>6.7</td>
<td>16.4</td>
</tr>
<tr>
<td>PM5</td>
<td>24.0</td>
<td>4 to 13</td>
<td>5.8</td>
<td>16.6</td>
</tr>
<tr>
<td>PM11</td>
<td>29.0</td>
<td>0 to 4</td>
<td>NA</td>
<td>17.0</td>
</tr>
</tbody>
</table>

Table 4.4: Comparison between test data and computational results for the PA, PM5 and PM11 parachute configurations. Computational quantities averaged during the period 40–120 s, where 0 s corresponds to beginning of desymmetrization.

test data, verifying that the 20 ft/s payload horizontal velocity hike is a valid means of representing the payload swing present in the drop test. The PM5 configuration
has a higher descent speed and breathing frequency with no change in the relative horizontal velocity. The PM11 configuration has a much high descent speed with no significant breathing motion and a very low relative horizontal velocity.

Figure 4.7 shows the time-dependent canopy descent speed, payload descent speed, and skirt diameter for each of the three parachutes. As previously detailed, the canopy descent speed and canopy diameter are coupled since drag is a function of both. It can be seen from the plots that PA and PM5 have similar maximum diameters. Even though the diameters are close, PM5 has a slightly higher average descent speed because of the decreased projected area due to the missing sail. PA and PM5 have similar maximum diameters because the pressurization of the lower sails is unaffected by removing the 5th sail. The drag performance of PM11 is hindered by the relatively small projected area due to the loss of pressurization of the removed sail and bottom two sails.

Figure 4.8 shows the time-dependent payload and vent horizontal-velocity magnitudes for the three configurations. The payload horizontal velocity takes into account contributions from swinging and gliding. The vent horizontal velocity, which is dominated by the gliding component, provides a good measure of the horizontal velocity of the entire parachute system and is obtained by averaging the velocities of all nodes at the vent edge. In this study, improved static stability is defined as a lower gliding speed. PA and PM5 have larger gliding speeds while the glide speed of PM11 is very low. Thus, the static stability of PM11 is much improved over that of PM5 and PA. The horizontal velocity magnitude for the PM11 payload exhibits sharp kinks near 0 ft/s, indicating that the horizontal velocity is reversing direction. This reversal happens when, in a swinging cycle, the tilt angle is maximum. On the other hand, the PA and PM5 payloads are moving on a more elliptical trajectory with respect to the vent, as clearly seen in Figure 4.9, and therefore do not exhibit many sharp kinks.
Overall, this study shows that more stable configurations exhibit a loss of drag. To noticeably improve the stability of a parachute, a significant amount of drag must be traded. Furthermore, in this study, reducing the amplitude of canopy descent speed oscillations does not decrease maximum payload descent speed for two reasons. Firstly, removing a sail increases the average parachute descent speed so that even smaller oscillation amplitudes result in greater maximum descent speeds for the parachute system. Secondly, the descent speed constraint is based on the payload where the crew resides. When the parachute exhibits a payload swing, the magnitude of the swing velocity, rather than velocity contributions from the breathing dynamic, dominates the maximum payload descent speed. However, the study does show that parachute performance can change significantly based on the location of geometric porosity changes. Therefore, a more optimal distribution of geometric porosity may exist that allows for better optimization of stability and drag.
Figure 4.4: Vertical projected area, vertical drag component, and descent speed of the PA canopy. Solid vertical lines indicate time of maximum drag, with subsequent drag peak due to vortex ring. Dashed vertical lines indicate time of minimum drag. The horizontal line on the drag plot represents the total system weight.
Figure 4.5: Two breathing cycles shown for the PA baseline case with velocity vectors colored by magnitude, pressure shown on cut plane, and canopy pressure differential shown on canopy. Images progress from left to right, then top to bottom and range from approximately 84–97 s with an increment of approximately 2 s. Note that the camera is fixed on the canopy, which is gliding to the right with the payload swinging out of the plane.
Figure 4.6: Parachute and flow field for PA, PM5 and PM11 when the tilt angle is at a maximum.
Figure 4.7: Canopy descent speed (top), payload descent speed (middle), and skirt diameter for the PA, PM5 and PM11 parachute configurations. The thin vertical line at 7 s marks the end of the desymmetrization.
Figure 4.8: Horizontal-velocity magnitude for the payload (top) and vent (bottom) for the PA, PM5 and PM11 parachute configurations. The thin vertical line at 7 s marks the end of the desymmetrization.
Figure 4.9: Payload trajectories (relative to the vent) for PA (top), PM5 (middle) and PM11 (bottom). Lines are drawn from 60 to 120 s. Dots are placed every 2.3 s. Note that the $y$ scale is stretched to twice that of the $x$ scale.
Chapter 5

Periodic $n$-gore Computations

The constructed structure geometry of the ringsail parachute exhibits rotational periodicity, as shown in Figure 1.2. This rotational periodicity can be exploited to obtain a more accurate local fluid mechanics solution by creating a domain with rotationally periodic boundary conditions, described in Section 3.6, that models only a fraction of the total 80 canopy gores. Modeling only a $n$-gore slice of the entire domain reduces the computational cost to such a level that increasing the fluid mechanics mesh refinement near the canopy to model the geometrically true structure becomes feasible. A periodic $n$-gore computation that models the actual structure geometry provides much better resolution of local flow behavior than can be achieved by a computation with a smoothened fluid interface and HMGP equivalent porosity, as seen in Figure 5.1.

Although a periodic $n$-gore computation better resolves local flow behavior by representing the actual structure geometry, it is not sufficient when considering global quantities such as drag or descent speed. This is because the periodic boundary conditions treat vortex shedding at the skirt as a rotationally periodic phenomena. Based on empirical data and computational results, it is observed that the initial vortex strength and velocity varies circumferentially, especially in the case of an unconstrained parachute that is gliding, swinging, or experiencing asymmetric deformation.
Figure 5.1: Flow field comparison for HMGP computation with smoothened fluid interface (top) and periodic n-gore computation with geometrically true fluid interface (bottom).

Even parachutes constrained to symmetric deformation with zero horizontal velocity and no wind exhibit asymmetric vortex shedding, as seen in symmetric FSI computations. Furthermore, vortex rings that may initially appear symmetric rapidly distort as they advect downstream in the unsteady parachute wake. By enforcing flow periodicity in the parachute wake, periodic boundary conditions simplify the complex wake interactions that can affect parachute performance, thus preventing accurate determination of global aerodynamic quantities.

In summary, flow past the actual parachute geometry can be modeled using rotationally periodic boundary conditions for the sake of reducing computational cost.
This approach allows for a more accurate flow solution near the canopy but sacrifices the physical representation of flow elsewhere in the wake. The periodic $n$-gore computations presented in this thesis are used to calculate fabric and geometric porosity coefficients for the improved HMGP-FG technique.

5.1 Advancements for $n$-gore Computations

A four-gore fluid mechanics computation with slip boundary conditions instead of rotational periodicity conditions on the lateral boundaries was previously carried out in [23] to calculate HMGP porosity coefficients. The periodic $n$-gore computations described in this thesis are improved from the previous slip-condition four-gore computation in several areas. Firstly, rotational periodicity conditions permit flow through the lateral boundaries of the periodic $n$-gore model. The four-gore computation with slip-conditions does not allow flow to cross the lateral boundaries, which introduces an additional constraint. Secondly, the periodic $n$-gore model described here and used to calculate HMGP-FG fabric and geometric porosity coefficients is computed with the DSD/SST formulation, whereas the four-gore model with slip-conditions previously used with the HMGP technique is computed using a semi-discrete formulation (see [16]). The space–time formulation integrates over time whereas the semi-discrete formulation does not, meaning the periodic $n$-gore results, if computed with the “-SP” option (see Remarks 2 and 3), are more time-accurate than the slip-condition four-gore results. Thirdly, the fluid interface mesh of the periodic $n$-gore model is much more refined than that of the slip-condition four-gore model. Higher interface mesh refinement provides for better flow resolution near the canopy. Figure 5.2 shows a comparison of the fluid interface meshes and Figure 5.3 shows a comparison of the fluid volume meshes near the canopy for the periodic and slip-condition $n$-gore models.
5.2 Periodic $n$-gore Mesh Generation

FSI data from the PA case in Chapter 4 is used to obtain canopy shapes for the periodic $n$-gore computations in this chapter. At a single time step, the pressure across the fluid interface is calculated and circumferentially averaged to obtain a symmetric pressure distribution. To obtain several different canopy shapes, symmetrized pressure distributions are calculated at various skirt diameters so that the pressure distributions inflate the canopy to different shapes seen during a breathing cycle. Next, the circumferentially averaged pressures on the FSI smoothened fluid interface
Figure 5.3: Lateral boundary of fluid volume near canopy for the slip-condition (left) and periodic (right) \( n \)-gore models.

mesh are projected using SSP to a heavily refined structure mesh with four-node quadrilateral elements. A stand-alone structural mechanics computation is carried out using the undeformed, refined structure mesh and symmetric pressure calculated from FSI data. Once the canopy shape reaches a steady-state solution, the four-node quadrilateral elements of the structure mesh are subdivided to create a fluid interface mesh composed of three-node triangle elements that models the actual parachute geometry. All \( n \)-gore computations in this thesis use the same fluid interface mesh refinement, shown at the bottom of Figure 5.2, which has 464,960 nodes and 900,160 three-node triangular elements for an 80-gore canopy.

Next, one gore is extracted from the refined fluid interface and a specified number of triangles are created across the gaps and slits of this single-gore mesh. A surface mesh is then created for the primary lateral boundary. By applying the rotational transformation shown in Eq. (3.15) with \( \alpha = 4.5^\circ \) to the nodal coordinates of the primary boundary, a secondary lateral boundary is created with similar nodal coordinates and element connectivity. The remaining domain boundaries are then created and the fluid volume mesh is generated. Ultimately the parachute vent and outflow boundary are located 290 ft and 1,450 ft downstream of the inflow boundary, respectively, and the radial boundary is located 290 ft from the vent center.
A one-gore model cannot be used for computations since the fluid volume mesh may contain elements consisting of both primary and secondary boundary nodes. This is not ideal since the velocity and pressure in such elements could rapidly propagate repeatedly across the domain if not disrupted by flow elsewhere in the domain. To ensure at least two elements exist between primary and secondary boundary nodes, copies of the single-gore mesh are rotated in 4.5° increments and the overlapping boundary nodes are merged to create a $n$-gore fluid volume mesh, where $n > 1$. Finally, nodes on the fluid-structure interface are split (see Remark 5) so that the pressure differential across the canopy can be modeled. A four-gore fluid volume domain created using this mesh generation method is shown in Figure 5.4.

![Figure 5.4: Periodic four-gore domain.](image-url)
5.3 HMGP-FG Porosity Coefficient Determination

The main focus of developing an improved n-gore model is to accurately calculate the HMGP-FG fabric and geometric porosity coefficients. Since the manufactured fabric porosity of the canopy material is known, the HMGP-FG fabric porosity coefficients for each patch can be approximated by integrating the material porosity over the patch area and dividing by the total patch area. Calculating the HMGP-FG geometric porosity coefficients for each patch is more complicated and requires a fluid mechanics solution, including the volumetric flowrates through the gaps and slits, average pressure differential across the fabric of each patch, and various patch areas. Variations in these quantities, especially volumetric flowrate, can have a large impact on the calculated geometric porosity coefficient. As such, it is important to ensure the fluid mechanics solution is adequately converged. A detailed examination of periodic n-gore solution convergence based on mesh refinement, time accuracy and number of gores modeled is presented in Sections 5.3.1–5.3.3. The results of these convergence studies are used to determine geometric porosity coefficients for the different gap and slit shapes encountered during canopy breathing in the full open stage. Section 5.3.4 details the computations used to calculate the HMGP-FG fabric and geometric porosity coefficients for several full open canopy shapes. The superiority of the HMGP-FG technique compared to the HMGP technique is demonstrated in Section 5.3.5, where porosity coefficient dependence on pressure is examined.

5.3.1 Mesh Refinement Convergence

The stand-alone fluid mechanics computations presented in this section are carried out in a parallel computing environment, using PC clusters. In solving the linear equation systems involved at every nonlinear iteration, the GMRES search technique [11] is used with a diagonal preconditioner. The meshes are partitioned to enhance the parallel efficiency of the computations. Mesh partitioning is based on the METIS [7]
algorithm. The computations are carried out using the DSD/SST-TIP1 technique (see Remarks 3 and 4), with the SUPG test function option WTSA (see Remark 1). The stabilization parameters used are those given in Eqs. (2.18)–(2.19) and (2.13)–(2.17), with the $\tau_{\text{SUPG}}$ term dropped from Eq. (2.18). The interface-stress projection is based on the SSP technique described in Section 3.2. The time-step size is 0.0232 s. The number of nonlinear iterations per time step is 6, and the number of GMRES iterations per nonlinear iteration is 90. Selective scaling (see [13, 22]) is used, with the scale for the equations corresponding to the incompressibility constraint set to 10. All computations are carried out at a Re value of approximately 18 million and use properties of air at standard sea-level conditions.

A periodic four-gore domain, exemplified in Figure 5.4, is used for these computations. A four-gore model was selected so that several elements exist between primary and secondary boundary nodes while keeping the computational cost at a minimum. At the inflow all velocity components are specified and at the outflow all stress components are set to zero. Periodic boundary conditions are used for the lateral boundaries that intersect the canopy and for the radial boundary a slip condition with horizontal velocity components set to zero is used. A freestream velocity of 26 ft/s is specified at the inflow boundary and as the initial condition for interior fluid nodes, except those on the fluid-structure interface for which all velocity components are set to zero.

Four different fluid mechanics meshes are used to investigate the effect of local and global mesh refinement on the HMGP-FG porosity coefficients. Local mesh refinement refers to the number of fluid volume elements across each gap and slit while global mesh refinement refers to the total relative number of fluid volume elements in the domain. Mesh 1 and Mesh 2 have a local refinement of 12 and 18 elements, respectively, and similar global refinement. Mesh 3 and Mesh 4 have a local refinement of 8 and 12 elements, respectively, and double the global refinement of Meshes 1 and 2. A comparison of global mesh refinement near the canopy for Mesh 1 and Mesh 4
is shown in Figure 5.5. A local mesh refinement of 8, 12 and 18 elements across each gap and slit is depicted in Figure 5.6. All meshes are based on the same geometrically accurate, four-gore fluid interface mesh with 23,630 nodes and 45,008 three-node triangular elements. The shape of this fluid interface mesh was calculated using a symmetric pressure distribution taken from FSI data when the skirt diameter was at an average value and decreasing. The resulting fluid interface mesh has a skirt diameter of 76 ft, which is slightly smaller than the average skirt diameter of approximately 80 ft. The total number of fluid volume nodes and elements, along with the number of nodes on the periodic boundary, is shown in Table 5.1 for each of the four meshes.

A stand-alone fluid mechanics computation for each mesh is performed using the previously mentioned semi-discrete formulation [16] until the flow field becomes developed and exhibits vortex shedding. The semi-discrete computations are performed simply to speed up the development of an initial flow field. The computations are then continued for approximately 1,700 time steps (40 s) using the DSD/SST-TIP1 formulation. The HMGP-FG porosity coefficients are calculated for each patch using data averaged over the final 300 time steps (7 s) of the space-time computation. The flow field for each mesh is shown in Figure 5.7 and the calculated fabric and geometric
Figure 5.6: Local refinement of 8, 12 and 18 elements across each gap and slit shown from top to bottom. Ring gap shown on left is between Ring 4 and Sail 1 and sail slit shown on right is between Sail 3 and Sail 4.

<table>
<thead>
<tr>
<th>Local Refinement</th>
<th>Mesh 1</th>
<th>Mesh 2</th>
<th>Mesh 3</th>
<th>Mesh 4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fluid Volume</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$nn$</td>
<td>367,723</td>
<td>379,588</td>
<td>706,542</td>
<td>722,326</td>
</tr>
<tr>
<td>$ne$</td>
<td>2,002,804</td>
<td>2,076,460</td>
<td>3,956,468</td>
<td>4,050,468</td>
</tr>
<tr>
<td>Periodic</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$nn$</td>
<td>12,047</td>
<td>12,344</td>
<td>24,578</td>
<td>24,954</td>
</tr>
</tbody>
</table>

Table 5.1: Number of fluid volume nodes and elements and periodic boundary nodes for each of the periodic four-gore meshes used in the mesh convergence study. The fluid volume mesh consists of four-node tetrahedral elements. Periodic $nn$ is for one periodic boundary, so that $nn$ is the number of periodic node sets mapped between the two lateral boundaries.

Porosity coefficients are presented in Figure 5.8. As expected, the homogenized fabric porosity distribution is the same for all meshes. However, the geometric porosity coefficients vary, especially for Patches 3–5 of Mesh 2. The drop in $k_G$ is caused by
Figure 5.7: Velocity magnitude near canopy shown on cut plane for Mesh 1–4, from left to right and top to bottom.

a low volumetric flowrate through the gaps of Patches 3–5 despite a high pressure differential in this region, as seen in Figure 5.9. The decrease in volumetric flowrate through the ring gaps of Mesh 2 is attributed to high element aspect ratios in the gap region, which cause convergence issues. Other than Mesh 2, the remaining $k_G$ coefficients are very close. The sole difference between Mesh 1 and Mesh 4 is the extra global mesh refinement of Mesh 4, indicating that additional global mesh refinement does not improve $k_G$ convergence. Furthermore, Mesh 3 and Mesh 4 only differ in the number of local refinement elements, indicating that the solution has adequately converged for the lower local refinement of 8 gap elements. In summary, the results of this mesh refinement study indicate that a local mesh refinement of 8 elements and a standard global mesh refinement such as that shown in the left image of Figure 5.5
is sufficient for calculating geometric porosity coefficients.

Figure 5.8: Fabric and geometric porosity coefficients for the four mesh refinement cases.
Figure 5.9: Patch-based gap and slit volumetric flowrate and interface pressure differential for the four mesh refinement cases.
5.3.2 Temporal Convergence

As discovered in the mesh refinement study of the previous section, geometric porosity coefficients are sensitive to changes in gap and slit volumetric flowrate. Thus, another convergence study is performed to ensure that the most accurate volumetric flowrates are used to calculate the geometric porosity coefficients. The focus of this convergence study is to increase the time accuracy of the computations by decreasing the time-step size and increasing the temporal order of integration. Two cases are computed, one using Mesh 1 for comparison with the spatially converged solution of 12 local refinement elements and the other using Mesh 2 to determine whether increased time accuracy can improve the volumetric flowrate solution attributed to poor mesh quality. The computational conditions for these two cases are the same as described in Section 5.3.1 with two exceptions. The number of integration points in time is increased to two by employing the DSD/SST-SP technique and the time-step size is decreased by a factor of four to 0.0058 s.

A stand-alone fluid mechanics computation for each mesh is performed using the semi-discrete formulation [16] until the flow field becomes developed and exhibits vortex shedding. The computations are then continued for approximately 1,700 time steps (10 s) using the DSD/SST-SP formulation. The HMGP-FG porosity coefficients are calculated for each patch using data averaged over the final 300 time steps (2 s) of the space-time computation. The flow fields for these two cases are shown in Figure 5.10 and the calculated porosity coefficients are plotted along with the original Mesh 1 and Mesh 2 “-TIP1” results in Figure 5.11. Again the fabric porosity does not change since the volumetric flowrate through the fabric is governed by the specified material porosity. However, the volumetric flowrate through the gaps and slits is not constrained by a constitutive relationship, and thus increases with time-accuracy, resulting in higher geometric porosity coefficients. This indicates that more accurate geometric porosity coefficients can be calculated from periodic n-gore computations.
that use the DSD/SST-SP technique and a smaller time-step size. The individual contribution of each condition to the overall temporal convergence is unknown. It is also not known how a further decrease in time-step size would affect the geometric volumetric flowrate. However, for the purpose of this thesis, using the DSD/SST-SP technique with a time step of 0.0058 s is deemed sufficient for calculating the geometric porosity coefficients with reasonable accuracy.
Figure 5.11: Fabric and geometric porosity coefficients for the "-TIP1" and "-SP" computations of Mesh 1 and Mesh 2.
5.3.3 Gore Convergence

Despite being less restrictive than slip-conditions, rotationally periodicity still constrains the flow to periodic behavior. In this section, the periodicity constraint enforced on the parachute is relaxed by increasing the number of gores modeled in the n-gore domain. The computational conditions used here are the same as those described in Section 5.3.1. An eight-gore model is considered, where the mesh and starting condition is generated by applying Eq. (3.15), where $\alpha = 18^\circ$, to the four-gore volume mesh and final solution of Mesh 1 from Section 5.3.1 and merging the overlapping boundary nodes. The number of fluid volume nodes and elements for the Mesh 1 four-gore case and eight-gore mesh modeled after it are listed in Table 5.2.

<table>
<thead>
<tr>
<th></th>
<th>4-gore</th>
<th>8-gore</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fluid Volume $nn$</td>
<td>367,723</td>
<td>723,399</td>
</tr>
<tr>
<td>Fluid Volume $ne$</td>
<td>2,002,804</td>
<td>4,005,608</td>
</tr>
<tr>
<td>Periodic $nn$</td>
<td>12,047</td>
<td>12,047</td>
</tr>
</tbody>
</table>

Table 5.2: Number of fluid volume nodes and elements and periodic boundary nodes for the four-gore and eight-gore Mesh 1 computations. The fluid volume mesh consists of four-node tetrahedral elements. Periodic $nn$ is for one periodic boundary, so that $nn$ is the number of periodic node sets mapped between the two lateral boundaries.

The eight-gore model is computed using the DSD/SST-TIP1 technique for 1000 time steps (23.2 s). The fabric and geometric porosity coefficients are calculated using data averaged over the final 300 time steps (7 s). Flow fields for the four-gore and eight-gore computations are depicted in Figure 5.12. The HMGP-FG porosity coefficients for the four-gore and eight-gore computations are compared in Figure 5.13.

There is practically no change in either $k_G$ or $k_F$ distributions as the periodicity constraint is relaxed by doubling the number of gores modeled. Thus, the four-gore model does not overly constrain the flow and can reliably be used for calculating the homogenized porosity coefficients.
Figure 5.12: Flowfield at the parachute crown and skirt shown for four-gore (left) and eight-gore (right) models. Velocity vectors are colored by magnitude.
Figure 5.13: Fabric and geometric porosity coefficients for the four-gore and eight-gore computations.
5.3.4 Porosity Coefficient Determination for Various Canopy Shapes

Now that the conditions affecting $k_G$ convergence have been investigated, reliable HMGP-FG porosity coefficients can be calculated for various canopy shapes by incorporating favorable convergence conditions. Of primary interest is determining the variation of $k_G$ coefficients with slit shape, since the slits become very narrow as the skirt diameter decreases. Canopy shapes encountered during the full open stage of the parachute descent are considered here, although the same method can be used for reefed canopy shapes. Based on the convergence studies described in Sections 5.3.1–5.3.3, a periodic four-gore mesh with a local refinement of 8 elements and standard global refinement computed with the “-SP” option and a time step of 0.0058 s will yield the most accurate geometric porosity coefficients. Thus, these conditions are used to compute $k_F$ and $k_G$ values for three additional canopy shapes.

Up until this point, all periodic $n$-gore meshes were based on a canopy shape found by applying to the structure a symmetrized pressure distribution calculated when the skirt diameter was at an average value and decreasing. The resulting canopy shape had a skirt diameter smaller than the average because the inertia of the structure causes it to lag behind the driving pressure distribution. This can be seen in Figure 5.14 where the two pressure distributions taken at the decreasing and increasing average skirt diameter differ significantly, thus resulting in different canopy shapes. The three additional canopy shapes were determined by applying pressure distributions symmetrized from FSI data when the skirt diameter was at a maximum, increasing average, and minimum value. The resulting canopy shapes have skirt diameters of approximately 91 ft, 83 ft and 73 ft and will be referred to as the MAX, AVG-UP and MIN cases, respectively. The previous canopy shape, calculated using symmetrized pressures from the decreasing average skirt diameter, has a resulting skirt diameter of 76 ft and will be referred to as the AVG-DOWN case. Figure 5.14 illustrates the
symmetrized pressure distribution for each case on an undeformed structure mesh. Figure 5.15 displays the canopy shapes found by applying the symmetrized pressures to the structure in a stand-alone structural mechanics computation. Figure 5.16 shows the variation in slit shape as the skirt diameter decreases. Table 5.3 lists the number of fluid volume nodes and elements and number of periodic boundary nodes for each case.

A stand-alone fluid mechanics computation is performed for each case using the semi-discrete formulation [16] until the flow field becomes developed and exhibits vortex shedding. The computations are then continued for approximately 2,500 time steps (15 s) using the DSD/SST-SP formulation. The HMGP-FG porosity coefficients are calculated for each patch using data averaged over the final 150 time steps (1 s) of
the space–time computation. The flow field for each case is shown in Figure 5.17. The fabric and geometric porosity coefficients for each case are graphed in Figure 5.18.

The fabric porosity distribution is similar for all cases, with slight differences in Patches 11–14. The distribution of geometric porosity coefficients is also similar for all cases. This indicates that the $k_G$ for each patch does not vary significantly during the breathing cycle, even for slits near the skirt that experience significant shape changes. However, a difference in $k_G$ values is observed between the ring gaps and sail slits. The sail slits have higher viscous losses due to the narrow corners where the flow experiences a no-slip condition on the fabric, whereas flow through the ring gaps
Table 5.3: Number of fluid volume nodes and elements and periodic boundary nodes for the four canopy shape meshes with 8 local refinement elements. The fluid volume mesh consists of four-node tetrahedral elements. Periodic \( nn \) is for one periodic boundary, so that \( nn \) is the number of periodic node sets mapped between the two lateral boundaries.

<table>
<thead>
<tr>
<th></th>
<th>MAX</th>
<th>AVG-UP</th>
<th>AVG-DOWN</th>
<th>MIN</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fluid Volume</td>
<td>( nn )</td>
<td>531,639</td>
<td>644,930</td>
<td>358,773</td>
</tr>
<tr>
<td></td>
<td>( ne )</td>
<td>2,975,232</td>
<td>3,639,968</td>
<td>1,948,196</td>
</tr>
<tr>
<td>Periodic</td>
<td>( nn )</td>
<td>12,883</td>
<td>15,190</td>
<td>11,837</td>
</tr>
</tbody>
</table>

Figure 5.17: Velocity magnitude near canopy shown on cut plane for MIN, AVG-UP, AVG-DOWN and MAX cases, from left to right and top to bottom.

Figure 5.18, where the \( k_G \) values for the ring gaps of Patch 2–5 are uniformly higher than the \( k_G \) values for the sail slits of Patch 6–13. Since the \( k_F \) and \( k_G \) values do not vary with canopy shape, average values can be used for each patch when performing
Figure 5.18: Fabric and geometric porosity coefficients for the four computations with varying canopy shape.

FSI computations of full open parachutes with the HMGP-FG technique. Average fabric and geometric porosity coefficients are calculated by averaging the values for all four shapes at each individual patch. The average fabric and geometric porosity coefficients for each patch are listed in Table 5.4. This is a valid approximation since the coefficients differ very little with canopy shape and the average skirt diameter of the four canopy shapes considered is very close to the average FSI skirt diameter of 80 ft.

Figure 5.19 further validates the HMGP-FG technique, as it clearly shows the HMGP porosity coefficients vary significantly during the breathing cycle for patches
Table 5.4: Average fabric porosity $k_F$ (in CFM) and geometric porosity $k_G$ (non-dimensional) coefficients listed by patch for the four canopy shapes.

containing a sail slit. The large difference in $\hat{V}_G$ through the sail slits as they change shape combined with a relatively constant patch pressure differential results in highly variable $k_{PORO}$ values, as expressed by Eq. (3.1). The accuracy of $k_{PORO}$ values throughout the breathing cycle is examined in greater detail in Section 5.3.5.

Figure 5.19: HMGP porosity coefficients for the four computations with varying canopy shape.

Based on the mesh refinement convergence study in Section 5.3.1, a local refinement of 8 and 12 elements both resulted in similar HMGP-FG porosity coefficients. A local refinement of 8 elements was selected for the four canopy shape computa-
tions presented in this section to reduce the mesh size. For accuracy verification purposes, the same four canopy shapes were computed using a mesh with 12 refinement elements across each gap and slit. The fluid volume and periodic boundary mesh information is shown in Table 5.5 and the calculated HMGP-FG coefficients are included as Figure 5.20.

<table>
<thead>
<tr>
<th></th>
<th>MAX</th>
<th>AVG-UP</th>
<th>AVG-DOWN</th>
<th>MIN</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fluid Volume</td>
<td>$nn$</td>
<td>539,432</td>
<td>652,841</td>
<td>367,723</td>
</tr>
<tr>
<td></td>
<td>$ne$</td>
<td>3,022,576</td>
<td>3,688,748</td>
<td>2,002,804</td>
</tr>
<tr>
<td>Periodic</td>
<td>$nn$</td>
<td>13,008</td>
<td>15,345</td>
<td>12,047</td>
</tr>
</tbody>
</table>

Table 5.5: Number of fluid volume nodes and elements and periodic boundary nodes for the four canopy shape meshes with 12 local refinement elements. The fluid volume mesh consists of four-node tetrahedral elements. Periodic $nn$ is for one periodic boundary, so that $nn$ is the number of periodic node sets mapped between the two lateral boundaries.

As seen in Figure 5.20, the MIN case with 12 local refinement elements has convergence issues for Patch 4 and 5. Doubling the global mesh refinement does not sufficiently improve the element aspect ratios in this very localized area and only serves to increase computational cost. Furthermore, Figure 5.21 shows that the $k_G$ distribution for a local refinement of 12 elements appears to be no better than that for 8 elements. Thus, a local refinement of 8 elements is recommended when utilizing the HMGP-FG technique, as this provides comparable $k_G$ values and complete convergence is more likely.
Figure 5.20: Fabric and geometric porosity coefficients for the four computations with varying canopy shape and 12 local refinement elements.
Figure 5.21: Average geometric porosity coefficient for variable shape computations with local refinements of 8 and 12 elements.
5.3.5 Pressure Dependence of the HMGP and HMGP-FG Models

In this section, the pressure dependence of the HMGP-FG \( k_G \) and \( k_F \) porosity coefficients and HMGP \( k_{PORO} \) porosity coefficient is investigated. The HMGP-FG technique separately calculates the linear and non-linear pressure contributions to the volumetric flowrate through the canopy, whereas the HMGP technique only accounts for a linear pressure contribution based on a combined porosity coefficient. Therefore, the separated HMGP-FG fabric and geometric porosity coefficients should be more pressure-invariant than the combined HMGP \( k_{PORO} \). In fact, the HMGP-FG fabric porosity coefficients for each patch should remain constant for all pressures. The volumetric flowrate through the fabric for a given pressure differential depends only on the specified material porosity of the membrane elements and their respective areas, as governed by Eq. (3.4) and \( u_n = -(k_F)_{MAT} \Delta p \) where \( (k_F)_{MAT} \) is the material porosity of the parachute fabric.

To obtain a wide range of pressure differentials over which to compare the porosity coefficients, the Re is varied from 2,000 to 18 million. Decreasing Re represents a decrease in the velocity scale, which results in much lower physical pressure differentials after scaling the computational \( \Delta p \) by \( \rho_S V_S^2 \), where the subscript \( S \) denotes the computational to physical scaling factor. A total of four computations are performed, with Re values of 2,000, 10 million, 15 million and 18 million.

The stand-alone fluid mechanics computations presented in this section are carried out in a parallel computing environment, using PC clusters. In solving the linear equation systems involved at every nonlinear iteration, the GMRES search technique [11] is used with a diagonal preconditioner. The meshes are partitioned to enhance the parallel efficiency of the computations. Mesh partitioning is based on the METIS [7] algorithm. The computations are carried out using the DSD/SST-TIP1 technique (see Remarks 3 and 4), with the SUPG test function option WTSA (see Remark 1).
The stabilization parameters used are those given in Eqs. (2.18)–(2.19) and (2.13)–(2.17), with the \( \tau_{\text{SIG}} \) term dropped from Eq. (2.18). The interface-stress projection is based on the SSP technique described in Section 3.2. The time-step size is 214 s, 0.0414 s, 0.0283 s, and 0.0232 s for \( \text{Re} = 2000 \), 10 million, 15 million and 18 million. The number of nonlinear iterations per time step is 6, and the number of GMRES iterations per nonlinear iteration is 90. Selective scaling (see [13, 22]) is used, with the scale for the equations corresponding to the incompressibility constraint set to 10.

All computations use properties of air at standard sea-level conditions. A periodic four-gore domain with the same fluid volume mesh as Mesh 1 of Section 5.3.1 is used for these computations.

The stand-alone fluid mechanics computation is performed for each case using the semi-discrete formulation [16] until the flow field becomes developed. The computations are then continued for approximately 850 time steps using the DSD/SST-TIP1 formulation. The HMGP-FG porosity coefficients are calculated for each patch using data averaged over the final 300 time steps of the space–time computation. The flow field for each for each case is shown in Figure 5.22. The HMGP-FG fabric and geometric porosity coefficients for each case are graphed in Figure 5.23 and the HMGP porosity coefficients for each case are graphed in Figure 5.24.

The geometric porosity coefficient for each patch is very close for all cases except \( \text{Re} = 2000 \), while the fabric porosity coefficient is constant for all cases. Conversely, the HMGP porosity coefficient increases as \( \text{Re} \), and thus \( \Delta p \), decreases. This is more easily seen in Figure 5.25, which shows the variation in \( k_F, k_G \) and \( k_{\text{PORO}} \) with the canopy pressure differential for the rings and sails. The \( \text{Re} = 2000 \) results are excluded from this figure, as the pressure differential is nearly zero and the resulting porosity coefficient trends are easily seen in Figures 5.23 and 5.24.

The pressure dependence of \( k_{\text{PORO}} \) is readily observed, as it varies significantly over the range of pressure differentials examined. To accurately model the volumetric
Figure 5.22: Velocity magnitude near canopy shown on cut plane for computations with Re values of 2,000, 10 million, 15 million and 18 million, from left to right and top to bottom.

flowrate through the parachute canopy in FSI, the value of $k_{PORO}$ would have to change during a breathing cycle based on the patch pressure differential. In current FSI computations with the HMGP technique, constant $k_{PORO}$ patch values are used based on a four-gore computation with an average canopy shape. Although the error in volumetric flowrate is minimized by computing $k_{PORO}$ values at an average canopy shape, this error will vary widely during the parachute descent and will likely be greater than indicated in Figure 5.19 due to a dynamic breathing motion and localized higher or lower pressure differentials.

On the other hand, the values for $k_F$ and $k_G$ do not change solely due to differences in the canopy pressure differentials ordinarily seen during a breathing cycle. As shown
Figure 5.23: HMGP-FG fabric and geometric porosity coefficients for cases with varying Re.

In Section 5.3.4, the fabric and geometric porosity coefficients for each patch also do not vary significantly for various slit shapes. Thus, the shape-averaged $k_F$ and $k_G$ values listed in Table 5.4 can be used to reliably model the homogenized porosity on the smoothened fluid interface as described in Section 3.3.2. In summary, the total volumetric flowrate through a parachute canopy experiencing dynamic changes in pressure can be modeled more accurately with the HMGP-FG than is currently done with the HMGP technique. This is because the HMGP-FG technique properly models the non-linear pressure dependence of the volumetric flowrate through the gaps and slits of the canopy.
Figure 5.24: HMGp porosity coefficients for the cases with varying Re. Note that these are plotted on a logarithmic axis due to the large values for Re = 2,000.
Figure 5.25: Variation in HMGP-FG fabric and geometric porosity coefficients and HMGP porosity coefficient for ring and sail patches with canopy pressure differential. Pressure differentials and porosity coefficients obtained by averaging values from Patches 2–5 for ring coefficients and Patches 6–13 for sail coefficients.
Chapter 6

SCFSI M2C Fabric Stress Comparison

The SCFSI M2C spatially multiscale technique, detailed in Section 3.7, is used here to more accurately determine canopy fabric stresses. Focus is placed on variations in fabric stress with structure mesh refinement and the presence of a vent hoop. Fabric stresses calculated with the SCFSI M2C technique are more reliable since stress concentrations and the effect of the vent hoop can be captured, whereas the FSI computations are performed with a coarser structure mesh and lack a vent hoop. Including the vent hoop in fabric stress calculations is important since it relieves stresses at the crown. However, it is reasonable to neglect the vent hoop in FSI computations where only aerodynamic quantities, not stresses, are desired. The vent hoop does not alter aerodynamic properties of the parachute significantly as the canopy shape does not change significantly, yet the vent hoop makes structural convergence more difficult. It is also reasonable to use relatively coarser structure meshes in FSI computations since precise structural deformations are not required to obtain general aerodynamic quantities. However, accurately capturing these structural deformations with a more refined mesh is imperative when calculating fabric stresses.
6.1 Vent Hoop Description

The vent hoop is a high-strength textile cord that replaces conventional vent lines [10]. Both vent components bear almost the entire parachute load from the radial lines. However, the vent hoop is less likely to cause parachute failure due to contact damage or vent entanglement during deployment. The vent hoop connects the eighty radial lines terminated at the vent but its length is less than the constructed vent circumference. This “foreshortening” allows the vent hoop to bear large radial loads in hoop stress, effectively relieving stresses on the low-strength canopy fabric. Figure 6.1 depicts the vent hoop connected to each radial line for a structure mesh with four membrane elements across each gore at the vent.

![Figure 6.1: Vent hoop (red) shown connected to radial lines of structure mesh with four membrane elements across each gore at the vent.](image)

6.2 Computational Conditions

As previously mentioned, the goal of these computations is only to determine how the vent hoop and structure mesh refinement affects fabric stresses. Quantifying time-accurate fabric stresses of a deforming parachute is not essential since steady-state fabric stresses can provide adequate data for comparison. Thus, the computations are expedited by using a time-invariant pressure distribution. This pressure distribution is determined from the PA computation reported in Chapter 4. The PA fluid interface stresses are time-averaged over one FSI breathing cycle then symmetrized by averaging circumferentially. The pressure components of these time-invariant interface stresses are projected to the structure mesh using the SSP technique described
in Section 3.2.

A total of four cases are considered, one coarse mesh with and without a vent hoop and one refined structure mesh with and without a vent hoop. The coarse mesh without vent hoop is the same mesh used for the PA computation in Chapter 4 and is computed here as a baseline case. The coarse structure mesh without a vent hoop has 29,200 nodes, 26,000 four-node membrane elements and 10,920 two-node cable elements. The fine mesh without a vent hoop has 115,680 nodes, 108,480 four-node membrane elements and 21,640 two-node cable elements. The coarse and fine meshes with a vent hoop simply have 80 additional cable elements. One-gore slices of the coarse and fine mesh are shown in Figure 6.2. All cases use a time-step size of 0.0232 s, with 5 nonlinear iterations per time step. The number of GMRES iterations per nonlinear iteration is 100 for the fine mesh computation with a vent hoop, and 30 for the other three cases. The mass-proportional damping coefficient is set to $2.155 \times 10^3 \text{ s}^{-1}$ for the fine mesh computation with a vent hoop, and $2.155 \times 10^2 \text{ s}^{-1}$ for the other three cases.
6.3 Fabric Stress Results

Before the time-averaged, circumferentially symmetric pressures from FSI are applied to the coarse and fine meshes without a vent hoop, a starting canopy shape is found. This is done by applying to an unstressed canopy a uniform pressure equal to the stagnation pressure at a descent speed of 25 ft/s. The starting canopy shapes for both meshes settle at approximately 500 time steps. Next the time-averaged, circumferentially symmetric pressures are applied to the starting shape for both meshes. After 4000 time steps, the coarse and fine meshes without a vent hoop achieve a steady canopy shape and the fabric stresses are calculated.

A vent hoop is then added to the settled shapes lacking a vent hoop and the same time-averaged, circumferentially symmetric pressures are applied to the structure. To prevent structural convergence issues when adding a vent hoop to these settled shapes, the vent hoop is given an initial unstressed length greater than its physical unstressed length. The artificial unstressed length is then gradually decreased to the physical length, allowing a smooth transition from a larger vent circumference to a smaller one. The vent hoop length transition occurs over 500 time steps for the coarse mesh and 2,000 time steps for the fine mesh. The canopy shapes settle after an additional period of time, approximately 1,500 time steps for the coarse mesh and 4,000 time steps for the fine mesh. At this point, fabric stresses for the coarse and fine mesh with a vent hoop are calculated.

The fabric stresses calculated are element-based maximum principal stresses. These element-based stresses are multiplied by the fabric thickness of each element to yield element-based fabric tensions. The element tensions are then projected to nodal-based values for display and reporting purposes. Figures 6.3 and 6.4 show the maximum principal fabric tensions for the coarse and fine meshes without a vent hoop. Figures 6.5 and 6.6 show the maximum principal fabric tensions for the coarse and fine meshes with a vent hoop. Figures 6.7 and 6.8 chart the maximum fabric tension in
each ring and sail for the cases without and with a vent hoop.

Figures 6.7 and 6.8 affirm that the refined mesh more accurately captures the tension concentrations at sail trailing edges near the radial lines, as seen in Figures 6.3–6.6. Additionally, the fine mesh with a vent hoop captures the tension present in Ring 1 solely due to the gore pressurization enabled by the vent hoop creating circumferential fabric slack in the crown, as seen in Figure 6.1. The coarse mesh is incapable of capturing pressurization tension in this region since it has only one membrane element spanning each gore on Ring 1. A comparison of Figures 6.7 and 6.8 reveals that the vent hoop drastically reduces fabric tension in Rings 1–3 with minor reductions occurring in Ring 4 through Sail 2 (labeled in figures as Ring/Sail 4–6).
Figure 6.3: Maximum principal fabric tension for the coarse mesh with no vent hoop.

Figure 6.4: Maximum principal fabric tension for the fine mesh with no vent hoop.
Figure 6.5: Maximum principal fabric tension for the coarse mesh with a vent hoop.

Figure 6.6: Maximum principal fabric tension for the fine mesh with a vent hoop.
Figure 6.7: Maximum principal fabric tension by ring/sail for the case with no vent hoop. Coarse mesh results denoted with red diamonds and fine mesh results denoted with blue squares.

Figure 6.8: Maximum principal fabric tension by ring/sail for the case with a vent hoop. Coarse mesh results denoted with red diamonds and fine mesh results denoted with blue squares.
Chapter 7

Conclusions

The core computational technologies developed by the T*AFSM for FSI problems, including the SSTFSI method and special parachute modeling techniques, perform well in simulating the descent of large parachutes with complex geometry. Current research efforts are focused on modeling the ringsail parachutes used to recover NASA’s Orion spacecraft. Fully understanding the dynamics of the parachute and accurately characterizing its performance is crucial for achieving safety, reliability and efficiency design requirements in this critical application. More realistic computational modeling will aid in determining the root causes of complex parachute phenomena and thus improve parachute design. With this goal in mind, several special techniques were presented that improve upon level of computational scope and accuracy currently available.

A method for generating a comparable starting condition for different parachute configurations was described in Chapter 4. Three configurations were considered, including a canopies with all sails in place (PA), with the 5th sail removed (PM5), and with the 11th sail removed. Shape determination iterations were performed to obtain an accurate initial canopy shape and descent speed. Symmetric FSI computations were then carried out with the resulting canopy shape and descent speed. Although the exact flow pattern varied for each parachute configuration, the breathing motion of
the canopy generated a flow field that was dominated by asymmetric vortex shedding and was reasonably similar between configurations. Before beginning fully coupled FSI computations, a payload kick was introduced by instantaneously increasing the payload horizontal speed. This was done to match the parachute swinging dynamic observed in NASA drop tests.

Comparison of PA results and drop test data indicated very good agreement, establishing a performance baseline for PM5 and PM11. Even though PM5 has a slightly larger skirt diameter than PA, its effective drag area is reduced by the missing sail, resulting in a higher descent speed. PM5 and PM11 exhibit a lower amplitude of canopy descent speed oscillations. However, the payload descent speed is of primary importance since it will house the crew and the amplitude of payload descent speed oscillations for PM5 and PM11 are similar to PA due to the dominating payload swing dynamic. The skirt diameter of PM11 is very low due to the loss of pressurization in the bottom three sails. This causes a large reduction in drag and corresponding increase in descent speed. The horizontal speed of PA and PM5 are comparable, while PM11 displays very little gliding. Overall, this study shows that redistributing canopy geometric porosity can improve parachute stability, but there is a tradeoff between stability and drag performance. The low drag of PM11 precludes it from specific consideration and indicates that to obtain favorable stability and drag performance, the geometric porosity distribution must be carefully optimized.

An improved model for calculating the HMGP-FG geometric and fabric porosity coefficients is proposed in Chapter 5. This model features rotationally periodic boundary conditions for a n-gore slice of the total domain. By using this technique to reduce computational cost, the fluid volume mesh refinement is increased so as to better resolve local flow behavior near the canopy. A convergence study based on mesh refinement shows that the fabric and geometric porosity coefficients are not affected by the further mesh refinement within the parachute wake. Increasing the
number of refinement elements across the gaps and slits beyond eight does not significantly improve the geometric porosity coefficient distribution. In fact, as the number of gap (or slit) elements increases, the local convergence in this region becomes less likely due to very high element aspect ratios. Thus, a lower number of local refinement elements is recommended to avoid unexpected convergence issues. Based on the cases considered in this research, a local refinement of 8 elements across each gap and slit is sufficient for calculating geometric porosity coefficients. A time-accuracy convergence study was conducted to investigate whether decreasing the time-step size and increasing the temporal order of integration improves the accuracy of geometric porosity. It was discovered that decreasing the time step by a factor of four and doubling the number of integration points in time by employing the "-SP" solution technique promotes more accurate geometric porosity values. A convergence study based on reducing the periodic constraint by increasing the number of gores modeled was conducted by computing a periodic eight-gore case. Very similar porosity values reveal that the four-gore model provides a sufficient representation of the flow through the complex canopy structure.

To maximize the accuracy of calculated fabric and geometric porosity coefficients, the results of the convergence studies were incorporated into subsequent periodic n-gore computations of various canopy shapes. Four cases based on fully open canopy shapes encountered during the periodic breathing dynamic are considered. Results show that the fabric and geometric porosity distributions remain relatively constant for each patch as the canopy shape changes. This means that the constant patch values of $k_F$ and $k_G$ listed in Table 5.4 can be used to accurately represent volumetric flowrate through the canopy in FSI. The HMGP porosity coefficient, however, varies significantly for different canopy shapes, especially near the skirt. Thus, the HMGP-FG technique represents an improvement over the previous HMGP technique for reliably modeling volumetric flowrate through the geometrically simplified, smoothened
fluid interface. The HMGP-FG and HMGP techniques are also compared by examining how the porosity coefficients of each vary based on changes in the canopy pressure differential. The results clearly show that the HMGP-FG fabric and geometric porosity coefficients do not vary over the range of pressure differentials encountered during parachute descent in the full open stage. Conversely, the HMGP porosity coefficient varies with pressure and as such, may significantly over- or under-predict the total volumetric flowrate based on whether the canopy pressure differentials are less than or greater than those used to calculate the $k_{PORO}$ values. It is noted that the HMGP-FG geometric porosity coefficient does decrease when a miniscule pressure differential exists. To determine geometric porosity values in situations where the pressure differential is zero or negative, as in the case in reefed parachute stages, performing a similar periodic n-gore computation with the applicable parachute shape would be desirable.

In Chapter 6, maximum principal fabric tensions were calculated for a static canopy shape with a stand-alone structural mechanics computation. This canopy shape was obtained by applying to the structure a time-averaged, circumferentially-averaged pressure distribution based on FSI data. The SCFSI M2C technique was used to compare fabric tensions for two different structure meshes. One mesh was a coarser structure mesh used in FSI computations, while the other was refined to provide more accurate fabric tension results. A vent hoop was also added to each mesh to investigate its tension-relieving effect on crown rings and sails. From a comparison of the coarse and fine mesh tensions, it was determined that a fine structure mesh is required to capture large tension gradients near the intersection of sail trailing edges and radials. Comparing the cases with and without a vent hoop reveals large tension reductions in Ring 1–3, with minor decreases continuing up to Sail 2. A vent hoop is not required for FSI computations where aerodynamic quantities are of interest, since it does not alter the parachute shape. However, it is important to include the
vent hoop structural component when calculating fabric tensions, as it greatly alters the tension distribution near the crown.
Bibliography


