DESIGN AND VALIDATION OF A HIGH-LIFT
LOW-PRESSURE TURBINE BLADE

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ABSTRACT


This dissertation is a design and validation study of the high-lift low-pressure turbine (LPT) blade designated L2F. High-lift LPTs offer the promise of reducing the blade count in modern gas turbine engines. Decreasing the blade count can reduce development and maintenance costs and the weight of the engine, but care must be taken in order to maintain turbine section performance with fewer blades. For an equivalent amount of work extracted, lower blade counts increase blade loading in the LPT section. The high-lift LPT presented herein allows 38% fewer blades with a Zweifel loading coefficient of 1.59 and maintains the same inlet and outlet blade metal angles of conventional geometries in service today while providing an improved low-Reynolds number characteristic. The computational design method utilizes the Turbine Design and Analysis System (TDAAS) developed by John Clark of the Air Force Research Laboratory. TDAAS integrates several government-funded design utilities including airfoil and grid generation capability with a Reynolds-Averaged Navier-Stokes flow solver into a single, menu-driven, Matlab-based system. Transition modeling is achieved with the recently developed model of Praisner and Clark, and this study validates the use of the model for design purposes outside of the Pratt & Whitney (P&W) design system where they were created. Turbulence modeling is achieved with the Baldwin and Lomax zero-equation model.

The experimental validation consists of testing the front-loaded L2F along with a previously designed, mid-loaded blade (L1M) in a linear turbine cascade in a low-speed wind tunnel over a range of Reynolds numbers at 3.3% freestream turbulence. Hot-wire anemometry and pressure measurements elucidate these comparisons, while a shear and stress sensitive film (S3F) also helps describe the flow in areas of interest. S3F can provide all 3 components of stress on a surface in a single measurement, and these tests extend the operational envelope of the technique to low speed air environments where small dynamic pressures and curved surfaces preclude the use of more traditional global measurement methods. Results are compared between the L1M and L2F geometries along with previous data taken in the same wind tunnel at identical flow conditions for the P&W Pack B geometry.
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Nomenclature

\( AL \)  aft-loaded

\( BL \)  boundary layer

\( B_x \)  axial chord length

\( C_f \)  skin friction coefficient, \( C_f = \frac{\tau_w}{q} \)

\( EXP \)  experiment

\( FL \)  front-loaded

\( FSTI \)  freestream turbulence intensity

\( GSM \)  gradient search methods

\( L_{area} \)  area-averaged loss coefficient, \( L_{area} = \frac{(P_{t,in} - P_{t,ex})}{(P_{t,in} - P_{s,in})} \)

\( L_{flux} \)  flux-averaged loss coefficient, \( L_{flux} = \frac{\sum u_{ex,i} (P_{t,in} - P_{t,ex})}{\sum u_{ex,i} (P_{t,in} - P_{s,in})} \)

\( LE \)  leading edge

\( LPT \)  low-pressure turbine

\( ML \)  mid-loaded

\( NN \)  neural networks

\( PS \)  pressure surface

\( q \)  dynamic pressure, \( q = \frac{1}{2} \rho U_\infty^2 \)

\( R_0 \)  Hot-wire sensor resistance at 0°C [Ω]

\( R_{100-0} \)  Difference in hot-wire resistances between 0 and 100 °C [Ω]

\( R_{op} \)  Hot-wire sensor operating resistance [Ω]

\( RANS \)  Reynolds-averaged Navier-Stokes

\( Re \)  Reynolds number, \( Re = \frac{U_\infty c}{\nu} \), \( Re_x = \frac{\rho U_\infty x}{\mu} \), \( Re_\theta = \frac{U_\infty \theta}{\nu} \)

\( S \)  suction surface area

\( SS \)  suction surface

\( SSL \)  suction surface length

\( T \)  temperature
$T_{dew}$  Dew-point temperature [K]

$T_m$  Mean hot-wire temperature [K]

$T_{wire}$  Hot-wire sensor temperature [K]

TDAAS  Turbine Design and Analysis System

$TR$  transition

$TU$  turbulence

$Tu$  local turbulence intensity

$u$  streamwise velocity

$U_\infty$  freestream velocity

$VGJs$  vortex generator jets

$WIT$  wake-induced transition

$x$  streamwise direction

$y$  surface-normal direction

$Zw$  Zweifel loading coefficient, $Zw = 2 \frac{S}{\pi x} \cos^2 \beta \left[ \frac{\alpha_x}{\beta_x} \tan \beta_1 + \tan \beta_2 \right]$

**Greek**

$\beta$  gas flow angle

$\delta$  boundary layer height, $u(\delta) = 0.99U_\infty$

$\lambda$  integral length scale

$\nu$  kinematic viscosity of fluid, $\nu = \frac{\mu}{\rho}$

$\rho$  density of air

$\tau$  shear stress, $\tau = \frac{\partial u}{\partial y}$

$\theta$  boundary layer momentum thickness, $\theta = \int_0^\delta \frac{u}{U_\infty} (1 - \frac{u}{U_\infty}) dy$

**Subscript**

$\infty$  freestream condition

$in$  inlet condition

$ex$  exit condition

$w$  wall condition
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1

Introduction

_We don’t know a millionth of one percent about anything._

– Thomas Edison

Modern gas turbine engines provide propulsion for many aircraft in service today. A cross-section of a modern gas turbine engine, the Pratt & Whitney PW4000, is shown in Figure 1.1(a). Air entering the engine is compressed by several rows of axial flow compressor blades before the combustion chamber in order to generate enough stagnation pressure in the flow to produce sufficient thrust. The compressor section is normally divided into two different groups: high-pressure and low-pressure; some recent designs even include a third spool for intermediate-pressure compressor and turbine sections. Each group consists of one or more stages of blades that pressurize the incoming flow of air. Following the compressor is the combustor, where fuel and air are mixed and burned, producing very high energy flow which exits into the turbine section. The turbine section extracts energy from the flow to drive the fan and compressor. The turbine section mirrors the compressor section with its high- and low-pressure sections, each with one or more rows of blades. The high-pressure turbine section is located directly behind the combustor. Its function is to extract energy from the highly pressurized hot gases and power the high-pressure compressor section through a shaft connecting the two. Likewise, the low-pressure turbine section extracts energy and drives the low-pressure compressor section. Due to the extremely hot exhaust gases from the combustor, intricate cooling mechanisms are required to keep the high-pressure turbine blades from melting. High-pressure turbine blade geometries house inner passages so cooling air can circulate through them, dumping heat out through hundreds of tiny holes in the blade surface. The thin trailing edges are also actively cooled with low temperature air relative to the blade metal temperature. Films of cooler air can also be injected near the leading edge of the blade that serve to coat the surface and protect it from the hot gases.
1.1. IMPORTANCE OF THE WORK

The mature gas turbine industry has become more cost-driven with its focus on the total cost of engine ownership, from initial purchase through maintenance and repair to replacement [Wisler 1998], as illustrated in Figure 1.1(b). This means the technology employed in the engine has reached such a developed state that some incremental improvements, however beneficial to performance, may no longer be cost-effective. Instead, the industry is focusing more on ways to reduce the current total cost of engine ownership.

![Diagram of gas turbine industry](image1.png)

Figure 1.1: The gas turbine industry.

(a) Its product - the gas turbine engine (P&W 4000). (b) Its focus - total cost of engine ownership.

1.1 Importance of the Work

This dissertation seeks to illustrate the design process and result when developing a front-loaded LPT airfoil with a loading characteristic higher than any previously published design. This work is part of an ongoing US Air Force Research Laboratory initiative which seeks to find the limits of the LPT design space. In other words, we want to find out what loading characteristics and loading levels provide optimal LPT designs over the varied flow conditions which occur in modern gas turbine engines.

The high-lift design presented herein promises a reduced low-pressure turbine blade count with an improved Reynolds lapse characteristic over current LPT blades in service today. The Reynolds lapse characteristic is the measure of total pressure loss across the LPT as the Reynolds number is decreased. Current LPTs suffer a dramatic increase in losses with decreasing Reynolds number, and this is usually due to the effects of separation [Sharma 1998]. A reduced turbine blade count can lower part counts, maintenance, and weight of an engine, thus lowering the overall system cost. The improved low-Re performance promises increased efficiency, reduced fuel consumption, longer loiter...
times, and higher maximum altitudes over current designs in service today. Through the course of this study, a recently developed separated-flow transition model by Praisner and Clark was used to design the new LPT airfoil. Its validation proves the model’s general utility in RANS-based turbomachinery design systems outside of the Pratt & Whitney design system where it was created. Thus the design of the new LPT dubbed “L2F” both expands the turbomachinery designer’s current design space with higher-lift options and proves to the research community the general utility of a new transition modeling tool for RANS-based design and analysis purposes. The extensive bank of experimental data obtained in this work also provides a wealth of information useful for CFD code validation purposes.

In addition, this dissertation will illustrate the development of a shear and stress sensitive film (S3F) for low-speed applications, particularly in the LPT environment. A major difficulty with measuring surface shear in air flow environments is the small friction forces limited by the low density of air; these difficulties grow exponentially as flow velocity is decreased. This problem is addressed in this research by properly tuning the properties of the film such as elastic modulus and thickness. These low-speed tests provide the opportunity for S3F technology development, accomplished by Innovative Scientific Solutions, Inc., of Dayton, OH. Highly curved surfaces on the LPT blades in this study also present challenges to overcome. These issues have been addressed in order to bring to the experimental low-speed fluids community a non-intrusive diagnostic technique to obtain regional skin friction and normal pressure maps in low-speed air flow environments.

1.2 The Low-Pressure Turbine

LPT design is a tricky game to play. On one hand, the LPT must perform optimally at high-Re conditions near sea level where maximum loading is required for take-off. On the other hand, the majority of flight time is spent in higher altitude cruise conditions, where the lower air density results in a lower $Re$ and lower momentum flow in the LPT section. Low-$Re$ conditions can be particularly troublesome, as the lower momentum flow is prone to separation and transition effects due to the adverse pressure gradient experienced on the suction surface of the blade. Off-design operating conditions in the LPT section result in performance degradation due to this flow separation. There are several parameters that must be studied in order to develop a comprehensive understanding of the flow behavior around the LPT blade, including Reynolds number, freestream turbulence intensity (FSTI) and length scale, blade geometry, and pressure gradient.

A turbine section can have multiple stages typically consisting of two rows of airfoils, a vane and a blade; some designs employ counter-rotating blades which eliminate the vane. The vane row is
fixed and guides the incoming flow into the tangential direction of rotation. The row of blades turn the flow, resulting in a net torque on the shaft. The effect of the entire turbine section is to expand the gas (increase the velocity) while extracting energy to power the compressor.

There are two primary phenomena dictating the flow environment in the low-pressure turbine section: unsteadiness and low-Reynolds number effects (flow separation). Unsteadiness in the turbine section is caused by two principle mechanisms: secondary flows generated by a single blade row and the interaction of upstream disturbances (wakes and secondaries) with downstream blade rows as they convect with the bulk fluid. This high degree of unsteadiness also complicates the modeling of the transition process as the flow around the turbine blades changes from laminar to turbulent. At low Reynolds numbers, the turbine performance may be dictated by the laminar flow’s poor resistance to separation. This flow separation in turn causes reduced efficiencies of the LPT section, and can significantly degrade turbine blade performance resulting in a loss of thrust and increased consumption of fuel. Gad-el-Hak [Gad-el-Hak 1990] suggests that for external aerodynamics, an airfoil shape experiencing a Reynolds number less than $5 \cdot 10^4$, based on freestream velocity and chord length, will experience laminar separation with no reattachment. Internal aerodynamics may react differently, however, as wall proximity effects and upstream disturbance convection may not allow the flow to expand and separate as easily as on airplane wings. Laminar separation in particular, as seen in Figure 1.2, leads to significant degradation of engine performance due to the presence of large re-circulation zones, with their attendant high degree of blockage, large wakes, and reduced flow turning. A turbulent boundary layer is much less likely to separate, and previous work has shown turbulent separation bubbles over turbine blades are smaller and have less effect on performance. The high-lift design presented herein seeks to increase the loading achieved by the LPT section rotor while simultaneously producing an airfoil with an improved low-$Re$ characteristic.

1.3 Instability and Transition

Problems involving stability and transition exist in internal aerodynamics (turbo machinery) and external aerodynamics (air vehicles), as well as with hydrodynamics (submarines). Transition studies have been performed on flat plates as well as turbine geometries over a wide variety of flow conditions. Knowing the locations and length of transition are extremely important for design purposes; for
1.3. INSTABILITY AND transition

example, turbulent heat transfer levels are generally 3 to 5 times higher than in laminar conditions [Mayle 1991]. Several studies point to the need for improved transition modeling in order to advance the design of turbomachinery [Lakshminarayana 1991; Simon and Ashpis 1996; Dunn 2001]. The exploitation of transition and turbulence features in LPT flows both allows the high-lift design and complicates the flow structure around the airfoil. The difficulty in studying transition comes from the variety of factors which influence its evolution: development of freestream turbulence intensity and length scale, pressure gradient, surface curvature, wall roughness, heating and cooling, three-dimensionality, and unsteadiness, as well as other, perhaps less important effects. These non-linear growth mechanisms complicate the use of stability theory when an accurate prediction of transition is desired [Mayle 1991].

There are generally five basic modes of transition in gas turbine engines: natural, bypass, separated-flow, periodic-unsteady, and reverse [Mayle 1991]. Natural transition is the mode most thought of when “transition” is mentioned, and this occurs as a weak instability grows in a laminar boundary layer until subsequent breakdown and formation of turbulence. The most likely form occurring in gas turbine engines is bypass transition, wherein some or all of the laminar breakdown process does not occur, and is instead driven by freestream unsteadiness. Separated-flow transition occurs when a laminar boundary layer separates and transitions in the free shear layer above the bubble. This type of transition can occur near the leading edges of blades and near the point of minimum pressure on the suction surface sides. Of all of the modes, separated-flow transition is the most crucial for compressor and LPT design. Periodic-unsteady transition occurs due to the nature of turbomachinery flows: they are inherently periodic and unsteady, so the transition processes taking place are also unsteady and periodic. Reverse transition, often called “relaminarization”, occurs where a previously turbulent or transitional region is affected, usually by a strong favorable pressure gradient, so it becomes laminar. An instantaneous snapshot of the flow over a single airfoil may include laminar flow near the leading edge, followed by a wake- or shock-induced transition which is in turn replaced by a relaminarization with subsequent transition to turbulence occurring at multiple locations simultaneously. Mayle points out that one of the unresolved issues is whether or not the linear instabilities show up in regions of wake and shock interaction or strong adverse pressure gradient such as that occurring over the suction surface of the LPT. Halstead et al. have considered the effects of wakes so important to transition in turbomachinery as to define only two modes: wake-induced, and everything else (non-wake-induced) [Halstead et al. 1997].

In 2004, Praisner and Clark published a transition model for RANS solvers using a two-equation turbulence model with sufficient accuracy for use in an airfoil design system [Praisner and Clark 2007; Praisner et al. 2007]. Using Wilcox’s $k-\omega$ turbulence model, they constructed a CFD-supplemented
1.4 Experimental Database of 57 Attached Flow and 47 Separated Flow with Turbulent Reattachment Test Cases Using Actual Turbine Geometries. Dimensional analyses led to the appropriate laminar boundary layer quantities for both flow situations. Two new transition models were developed - one for attached flow and one for separated flow with turbulent reattachment. The attached flow model is based on a correlation between the local turbulence intensity, turbulent length scale, and momentum thickness Reynolds number at transition onset, which can be recast into a critical ratio of the boundary layer diffusion time to turbulent time scale. The separated-flow model was a correlation based on the momentum thickness Reynolds number at separation onset. The separated-flow transition model of Praisner and Clark is used in the current study to design the L2F geometry.

1.4 Current Direction of Low-Pressure Turbine Design

The gas turbine industry has achieved a high degree of maturity, which means it is becoming more of a cost-driven business rather than a technology-driven business. With increasing regularity, gas turbine design is focusing on the total cost of engine ownership, from initial purchase through maintenance and replacement. At the Minnowbrook II 1997 Workshop on Boundary Layer Transition in Turbomachines, Wisler explained how the lack of ability to accurately predict the transition process has seriously hampered the turbine engine designer’s ability to gain maximum benefit from the design process [Wisler 1998]. This is especially crucial for compressor and turbine design, as the relative motion between adjacent stator and rotor blade rows cause high frequency disturbance patterns to propagate through the engine and influence the boundary layer behavior. Modern computational fluid dynamics (CFD) techniques can easily handle the Reynolds-Averaged Navier-Stokes equations (RANS) for both 2D and 3D configurations, but are typically only as accurate as the turbulence and transition modeling capability. Wisler continues to define the heart of the problem:

...what’s missing is an adequate turbulence model, one that provides a practical, CFD design tool that will consistently and accurately predict transition and other boundary layer features for arbitrary flows. This missing link impedes designers in their efforts to tailor airfoil shapes to achieve increased loading and/or increased efficiency.

Two major design outcomes are desired here - increased loading and increased efficiency. Increasing the airfoil loading allows the reduction of engine part count, thereby reducing the cost, maintenance, and weight of the engine. Care must be taken when reducing the blade count, however, as it can lead to an increased chance for larger separation bubbles and increased loss [Gier and Ardey 2001]. Increasing the efficiency reduces the fuel consumption of the engine. Wisler estimates that a 1% gain in LPT efficiency will provide nearly $52,000 per year per aircraft in operational savings for
1.5 WHAT IS A HIGH-LIFT DESIGN?

most commercial and military aircraft. Another possible benefactor from the extra work provided by
high-lift LPT designs include extra power generation for auxilliary systems such as air conditioning,
radar and other electrical requirements, as well as for directed energy weapon systems. A current
concern in their development and use is finding the tremendous amount of extra power needed to
operate these systems; current concepts also include the addition of a reheat to the Brayton cycle
in the form of an inter-turbine burner [Zelina 2006]. Wisler’s comment above illustrates how both
of the current objectives of the gas turbine engine design community can be better achieved with
improved turbulence and transition modeling.

1.5 What is a High-Lift Design?

The term “high-lift” in itself signifies an increased loading, which is one of the desired design features
for the gas turbine industry as mentioned in the previous section. High-lift designs offer the promise
of lower blade counts by extracting a greater amount of work per blade, thereby requiring fewer
blades for an equivalent overall power extraction. Multiple studies have used the terms “high-lift”
and “ultra-high-lift” to describe their “high performance” profiles, but with no standard descriptor
by which to compare designs it is difficult to globally quantify a “high-lift” design. Several studies
have called the Pack B profile a high performance blade, and as such a “high-lift” airfoil. One
common measure of the amount of available work which can be extracted from a turbine blade is
the Zweifel loading coefficient, $Z_w$, defined in this study as Equation 1.1:

$$Z_w = 2 \frac{S}{B_x} \cos^2 \beta \left[ \frac{u_1 \tan \beta_1 + u_2 \tan \beta_2}{u_2} \right]$$

(1.1)

where $S$ is the suction surface length of the blade, $B_x$ is the axial chord, $\beta_1$ and $\beta_2$ are the inlet
and exit gas flow angles, respectively, and $u_1$ and $u_2$ are the inlet and exit streamwise velocity
components, respectively. The Zweifel loading coefficient was originally developed to determine
the number of blades and solidity needed for optimal turbine performance [Zweifel 1945; Hill and
Peterson 1992]. Unfortunately, not all published work adheres to any standard guideline such as
including their Zweifel loading coefficient with other results. As this work produces a new low-
pressure turbine blade with a loading characteristic higher than any previously published design, it
is somewhat abstract and silly to come up with a “really-really-high-lift” LPT designation, although
other studies would lead one to consider such a designation. Therefore in this study, a “high-lift
design” will be considered a nominal $Z_w > 1.15$. 
1.5. WHAT IS A HIGH-LIFT DESIGN?

1.5.1 The Pack B Low-Pressure Turbine

Pratt & Whitney introduced the Pack B LPT design in order to provide a then-current higher-lift design with an improved low-$Re$ efficiency in the LPT section [Clark 2005]. The Pack B turbine is an incompressible Mach-number scaled version of a conventional geometry used in commercial aircraft, and results in a nominal $Zw = 1.15$. The Pack B geometry has been used as a baseline LPT for many researchers over many topics, and this trend will continue here. Overall LPT performances presented in this dissertation will be compared to the Pack B as a baseline.

1.5.2 Previous Design and Higher-Lift Efforts

Previous design efforts have included derivatives of the Pack B as well as completely new airfoils. Concepts have included front-loaded, mid-loaded, and aft-loaded designs. Some studies have included LPT-relevant phenomena such as disturbances or wakes generated by upstream blade rows which convect through the engine and disturb the flow around the LPT. Wakes are an important viscous phenomena as they can (but not always) dissipate over longer time scales than inviscid pressure gradients [Dring et al. 1982], and therefore can propagate and cause more interactions than inviscid pressure waves alone. The generated wakes attempt to mimic the actual LPT environment by providing frequent, localized concentrations of disturbance energy which affect the boundary layer in two distinct fashions: an immediate inviscid Kelvin-Helmholtz vortex roll-up due to the wake penetration and roll-up in the boundary layer, and a subsequent diffusion of turbulent kinetic energy brought by the convecting wake [Stieger and Hodson 2004]. The term “wake-induced transition” has been used to label the effects of such phenomena [Mayle and Dullenkopf 1989]. For brevity, only the LPT design studies most relevant to the current work will be described here. More information on other recent LPT design research is included as Appendix A.

In 1997, Curtis et al. studied various blade profiles with $Zw \approx 1$ for LPT applications by changing the suction surface pressure distribution with flaps and inserts over inlet Reynolds number from 70,000 to 400,000 [Curtis et al. 1997]. Noting a great need for higher loaded designs, their study found wakes affect the suction surface loss much more than the trailing edge or pressure surface losses. They proposed the two most significant factors to determine the suction surface velocity distribution are the degree of deceleration along the surface and the location of the velocity peak. This is important since it gives us two distinct properties of an airfoil design to examine closely:

1. “degree of deceleration”: this is the pressure gradient, specifically the magnitude of adverse pressure gradient along the suction surface of an LPT design.

2. “location of velocity peak”: this is the loading characteristic, i.e. where the maximum or
minimum pressure is located on the airfoil. Choices include front- (L2F), mid- (L1M), and aft-loaded (Pack B) designs.

González et al. in 2002 experimentally studied the redesign for high lift of a civil engine LPT, where the LPT section accounted for 20% of the total engine weight and 15% of the total engine cost [González et al. 2002]. They found that profile loss, or the loss generated by viscous effects around the airfoil’s cross-section, accounted for up to 80% of the total LPT losses. Their work supports the idea of an optimum location for the Mach number peak, or point of minimum pressure. Results show as Reynolds number is increased, the suction surface transition length decreases while reattachment moves upstream towards the leading edge. As Reynolds number was decreased, the stagnation pressure loss increased. Their investigation of an unsteady environment by introducing periodic wakes generated by moving bars across the inlet showed an earlier reattachment due to wake-induced transition. While unsteady results were unclear for aft-loading versus front-loading, González’s steady results did in fact show the overall loss increased for an aft-loaded design.

Praisner et al. recently used their attached-flow and separated-flow transition models to develop two new LPT designs based on the Pack B profile [Praisner et al. 2004]. Using optimization techniques, they designed two $Z_w = 1.4$ LPTs: one front-loaded (Pack D-F), and the other aft-loaded (Pack D-A). Compared to the performance of the original $Z_w = 1.15$ aft-loaded Pack B, steady results showed the aft-loaded Pack D-A suffered greater losses over a range of Reynolds numbers while the front-loaded Pack D-F enjoyed decreased losses. The front-loaded design has an earlier suction peak compared to the aft-loaded designs, and this allows the adverse pressure gradient experienced by the front-loaded design to be spread out over a longer suction surface distance. Thus the adverse pressure gradient for the Pack D-F is not strong enough to cause separation before transition as with the aft-loaded designs, but will instead promote transition before separation. The authors also illustrated the inability of the Wilcox $k-\omega$ turbulence model to accurately capture the effects of secondary flows near blade endwalls. With adequate transition modeling, they propose that front-loaded designs do not require the use of flow control (such as vortex generator jets) to maintain efficiency. In addition, later work found that a front-loaded LPT experiences a “row-loss augmentation” (decrease in losses) due to turning the flow earlier and encountering a less mixed-out wake [Praisner et al. 2006].

In 2005, Clark and Koch designed a mid-loaded LPT with $Z_w = 1.34$, a 17% increase over the Pack B [Bons et al. 2005]. Their mid-loaded design reduced the adverse pressure gradient in the latter uncovered turning portion of the blade. Their work also examined the feasibility of designing LPTs with integrated flow control, in their case vortex generator jets (VGJs). VGJs allow an equivalent loading with a reduced axial chord, an increased loading at constant chord and solidity,
or a decreased solidity (increased pitch) at constant loading. Sondergaard et al. have shown that the use of VGJs on the Pack B profile allows an equivalent loading with 50% fewer blades [Sondergaard et al. 2002]. Both experimental and numerical studies have agreed that VGJs can effectively control separation with pulsed blowing at a duty cycle as low as 10% [Sondergaard et al. 2002; McQuilling 2004; Postl et al. 2004].

In 2006, Reimann et al. experimentally studied the Pack B and L1M profiles at a chord Reynolds number of 20,000 and $FSTI = 0.3\%$ with hot-film anemometry in order to investigate the efficacy of VGJs with different loadings and separation characteristics [Reimann et al. 2006]. VGJs were placed at 9% axial chord upstream of the respective separation onset locations, and were pulsed at 5 Hz with blowing ratios of 2 for the L1M and 3 for the Pack B. Their baseline no flow control tests found the L1M transitions before the Pack B due to the more forward (upstream) loading characteristic, with minimum $C_p$ occurring at 47% and 63% axial chord for the L1M and Pack B, respectively. The L1M transition length was also longer than the Pack B; this was attributed to a closer proximity to the wall where the transition in the shear layer took place. Their work also showed earlier reattachment than previous MISES CFD results using Praisner and Clark’s separated-flow transition model [Bons et al. 2005]. The recent studies highlighted above, as well as those included in Appendix A, have been organized into two tables which summarize some important conclusions regarding LPT design. Tables 1.1 and 1.2 list the researcher(s), year of publication, airfoil shape(s), test or simulation flow conditions, method of examination, and highlighted results. A quick glance over these two tables gives one some insight into the motivation and philosophy behind the current design study.

Examining the summary of recent design efforts presented in Tables 1.1 and 1.2, it appears that there is support for both front-loaded and aft-loaded LPT designs. The inclusion of wakes seems to be the dividing factor which determines whether or not the aft-loaded design will be beneficial. The pros and cons of wakes have been well-documented, and the front-loaded design performs well in both situations. This suggests the most robust LPT design over all flight conditions should be a front-loaded design. It has also been pointed out that only improved transition modeling will allow the high-lift solution to the low-Reynolds number LPT lapse in efficiency. By using Dan Dorney’s (NASA Marshall) Wildcat flow solver [Dorney and Davis 1992] with Praisner and Clark’s transition model to design a front-loaded, higher-lift LPT with an improved low-\(Re\) characteristic, the current study validates the transition model for turbomachinery design purposes and answers the call of the gas turbine industry for well-behaved higher-lift airfoils which can decrease the required blade count in modern gas turbine engines.
### 1.5. WHAT IS A HIGH-LIFT DESIGN?

<table>
<thead>
<tr>
<th>Researcher(s)</th>
<th>Year</th>
<th>Airfoil(s)</th>
<th>Conditions</th>
<th>Method</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cicatelli &amp; Sieverding</td>
<td>1997</td>
<td>FL,LPT</td>
<td>$Re = 2 \cdot 10^6$</td>
<td>EXP</td>
<td>PS dominance in shedding spectrum; TR's BL has 2 maxima in spectra</td>
</tr>
<tr>
<td>Curtis et al.</td>
<td>1997</td>
<td>$Z_w \approx 1$ LPT</td>
<td>$Re = 7 \cdot 10^4$ to $4 \cdot 10^5$</td>
<td>EXP</td>
<td>wakes primarily affect SS</td>
</tr>
<tr>
<td>Wolff et al.</td>
<td>2000</td>
<td>Pack B-like LPT cascade</td>
<td>hot-wire, wakes</td>
<td></td>
<td>wakes allow greater $dp/dx$, increase local $\tau_w$; calmed region benefits</td>
</tr>
<tr>
<td>Dennis et al.</td>
<td>2001</td>
<td>FL, ML, AL LPTs</td>
<td>optimization: GA,SQP for min P-loss</td>
<td>2D NS, $\kappa - \epsilon$ TU</td>
<td>thicker LE and FL design has lowest loss</td>
</tr>
<tr>
<td>Howell et al.</td>
<td>2001</td>
<td>AL, $Z_w \approx 1$ LPTs</td>
<td>$Re = 1 \cdot 10^5$ to $2.1 \cdot 10^5$, FSTI=0.5%</td>
<td>EXP</td>
<td>$Re_\theta \sim 250$ for WIT; AL = increased losses when separated</td>
</tr>
<tr>
<td>Howell et al.</td>
<td>2002</td>
<td>$Z_w \approx 1.05$ BR710 &amp; BR715 LPTs</td>
<td>$Re = 6 \cdot 10^4$ to $1.2 \cdot 10^5$</td>
<td>EXP</td>
<td>$Re_\theta \sim 250$ for WIT; WIT occurs only if already separated</td>
</tr>
<tr>
<td>González et al.</td>
<td>2002</td>
<td>high-lift civil LPT, AL &amp; FL</td>
<td>EXP</td>
<td>wakes</td>
<td>$Re \uparrow = TR_{length} \downarrow$. RE upstream, loss ↓; AL = ↑ loss</td>
</tr>
<tr>
<td>Sieverding† et al.</td>
<td>2004</td>
<td>compressor blade</td>
<td>optimization: range and performance</td>
<td>3D MISES</td>
<td>GAs w/ mutation better than GSM, NN</td>
</tr>
<tr>
<td>Sonoda† et al.</td>
<td>2004</td>
<td>compressor blade</td>
<td>optimization: min P-loss</td>
<td>low-Re NS w/ Chien's $\kappa - \epsilon$ TU</td>
<td>FL blade = earlier TR = separation resistance</td>
</tr>
</tbody>
</table>

* Abbreviations defined in Nomenclature.
† Compressor optimization, not turbine.
### 1.5. WHAT IS A HIGH-LIFT DESIGN?

Table 1.2: Recent Design Efforts Summary, Part 2*.

<table>
<thead>
<tr>
<th>Researcher(s)</th>
<th>Year</th>
<th>Airfoil(s)</th>
<th>Conditions</th>
<th>Method</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>Houtermans et al.</td>
<td>2004</td>
<td>$Z_w = 1.47$</td>
<td>$Re = 5 \cdot 10^4$ to $2 \cdot 10^5$; $FSTI=0.6%$</td>
<td>EXP</td>
<td>$Re \uparrow = \uparrow$ loss; accurate TR modeling difficult for varied loading</td>
</tr>
<tr>
<td>Stieger et al.</td>
<td>2004</td>
<td>$Z_w \approx 1$</td>
<td>low $Re$</td>
<td>EXP</td>
<td>wake-BL interactions different inside separation bubble</td>
</tr>
<tr>
<td>Praisner et al.</td>
<td>2004</td>
<td>high-load</td>
<td>low-$Re$, 3D RANS sims</td>
<td>apply new TR model in design process</td>
<td>FL LPT outperforms ML and AL profiles; intermittency not considered for LPT design</td>
</tr>
<tr>
<td>Zhang &amp; Hodson</td>
<td>2005</td>
<td>$Z_w = 1.19$</td>
<td>$Re = 1.3 \cdot 10^5$ to $2.6 \cdot 10^5$</td>
<td>EXP</td>
<td>Re-dependent wire diameter; AL only with trips/wakes</td>
</tr>
<tr>
<td>Bons et al.</td>
<td>2005</td>
<td>$Z_w = 1.34$</td>
<td>low $Re$</td>
<td>EXP</td>
<td>VGJs allow ↓ chord, ↑ loading, or ↓ solidity; L1M stall-free down to $Re = 2 \cdot 10^4$</td>
</tr>
<tr>
<td>Reimann et al.</td>
<td>2006</td>
<td>$Z_w = 1.34$</td>
<td>$FSTI = 0.3%$</td>
<td>EXP</td>
<td>L1M TR’s before Pack B with longer TR length; VGJ lag &amp; effectiveness is blade-dependent; VGJ calming effect</td>
</tr>
</tbody>
</table>

* Abbreviations defined in Nomenclature.
1.6 Current Study

The current design study is part of an ongoing research plan undertaken by the US Air Force Research Laboratory (AFRL) at Wright-Patterson Air Force Base in Ohio. The research plan seeks to find the limits of stall-free loading by exploring and expanding the current design space for low-pressure turbine airfoils using state-of-the-art design codes and modeling capability. The previous research summarized in Tables 1.1 and 1.2 has used both computational and experimental methods to determine which type of LPT blade profile performs the best over a range of Reynolds number and relatively low freestream turbulence. The current transition modeling employed in this work, which allowed the aggressive loading levels of the L2F, is also currently being utilized for other platforms [Clark and Koch 2006]. These platforms include a transonic high-pressure turbine for advanced government cycles, a single-stage high-pressure turbine developed as an integral part of a supersonic UAV, LPT airfoils designed for Notre Dame’s experimental transonic rig, a low heat load vane geometry available for code validation, and a high-pressure turbine designed for a DARPA (Defense Advanced Research Projects Agency) study on fluidic control with MIT and Lockheed. What’s missing from the current literature is an answer to the call from the gas turbine industry for improved transition modeling to push the boundaries of the LPT design space and in doing so create an LPT design which allows the higher loading while maintaining or improving the low-Reynolds number lapse in efficiency. In addition to the new LPT design, this study provides the optimization needed for application of a shear and stress sensitive film to low-speed air flow environments on a curved surface, effectively providing the experimental fluids community with another non-intrusive measurement tool which can provide simultaneous regional maps of surface pressure and tangential stress on a surface. In summary, the current study accomplishes the following goals:

1. Expand gas turbine engine industry’s current LPT design space by validating the use of Praisner and Clark’s transition modeling in the design cycle of a higher-lift LPT airfoil with an improved low-Reynolds characteristic.

The current design study shows a well-behaved LPT with very high lift can be produced without the use of unsteady modeling or flow control. The experimental validation of the L2F also compares its performance to the previously-designed Pack B and L1M over a range of Reynolds numbers at a freestream turbulence level of 3.3%. The aft-loaded $Z_w = 1.15$ Pack B profile has a lower lift and high $Re$ lapse; the mid-loaded $Z_w = 1.34$ L1M has a high lift and lower lapse; the front-loaded $Z_w = 1.59$ L2F has an even higher lift with better low-$Re$ lapse. For each test blade, total pressure measurements in the wake define the midspan loss behavior over several chord Reynolds numbers. Thermal anemometry allows the determination of wind tunnel
freestream turbulence level and length scale at the inlet of the cascade. Its use also provides velocity profiles and turbulence spectra in and around the boundary layer, providing a means to validate the transition modeling used in this study.

2. **Demonstrate the use of a shear and stress sensitive film (S3F) for low-speed experimental applications.**

S3F provides skin friction measurements which can be used to determine separation bubble physics such as separation onset and reattachment for a range of Reynolds number. These locations are necessary for the validation of the transition modeling used in this study.

The current study was performed in the Air Force Research Laboratory’s Propulsion Directorate at Wright-Patterson Air Force Base in Ohio. The experimental validation is accomplished in the Low-Speed Wind Tunnel cascade testing facility in Building 252 on base. S3F development occurred in ISSI’s wind tunnel in Dayton, Ohio and in Building 252 on base. This dissertation is divided into 5 chapters and 5 appendices. Chapter 2 describes the computational framework, methodology, and results of the L2F profile design. Chapter 3 contains the experimental arrangement and techniques used in Building 252 as well as the initial low-speed development of S3F conducted at ISSI. The experimental results taken on base are presented in Chapter 4, while a discussion summarizing this work is given in Chapter 5. Appendix A contains more on higher-lift LPT design efforts not included in the introduction. The measurements taken to characterize the AFRL LSWT turbulence scale and decay are presented and discussed in Appendix B. Appendix C contains background information related to pressure and skin friction measurement techniques, while Appendix D describes the operation and data reduction of the S3F technique. Appendix E presents an uncertainty analysis for the experimental techniques and results presented in this study.
2

L2F Design

A designer knows he has achieved perfection not when there is nothing left to add, but when there is nothing left to take away.
– Antoine de Saint-Exuptry

As mentioned in Chapter 1, the mature gas turbine industry concentrates much of its design effort in the reduction of the overall cost of the engine. The high-lift LPT promises a higher loading per blade, providing a greater amount of work extracted per blade. This could be useful in higher-loading take-off or climbing flight situations, as well as in the reduction of the engine cost by lowering the required blade count for a given total loading. This chapter describes the tools used to design the L2F profile, and compares the L2F to two previous designs, the L1M and the Pack B low-pressure turbines.

2.1 Turbine Design and Analysis System (TDAAS)

The computational system used to design and analyze the LPT profiles is the Turbine Design and Analysis System (TDAAS) developed by John Clark of AFRL. The TDAAS is a menu-driven, Matlab-based turbine development system which can optimize both low- and high-pressure turbines. This system incorporates Frank Huber’s Huberfoil airfoil generation system together with Dan Dorney’s Wildgrd grid generator and Wildcat flow solver [Dorney and Davis 1992] into a single graphical user interface configured for Matlab. Once an airfoil (turbine blade) shape is generated, a grid is mapped around the surface and the flow solver can then be executed. TDAAS provides all pre-processing and setup as well as all post-processing required for analysis.
2.2 Airfoil Generation

For airfoils of equal flow turning, the percent change in the pitch-to-chord ratio is approximately equal to the percent change in the Zweifel loading coefficient:

\[
\frac{\text{pitch}}{\text{chord}}_{\text{old}} - \frac{\text{pitch}}{\text{chord}}_{\text{new}} = \frac{(Zw)_{\text{old}} - (Zw)_{\text{new}}}{(Zw)_{\text{old}}} \tag{2.1}
\]

Starting with the Pack B geometry at $Zw = 1.15$ and design pitch-to-chord of 0.885, Equation 2.1 was used to create the nominal L2F design at $Zw = 1.59$ resulting in a pitch-to-chord of 1.22. This results in a 38% increase in $Zw$ over the Pack B geometry and will therefore allow a 38% decrease in blade count. All airfoils generated in this work have fixed inlet and exit blade metal angles to allow direct comparison.

Using the design and optimization capability within TDAAS, the Pack B geometry was easily translated into the nominal L2F geometry. The Pack B geometry was previously reverse-engineered to fit the *Huberfoil* profile construction [Clark 2005]. Subsequent design changes were then required to optimize the performance of the new design. New candidate profiles were generated using Frank Huber’s *Huberfoil* airfoil shape generation algorithm which is internally executed from within TDAAS. The airfoil is defined using 6 Bezier curves and 13 other shape parameters for the candidate profile including: leading- and trailing-edge diameters and metal angles, axial chord length, height to length ratio, and uncovered turning. The user can choose between a graphical user interface (GUI), text, or graphical editing of up to 16 design parameters. The candidate profile is updated real-time in a viewing window along with curvature, thickness, and area distributions. These features are illustrated in screen shots of TDAAS for the Pack B profile in Figure 2.1.

2.3 Grid Generation

Any numerical CFD solver needs a set of grids over which the governing equations are solved. Dan Dorney’s grid generation program *Wildgrd* was specifically developed to create grids for axial turbomachinery blade rows for use with Dorney’s Wildcat flow solver [Dorney and Davis 1992]. Several different grid clusterings and spacings can be implemented depending on the type of turbine analysis desired, i.e. a supersonic grid vs a heat transfer grid vs a standard LPT grid. All results presented herein have used the standard LPT grid option as developed by John Clark. *Wildgrd* is externally executed from within TDAAS and produces 2D zonal H- and O-grids. The use of O- and H-grids was specifically developed for the design of linear turbomachinery cascades using 3D viscous flow codes [Lee and Knight 1989]. Dorney’s *Wildgrd* generates algebraic H-grids that are
used upstream of the leading edge, downstream of the trailing edge, and in the blade passages. The H-grids are based on the airfoil mean camber line. H-grid lines can be clustered in both the axial and circumferential directions upstream of the leading edge and downstream of the trailing edge. The body-fitted O-grids are generated after the H-grids with an elliptic equation solver and are used to resolve the viscous flow along the surface. *Wildgrid* then solves the elliptic equations (2.2 and 2.3) using a successive line over-relaxation technique to produce the orthogonal O-grids:

\[ \alpha x_{\xi \xi} - 2\beta x_{\xi \eta} + \zeta x_{\eta \eta} = -J^2(P x_{\xi} + Q x_{\eta}) \]  

(2.2)

\[ \alpha y_{\eta \eta} - 2\beta y_{\eta \xi} + \zeta y_{\xi \xi} = -J^2(P y_{\eta} + Q y_{\xi}) \]  

(2.3)

with

\[ \alpha = x_{\xi}^2 + y_{\eta}^2 \]  

(2.4)

\[ \beta = x_{\xi} x_{\eta} + y_{\xi} y_{\eta} \]  

(2.5)

\[ \zeta = x_{\eta}^2 + y_{\eta}^2 \]  

(2.6)

where \( J \) is the Jacobian of the transformation matrix and \( P \) and \( Q \) are functions used to control clustering and orthogonality near walls. The blade-normal direction is stretched to provide fine grid...
spacing at the wall with $y^+$ values less than unity. There are typically around 7,000 grid points per blade passage for the 2D model, and approximately 7 grid points per momentum thickness in airfoil boundary layers. The grid densities and spacings used for this study are consistent with industry design practices to capture thermal fields, surface heat transfer, and transition-related streamwise gradients in gas turbines (see, e.g. [Praisner and Clark 2007]). In addition, two four-point coordinate sets define the overlap between the O- and H-grids. These sets of coordinates help define the inner and outer boundaries of the O-H overlap. The points inside the overlapped inner boundary of the H-grid are not used to solve the governing equations. The governing equations are solved on both grids in the region between the inner and outer specified boxes, each box being defined by its respective four-point set of coordinates. Overlap of the grids helps ensure stability of the solution but can add computation time due to the calculation of redundant grid locations. A screen shot of a typical O-H grid mesh around the Pack B profile is shown in Figure 2.2.

2.4 Flow Solver

Wildcat is a quasi-3D, time-marching, implicit, zonal-grid, unsteady multiple blade row flow solver [Dorney and Davis 1992]. It solves the full or thin-layer Reynolds-Averaged Navier-Stokes equations by using a dual-time-step, linearized, approximate factored, upwind finite difference scheme. The upwind formulation, which inherently possesses dissipation that controls numerical instabilities
2.4. FLOW SOLVER

(a) Separated-flow model.  
(b) Attached-flow model.

Figure 2.3: Transition model databases, from [Praisner and Clark 2007].

[Tannehill et al. 1997], is third order accurate in space and second order accurate in time.

The separated-flow transition model developed by Praisner and Clark [Praisner and Clark 2007] is used in this work to design the L2F. It is an empirically based model for RANS solvers intended for use in airfoil design systems. The model was constructed from a CFD-supplemented database of 47 separated-flow transition with turbulent reattachment experimental cascade test cases. The separated-flow model was developed in conjunction with an attached-flow model based on 57 experimental test cases. Dimensional analyses led to the appropriate parameters in each situation. The separated-flow model is a correlation relating the momentum thickness Reynolds number at separation onset with the length of the separation bubble from onset to transition in the bubble’s shear layer. The separated model equation is presented below:

\[
\frac{L_{sep}}{S_{sep}} = C Re_\theta^{D_{sep}}
\]

where \( L \) is the length along the bubble from onset to transition, \( S_{sep} \) is the suction surface distance from the leading edge to the onset of separation, \( C = 173.0 \), and \( D = -1.227 \).

The attached-flow model implies transition onset occurs when a ratio of a boundary layer diffusion time to a timescale of local large-eddy turbulent fluctuations (integral time scale) reaches a critical level. This relationship can be cast as Equation 2.8 below:

\[
Re_\theta = A \left( \frac{T u \cdot \theta}{\lambda} \right)^B
\]

where the momentum thickness Reynolds number is equal to the RHS at transition; in the model \( A = 8.52 \), \( B = -0.956 \), and \( Tu, \theta, \) and \( \lambda \) are the turbulence intensity, momentum thickness, and integral length scale at the boundary layer edge at the transition location. Data from the
2.4. FLOW SOLVER

Development of the models is presented in Figure 2.3. Validation of the models is provided in [Praisner et al. 2007]. Here, turbulent eddy viscosity is calculated using the zero equation Baldwin-Lomax algebraic model [Baldwin and Lomax 1978].

Running a transitional case in Wildcat requires a previously converged laminar run in order to extract the model parameters at separation onset required for the separated-flow transition model. Thus, the transition trip location is determined based on laminar predictions. Turbulent eddy viscosity is then applied after a short transition zone of pre-specified length. It should also be noted the current implementation of the transition model is somewhat conservative. The original development within the Pratt & Whitney design system included the use of the attached-flow model up to the point of separation, the separated-flow model after separation, and the use of an additional quasi-laminar (QL) model which accounted for the effects of freestream turbulence on pre-transitional laminar boundary layers. The exclusion of the attached-flow and QL models results in an over-prediction of reattachment locations, translating into a longer bubble length and an increased loss prediction than that obtained through experiment. The previous use of the separated-flow transition model in creating the L1M profile [Bons et al. 2005] illustrates this phenomenon. The L1M was designed without the attached-flow or QL modeling, and their experimental verification found an under-prediction of onset and separation values of momentum thickness by 15% or more. Regarding the inverse relationship between \( Re \) and bubble length, this causes an over-prediction of the bubble length resulting in a conservative prediction; for \( Re = 20,000 \), the L1M predicted reattachment was 12% chord downstream of the experimental location.

As of 1997, the majority of CFD solvers designed for compressible flows experienced lowered efficiency or decreased accuracy in low Mach number regimes [Tannehill et al. 1997]. These difficulties are believed to be due to the stiff matrices of the ill-conditioned algebraic problem and round-off errors which are amplified due to differing magnitudes of flow variables. As Wildcat is a compressible code, simply changing the Mach number to survey the low Reynolds number range of interest in this study would not produce converged results. Therefore, an alternate method of changing the Reynolds number was employed. Instead of changing the velocity to reduce the Reynolds number, the density was instead modified (and therefore pressure by the ideal gas equation of state) to achieve the desired Reynolds numbers. The former approach of changing the velocity (Mach number) allowed converged results down to a chord inlet Reynolds number of \( 1.5 \cdot 10^5 \), while the latter method allowed converged results down to \( 2 \cdot 10^4 \), a difference of nearly an order of magnitude. Pressure fields converge faster than entropy fields due to different event time scales associated with pressure waves and diffusion [Clark and Grover 2006], and as such convergence in this study was achieved once the pressure field downstream of the trailing edge became periodic. Thus, changing the density and pressure allowed
Wildcat to converge in the lower range of Reynolds number desired for this study.

2.5 Design of Experiments

After the nominal L2F profile was created, further exploration of the design space was required to tailor the blade geometry for optimal performance. The methodology used to find the final L2F profile is outlined in the flow chart of Figure 2.4. First, the nominal design was constructed according to section 2.2. Baseline results were computed from Wildcat, and runs were then executed to find which of the 19 design variables had the most influence on the desired outcome: a front-loaded design with a good low-Reynolds number characteristic. Initially, a Monte-Carlo exploration of the design space was employed in order to find which parameter ranges led to reasonable performance. This first exploration narrowed the 19 variables down to 9, which included the leading edge diameter, leading edge wedge angle, degree of uncovered turning, leading edge ellipse ratio, and the 5 Bezier curve control handles L1, L3F, L3R, L4, and L7. In order to explore this reduced design space, a Matlab Design of Experiments (DOE) algorithm available within TDAAS was employed. This DOE algorithm uses the latin hyper square method which randomly distributes and permutes through the given variable ranges of each selected input variable. The DOE generated 250 variations within the selected design space, and each profile was examined for overall loading coefficient ($Z_w$), total loss ($\Delta P t/Qin$), degree of front-loading (location of minimum pressure), and the magnitude and smoothness of the adverse pressure gradient along the suction surface.

Profiles generated by the DOE include thin as well as thick variations, some more front-loaded than others and some with smoother curvature and pressure gradient distributions. The exit Mach number for these design runs was held constant at 0.2; although this is considered a high-Reynolds number flow and our design intent is to produce a well-behaved airfoil at lower Reynolds numbers, this design philosophy successfully produced a mid-loaded airfoil (L1M) which was reported stall-free down to $Re = 20,000$ [Bons et al. 2005], and is therefore employed in the current work with confidence. Figures 2.5 and 2.6 show examples of selected shapes generated from the latin hyper square DOE. These figures present the selected profile attributes (shape and loading) in red with a blue dotted line background showcasing the final L2F for comparison. A loss parameter defined as the integrated change in total pressure normalized by the inlet total pressure is included in the title of the loading plot; this loss value for the L2F is 0.08969. Various selected profiles generated by the DOE will now be described in order to give the reader an idea of the reasoning behind the L2F selection.

Figures 2.5(a) and 2.5(b) show examples of thin profiles generated by the DOE, one with a lower
2.5. DESIGN OF EXPERIMENTS

Create Zw=1.59 Design

Run simulation for baseline result

Reduce Design Parameters:
9 of 19 variables most influential;
find “reasonable” design limits

Design of Experiments:
MATLAB Latin Hyper Square
generates 250 variations of
9-variable design space

Design Comparison Criteria:
Zw, total loss, minimum pressure
location, magnitude and smoothness
of adverse pressure gradient

Select L2F Design

Figure 2.4: Design process flow chart.
loss and one with a higher loss value compared to the L2F. Both profiles also exhibit a more front-loaded characteristic with their respective pressure minimums in front of the L2F. Profiles such as these were not selected due to the non-smooth pressure gradient along the suction surface. A non-smooth pressure gradient such as in 2.5(b) induces oscillating forces on the flow, first decelerating from the pressure minimum to nearly 38% axial chord, then accelerating as the pressure gradient decreases until nearly 58%, then again decelerating the flow as the pressure rises to meet the trailing edge. In addition, these profiles induce a more severe magnitude of adverse pressure gradient (as evidenced by the slope of the pressure loading curve) which is known to cause separations to occur in lower Reynolds situations. Therefore, profiles which exhibit a less degree of adverse pressure gradient are favorable. Thicker designs, such as those in Figures 2.5(c) and 2.5(d), can allow a smaller magnitude pressure gradient but often induce a greater loss possibly due to the increased flow area blockage. Again, care must be taken with respect to where the point of pressure minimum is located - a more front-loaded design inherently spreads the adverse pressure gradient out over a longer suction surface distance, thus reducing the likelihood of separation. As some have suggested [Dennis et al. 2001], global shape optimizations work better to achieve the desired performance characteristics over inverse design methods, and Figure 2.5(d) may lend some support evidence to this argument. The loading achieved with this profile is very similar to the final L2F profile, but much more material is required for this blade over the L2F due to its thicker cross-section. Keeping in mind that an objective of this design should be the minimization of engine weight, the L2F is clearly superior (as long as the material density is the same for both cases). Perhaps an inverse design method used in conjunction with additional DOE or optimization studies could do better than either alone. Figures 2.5(e) and 2.5(f) show profiles which are more front-loaded and have an increased height-to-length ratio than the final L2F design. Again, these results are for an exit Mach number of 0.2, while a desired outcome is for improved low-Reynolds performance which dictates the adverse pressure gradient be as small as possible. Therefore, designs with gradients towards the upper end of the spectrum were not considered for the final design.

Figures 2.6(a) through 2.6(f) show examples of designs which are similar to the final L2F design. Again, attention was paid to the integrated loss values as well as the pressure minimum peaks and magnitude and smoothness of the adverse pressure gradients. These plots, in particular Figures 2.6(b) and 2.6(d), show how very small changes to the suction surface of the blade can result in significant differences in performance. A smooth curvature is also required when considering available manufacturing methods, which undoubtedly become increasingly difficult when adding more complexity to the design.
Figure 2.5: Variation in profiles generated by DOE.
Figure 2.6: L2F-like profiles generated by DOE.
2.6 The Result: L2F

The design intention was to produce a front-loaded LPT with a nominal Zweifel loading coefficient near 1.6. By tailoring the leading edge and suction surface design parameters, the LPT should perform better than the Pack B LPT in the low Reynolds flow regime, thus answering the gas turbine industry’s call for a higher-lift LPT with an improved low-Re characteristic. The latter portions of the resulting L2F profile is presented and compared to the Pack B and L1M geometries in Figure 2.7. The Pack B profile was developed by Pratt & Whitney in the 1990s and has a moderate lift ($Z_w = 1.15$) and high Re-lapse; the L1M blade developed by Clark and Koch in 2005 has lower Re-lapse while producing higher lift ($Z_w = 1.34$), allowing a 17% decrease in blade count and weight; the L2F profile designed herein has a lower Re-lapse at an even higher lift ($Z_w = 1.59$), which allows a 38% reduction in blade count and weight. Again, the blade metal angles were held fixed to allow direct comparison of results. Figure 2.7 also gives the reader an idea of the different blade spacings achieved with the advanced blades.

Figure 2.7: Geometric comparison of Pack B, L1M, and L2F profiles.
As mentioned in the introduction, Curtis et al. suggested two important characteristics of an LPT suction surface that we should consider during the design cycle: the location of the pressure minimum and the magnitude of adverse pressure gradient along the suction surface. Figure 2.8 presents Wildcat’s transitional predictions of the coefficient of pressure distributions for the Pack B, L1M, and L2F profiles over various values of $Re$. This figure plots the difference between the local surface pressure and total inlet pressure, normalized by the inlet dynamic pressure, and is plotted with the classical inverse y-axis so the suction surface is on top. The Pack B, L1M, and L2F axial chord locations of pressure minimums are 62%, 48%, and 27%, respectively. Although not calculated, the magnitude of pressure gradient along the suction surface (slope of $C_p$ curve) can also be seen in Figure 2.8, and shows that pushing the loading towards the leading edge allows a milder distribution of adverse pressure gradient along the suction surface by spreading out the total pressure change from the pressure minimum to the trailing edge over a greater distance. Therefore, front-loaded designs are more intrinsically separation-resistant due to the reduction of the adverse pressure gradient, which is known to be a primary factor in generating separation zones. It is interesting to note these pressure distributions do not exhibit the pressure plateau commonly associated with separation bubbles. We’ll see later that these flows are in fact separated, but may have very thin separation bubbles.
2.7 Chapter Summary

Chapter 2 presented the computational design of the L2F airfoil. The Turbine Design and Analysis System in use at AFRL was outlined, including airfoil and grid generation, the Wildcat RANS flow solver, and the transition modeling developed by [Praisner and Clark 2007]. The two models, one for attached-flow and one for separated-flow transition, were intended for use in RANS-based airfoil design systems and were created by examining 47 experimental test cases of separated-flow transition with turbulent reattachment and 57 cases of attached-flow transition. Dimensional analyses led to the appropriate model representations, where the separated-flow model predicts the length along the separation bubble from separation onset to transition, and the attached-flow model defines the ratio of a boundary layer diffusion time to a local turbulent timescale which must match a critical level for transition to occur. The L2F design of experiments implemented a latin-hyper square method which randomly permuted through the design variables to create 250 variations of the design space. These variations included thick and thin designs with various degrees of front-loading and adverse pressure gradient. It was also shown how inverse design methods may possibly miss optimal designs, as thicker designs achieved similar pressure distributions as the final L2F, but with much more material these thicker designs were not chosen due to the requirement for reduced weight. The final L2F design was not chosen from the generated 250 variations; these variations instead provided insight into which levels of design variables produced the intended design: a high-lift front-loaded airfoil which performed well in the low Reynolds regime. The final design therefore became a mix of those generated by the DOE, resulting in a nominal $Z_w = 1.59$ airfoil with its suction peak at 27% axial chord. This front-loaded profile provided a 38% increase in Zweifel loading coefficient, representing a 38% reduction in blade count over an airfoil typical of those in service today (e.g., the Pack B). This increase in lift also came with an improved Reynolds lapse characteristic which promises longer loiter times, a higher altitude flight ceiling, and reduced fuel consumption, all of which decrease the total cost of the gas turbine engine over its life cycle.
Experimental Arrangement

The satisfaction derived from solving a problem with an experiment is a very heady experience, almost addicting.

– Paul Berg

Several experimental measurement techniques are used in this work in order to validate L1M and L2F airfoil performance as well as verify the transition modeling used in the design process. All results presented in Chapter 4 were taken in AFRL’s Low-Speed Wind Tunnel (LSWT) facility in Building 252 at Wright-Patterson Air Force Base in Ohio. This chapter briefly describes this facility along with the experimental techniques employed. In order to generate approximately 3.3% freestream turbulence intensity at the test section, a turbulence-generating grid was placed upstream of the test section. Appendix B describes the measurements taken in order to characterize the turbulence decay and length scale generated by the upstream grid. In addition, this chapter contains the initial development of the S3F technology accomplished in the Innovative Scientific Solutions, Inc. wind tunnel in Dayton, Ohio. This initial development helped set the framework for S3F application in low-speed air flow tests on a curved surface.

3.1 Low-Speed Wind Tunnel

The AFRL Low-Speed Wind Tunnel (LSWT) is inductively driven by a 125-hp electric motor powering an axial flow fan located downstream of the 0.85m tall by 1.22m wide test section. The test section is constructed of clear polycarbonate (Lexan) for optical access and is arranged to allow quick interchange of test cascades. This configuration can produce air inlet velocities up to 80 m/s. Honeycomb flow straighteners follow the 3.0m by 2.7m rectangular bell-mouth inlet with a gradual 8:1 area contraction which produces a uniform, low-turbulence velocity profile at the cascade test
3.1. LOW-SPEED WIND TUNNEL

Figure 3.1: AFRL's Low-Speed Wind Tunnel.

Section inlet. Figure 3.1(a) shows the full view of the AFRL LSWT tunnel while Figure 3.1(b) shows the top view of the test section for the Pack B cascade with 8 blades. The test section is constructed to accommodate interchangeable packs of blades which can slide into and out of the main section, and it contains adjustable tailboards to set proper periodicity. These tailboards can be slid toward or away from the cascade and can be angled to accommodate various inlet and exit angles. The L1M and L2F cascades were fitted with 7 blades to achieve periodicity, and the axial chords of the airfoils were 7in and 6in, respectively. Both airfoils have a 0.876m span with an inlet gas angle of 35 degrees and an exit gas angle of 60 degrees. A turbulence-generating grid can be placed 2.3m upstream of the test blade leading edge in order to generate 3.3% freestream turbulence at the cascade inlet. The grid is a square-mesh array of 2.54cm diameter tubes spaced 7.6cm apart, producing unsteady uniformity of ±0.3%. Over the Reynolds number range from 25,000 to 75,000, the grid produces an integral length scale between 3.51 and 4.19cm, a Taylor microscale between 0.38 and 0.81cm, and a Kolmogorov length scale between 0.08 and 0.05cm at the entrance to the cascade. Details of the turbulence characterization can be found in Appendix B. Upstream flow diagnostics include a thermocouple for inlet temperature, an inlet hot-wire to determine the test Reynolds number, and a pitot-static reference probe. There are also four 1.5m long traverse slots located atop the cascade both upstream and downstream of the linear cascade. A three-axis traverse can be placed atop the tunnel to measure a planar section of the wind tunnel through these slots; its position is accurate to within ±0.5mm. A smaller 3-axis traverse can be placed inside the tunnel and is accurate to within ±0.01mm; this smaller traverse is used to position the hot-film probe in the boundary layer. Loss coefficients across the the wake of a cascade are measured with a Kiel probe containing a 1.5mm diameter sensor head plumbed to a differential pressure transducer with range of -0.2 to 0.8 inches
3.2 Techniques and Data Processing

The standard flow diagnostic capability used in the AFRL LSWT includes pressure probes, hot-wire anemometry, and particle image velocimetry. An instrumentation demonstration was also conducted using a shear and stress sensitive film (S3F) developed by Innovative Scientific Solutions, Inc.

3.2.1 Pressure Measurement

Pressure probe measurements give the pressure at a single point in the wake of the turbine blades. All pressure data was taken at 1kHz for 30,000 samples with a 5s settling time in between successive measurements. The pressure probe setup uses a Druck LPM5481 differential transducer with a range of -0.2 to 0.8 in of water (-50 to 200 Pa) with output of 0 to 5 Volts, powered by a GW Instek GPS 3303 3-channel DC power supply. The pressure data is digitized and transferred to a PC through an 8-slot National Instruments PXI-1010 chassis with 16-bit A/D conversion; the chassis supports both PXI and SCXI connections. The probe is traversed across the wake in order to quantify the losses caused by the airfoil. Loss is generally defined as the deficiency in total pressure between the exit and inlet. As the instruments used herein offer differential pressure data, the following definitions are used to define area-averaged and flux- or mass-averaged losses:

\[
L_{\text{area}} = \frac{(P_{t,\text{in}} - P_{t,\text{ex}})}{(P_{t,\text{in}} - P_{s,\text{in}})} \\
L_{\text{flux}} = \frac{\sum u_{\text{ex},i} \cdot (P_{t,\text{in}} - P_{t,\text{ex}})_i}{\sum u_{\text{ex},i}} \frac{(P_{t,\text{in}} - P_{s,\text{in}})}{(P_{t,\text{in}} - P_{s,\text{in}})}
\]

(3.1)  

(3.2)

The wake loss measurements are compared to predictions from Wildcat in Chapter 4.

3.2.2 Thermal Anemometry

Thermal anemometry is a point velocity measurement commonly used in contaminant-free, nearly isothermal gas flows where the mean velocities and turbulence intensities are not extremely high [Bernard and Wallace 2002]. The physical principle used by hot-wire anemometry is that the rate of cooling experienced by a thin heated wire can be nonlinearly related to the local flow velocity through King’s Law. The algorithm which uses King’s Law in this study to convert hot-wire signals to velocity has been developed over many years at WPAFB and is different from the four-constant model presented in [Bernard and Wallace 2002]. Therefore, it is described in more detail here. First,
3.2. TECHNIQUES AND DATA PROCESSING

the temperature of the wire, $T_{\text{wire}}$ [K], is calculated using the sensor operating resistance, $R_{\text{op}}$ [Ω], resistance at 0°C, $R_0$ [Ω], and difference in resistances between 100 and 0 °C, $R_{100-0}$ [Ω]:

$$T_{\text{wire}} = \frac{R_{\text{op}} - R_0}{R_{100-0}} \times 100 + 273.15 \ (3.3)$$

The mean sensor film temperature, $T_m$ [K], is then calculated using $T_{\text{wire}}$ and the freestream temperature, $T_\infty$ [K], as measured by an upstream thermocouple:

$$T_m = \frac{T_{\text{wire}} + T_\infty}{2} \ (3.4)$$

Next the density of air, $\rho$ [kg/m³], dynamic viscosity, $\mu$ [kg·m⁻¹·s⁻¹], thermal conductivity, $k$ [W·m⁻¹·K⁻¹], and Prandtl number, $Pr$, are calculated using $T_m$, the atmospheric pressure read by a local barometer, and the dew-point temperature read with a hand-held hygrometer made by Control Company. The power dissipated by the wire is then calculated according to Equation 3.5:

$$\text{power} = \text{volt}^2 \times \frac{R_{\text{op}}}{(R_{\text{op}} + 10)^2} \ (3.5)$$

where $\text{volt}$ is the voltage reading from the anemometer. The local Nusselt and Reynolds numbers are then calculated according to Equations 3.6 and 3.7, respectively:

$$Nu = \frac{\text{power} \times (T_{\text{wire}})^{\text{TempPowRatio}} \times Pr^{-1/3}}{\pi \times L_{\text{wire}} \times k \times (T_{\text{wire}} - T_0)} \ (3.6)$$

$$Re = \left( \frac{Nu - C_{\text{int}}}{D_{\text{slope}}} \right)^{(1/Re_{\text{exp}})} \ (3.7)$$

where $\text{TempPowRatio}$ is a factory-set value used to account for differences between calibration and operation temperatures, $L_{\text{wire}}$ is the length of the sensor, $C_{\text{int}}$ and $D_{\text{slope}}$ are the calibration constants of the sensor, and $Re_{\text{exp}}$ is the experimentally determined Reynolds number of the bulk fluid flow. Finally, the local flow velocity, $Vel$ [m/s], is determined from Equation 3.8:

$$Vel = \frac{Re}{(\rho \times d_{\text{wire}})/\mu} \ (3.8)$$

where $d_{\text{wire}}$ [m] is the diameter of the sensor. The local flow temperature, $T_{\text{flow}}$ [K], can also be calculated with Equation 3.9:

$$T_{\text{flow}} = T_0 \left( \frac{(1/2\rho Vel^2 + P)/P}{(\gamma - 1)/\gamma} \right)^{(\gamma - 1)/\gamma} \ (3.9)$$

When using the X-wire, a distinct calibration technique is employed in order to gain the additional velocity component. First, the probe is calibrated over the full range of velocity at zero degrees incidence with the probe wires angled approximately 45° to the flow. Next, the probe is rotated through ±30° yaw for high, medium, and low velocities in order to construct interpolation curves which effectively produce a calibrated response good over ±30° yaw for the second velocity component. An
8-channel IFA 300 constant temperature anemometer made by TSI controls the sensor and supplies the voltage data to a PC through the NI PXI-1010 chassis with 16-bit A/D conversion. Two separate sensors are used with the anemometer, a single normal hot-film (model 1211-20) and a cross-flow X-wire hot-film (model 1240-20), both manufactured by TSI. The hot-film sensors are 3.2mm in length and 0.5mm in diameter and consist of a thin film of platinum on a quartz cylinder which does not lengthen or bend when heated. Data with the single normal hot-film was taken at 50kHz for 250,000 samples while data with the x-wire was taken at 2kHz for 40,000 samples. These sensors are used to obtain turbulence spectra in the wind tunnel, flow characteristics in the boundary layer, and flux-averaged loss coefficients over the wake of the linear cascade. Bons et al. have previously used hot-film anemometry to describe the turbulent character of separated flow above the Pack B and L1M blade profiles [Reimann et al. 2006], providing information such as locations of separation onset, transition onset and length, turbulent reattachment, and intermittency (percentage of time the flow is turbulent). An intermittency of one denotes a turbulent flow while an intermittency of zero indicates a laminar flow. The intermittency algorithm developed by Clark [Clark et al. 1994] is employed in this work.

The Clark intermittency algorithm was developed for surface-mounted heat-flux gauges in order to track the leading and trailing edges of passing turbulent spots, and uses the first derivative of the time-resolved, measured quantity of interest with weighting and criterion functions as turbulence discriminators. Although this algorithm was not developed specifically for anemometry signals, it is expected to work equally as well away from the wall since the algorithm operates on signal derivatives which are indicative of transitional activity both on and away from the wall. The discriminator function is defined as:

$$D_i = m_i(q'_i)^2$$

where $q$ is the measurement signal (velocity in this case) and $m$ is the relative signal magnitude defined by:

$$m_i = \frac{q_i - q_{\min}}{q_{\max} - q_{\min}}$$

The first derivative $q'$ is found from the central difference formula:

$$q'_i = \frac{q_{i+1} - q_{i-1}}{2h}$$

The original implementation used the sampling period for $h$; here, $h$ is chosen as the time step in between data points. The criterion function $Cr$ is an exponentially-weighted, centered moving-average based on the detector function:

$$Cr_i = \frac{h^2}{1 + (\tau_s/h)} \sum_{j=i-(\tau_s/h)}^{j=i+(\tau_s/h)} w_j D_j$$

(3.13)
where \( w_j \) is the weighting factor defined by:

\[
w_j = \exp\left( -\frac{0.625}{\tau_s/h} |j - i| \right)
\]

(3.14)

Here, \( \tau_s \) is the smoothing period, and was originally set to 10 \( \mu s \) based on the smallest detectable turbulent spot as determined by the upper limit of the bandwidth of the heat-flux instrumentation. In this study, \( \tau_s \) is set to an integer multiple of the time step in order to maintain proper indices in the summation of the criterion function; this integer is chosen to select the number of surrounding data points used for the summation. Intermittency is determined by evaluating the criterion function against an appropriate threshold found by a trial and error approach. Table 3.1 presents the parameters used for intermittency calculation following Clark’s algorithm, where \( n \) is the smoothing period integer, \( \tau_s = n\Delta t \), and \( CT \) is the Clark intermittency threshold.

<table>
<thead>
<tr>
<th>Re</th>
<th>( n )</th>
<th>( \tau_s )</th>
<th>( CT )</th>
</tr>
</thead>
<tbody>
<tr>
<td>25</td>
<td>9</td>
<td>9</td>
<td>( 9.40e^{-5} )</td>
</tr>
<tr>
<td>50</td>
<td>9</td>
<td>9</td>
<td>( 1.95e^{-4} )</td>
</tr>
<tr>
<td>75</td>
<td>9</td>
<td>9</td>
<td>( 2.75e^{-4} )</td>
</tr>
<tr>
<td>25</td>
<td>9</td>
<td>9</td>
<td>( 1.25e^{-4} )</td>
</tr>
<tr>
<td>50</td>
<td>9</td>
<td>9</td>
<td>( 3.25e^{-4} )</td>
</tr>
<tr>
<td>75</td>
<td>9</td>
<td>9</td>
<td>( 3.75e^{-4} )</td>
</tr>
</tbody>
</table>

There is some subjectivity in selecting these parameters. In the present work, three guiding principles were used as follows, in order of consideration:

1. Maintain the maximum intermittency at the wall in the downstream portion of the blade, subsequent to the establishment of fully turbulent flow.

2. Maintain an intermittency value less than 0.02 at the wall in the upstream, pre-transitional portion of the blade.

3. Maintain an intermittency value less than 0.02 in the freestream over the entire blade.

The guiding philosophy was to select a set of parameters which forced the intermittency to be less than 0.02 in the freestream and upstream of the transition location near the wall, while producing the highest possible intermittency downstream of transition. In fact, a range of parameters in the neighborhood of those in Table 3.1 produced somewhat consistent results, as shown in Figure 3.2(a) for the L2F airfoil near-wall values at \( Re = 25k \) with \( n = 5 \). The figure legend contains various values for the intermittency threshold which show how a certain range, here between \( 1.5e - 4 \) and \( 5e - 5 \), produces a similar intermittency shape. The sudden increase in intermittency near 55% of the suction surface length (SSL) signals transition has occurred, and this trend was sought after...
3.2. TECHNIQUES AND DATA PROCESSING

(a) Parameter selection, L2F $Re = 25k$, $n = 5$.

(b) Sample locations, L2F $Re = 75k$.

(c) Velocity traces, L2F $Re = 75k$.

Figure 3.2: Example of Clark intermittency algorithm.
for each test case. The smoothing integer \( n \) was selected equal to 9 in every case; as \( n \) increases the intermittency obviously increases, but it was found to be a good fit for highest possible intermittency after the sudden increase while maintaining low intermittency before the sudden increase.

Clark’s intermittency algorithm was used in this manner with confidence since a definite spike in intermittency occurred for all cases which produced a region which was shaped as expected. Visual inspection of data signals also gave confidence in the algorithm. An example sequence of velocity trace signals is presented in Figure 3.2(c) for the locations marked by white circles in Figure 3.2(b) of intermittency for the L2F airfoil at \( Re = 75k; \) these locations are 4.8mm (3% chord) up from the surface. In Figure 3.2(c), the first profile at the top corresponds to 37.6\%SSL and each successive profile is plotted until the bottom profile at 90.4\%SSL. By the 7th location (52.6\%SSL) there is evidence of turbulent bursts when the intermittency in Figure 3.2(b) begins to ramp up. Continuing downstream, the random bursts increase in frequency until the flow is more turbulent than not, as indicated in Figure 3.2(b) as well. It is therefore believed that Clark’s algorithm works well for determining the intermittency away from the wall, and intermittency results produced from this algorithm will be presented later in Chapter 4.

Higher-order turbulence statistics including the skew and kurtosis of the fluctuating velocity have also previously been used to identify the turbulent nature of a flow, and are calculated according to Equations 3.15 and 3.16 (from [Bernard and Wallace 2002]):

\[
\text{skew} = \frac{u'^3}{(u'^2)^{3/2}} \tag{3.15}
\]

\[
\text{kurtosis} = \frac{u'^4}{(u'^2)^2} \tag{3.16}
\]

These quantities have previously been used by [Reimann et al. 2006] and others to help identify regions of separated and transitional flow. In particular, Bons’ group identified regions of reversed flow with negative skew, and regions containing both negative skew and positive kurtosis with those undergoing transitional events. This logic will be used in the current results of Chapter 4. In addition, a parameter using the velocities at the edge of the boundary layer will be used to determine the acceleration throughout the passage above the turbine blades. The acceleration parameter, \( K \), is presented as Equation 3.17 below:

\[
K = \frac{\nu}{U_e^2} \frac{dU_e}{d\text{SSL}} \tag{3.17}
\]

where \( \nu \) is the kinematic viscosity, \( U_e \) is the velocity at the boundary layer edge, and \( d\text{SSL} \) is the change in streamwise distance across the suction surface. Using experimental data, the derivative is calculated with a 2nd order central difference using the change in suction surface length as the distance variable.
In order to position the hot-film probe accurately in the boundary layer, a Matlab code was written which takes into account the test section geometry, the cascade position within the test section, the airfoil surface geometries, and the probe and stand positions. This code outputs coordinates of surface-normal test points in the boundary layer for any suction surface location which can be directly input into the traverse control for data acquisition. Figure 3.3 shows the selected profile locations for each blade. These locations were selected based on the traverse and probe range of motion. The L1M is surveyed from 22.5 to 89.5%SSL, while the L2F is surveyed from 19.9 to 90.4%SSL. The profile locations were selected based on a 200pt coordinate set for each blade and a streamwise spacing of approximately 2.5%SSL. L2F profiles contain 50 evenly distributed surface-normal points from 1mm to 20mm up from the surface where possible; the L1M profiles contain from 50 locations to 184 locations, each assigned based on initial findings of boundary layer behavior in order to resolve the boundary layer height. In order to position the hot-film probe around the curva-

![Figure 3.3: L1M and L2F boundary profile locations.](image)

![Figure 3.4: Method for determining attainable profile locations.](image)
3.2. TECHNIQUES AND DATA PROCESSING

ture of each blade, a 90 degree bend adaptor was required between the probe holder and the probe. Figure 3.4 illustrates the process used to determine which profile locations were accessible in and around the test section using this adaptor. Figure 3.4(a) depicts the probe with 90 degree adaptor and probe holder, probe stand, and 3 relevant LPT blades for the L1M cascade. The probe device could be translated to any profile location in order to ensure none of the device conflicted with blade surfaces. Figure 3.4(b) shows an example of an outer location of a profile which is unattainable due to interference from the adjacent blade, while Figure 3.4(c) shows an acceptable location. The locations where data can not be obtained with this setup show up as void spots in contour maps such as those presented later in Figure 4.2.

The production of mean velocity profiles from thermal anemometry also allows shape comparison for laminar and turbulent similarity using boundary layer similarity laws selected from [White 2006]. In this work laminar profiles are considered those with a maximum intermittency value less than 0.02, even though the inlet freestream turbulence is 3.3%. The laminar profiles will be compared to Falkner-Skan wedge flows, which are a single parameter family of non-separating flows which solve the equation:

\[ f''' + ff'' + \beta (1 - f'^2) = 0 \] (3.18)

where \( f' = u/U_{edge} \) and

\[ \beta = \frac{2m}{m + 1} \] (3.19)

with boundary conditions

\[ f(0) = f'(0) = 0 \quad f'(\infty) = 1 \] (3.20)

The parameter \( \beta \) is a measure of the pressure gradient, and is positive for favorable gradients and negative for adverse gradients. This parameter is equal to zero for Blasius flow and has a limit of -0.19884 just prior to separation; thus the \( \beta \) parameter is a measure of the deviation from flat-plate Blasius flow. Profiles are normally plotted with \( f' \) on the ordinate axis and the dimensionless variable \( \eta \) on the abscissa:

\[ \eta = y \sqrt{\frac{m + 1}{2} \frac{U_{edge}}{\nu x}} \] (3.21)

Turbulent profiles are analyzed for similarity by iterating upon the surface shear, \( \tau_w \), which produces similar shapes. In this work turbulent profiles are compared to the linear-law, the log-law, Spalding’s law of the wall, and Clauser’s similarity law [Clauser 1954] with Coles’ addition of the “PI” term which accounts for pressure gradients [Coles 1956], hereafter referred to as Coles’ law of the wake. All of these turbulent similarity were developed for fully developed turbulent flows, and all except Coles’ law were intended for use under zero pressure gradient. These comparisons require the
transformation of velocity and surface-normal location into the wall coordinates given below:

\[
\begin{align*}
    u^+ &= \frac{u_{\text{mean}}}{v^*} \\
    y^+ &= \frac{yv^*}{\nu}
\end{align*}
\]

where \( v^* \) is the friction velocity defined as:

\[
\nu^* = \sqrt{\frac{\tau_w}{\rho}}
\]

with \( \tau_w \) being the wall shear stress and \( \rho \) the density of the fluid. The linear law, given by

\[
u^+ = y^+
\]

is typically applicable for \( y^+ \leq 5 \) and governs the region where viscous (molecular) shear dominates the flow field near the wall. The log-law is given by:

\[
u^+ = \frac{1}{\kappa} \ln(y^+) + B
\]

where \( \kappa \) is the von Kármán constant equal to 0.41 and \( B \) is set equal to 5.0. The log-law region governs the overlap area of the flow where turbulent shear begins to dominate over molecular shear, and is normally applicable in the range \( 35 \leq y^+ \leq 350 \). The third turbulent similarity comparison used in this work is Spalding’s law of the wall given by:

\[
y^+ = u^+ + e^{-\kappa B} \left[ e^{\kappa u^+} - 1 - \kappa u^+ - \frac{(\kappa u^+)^2}{2} - \frac{(\kappa u^+)^3}{6} \right] = u^+ + e^{-\kappa B} \left[ e^{\kappa u^+} - 1 - \kappa u^+ - \frac{(\kappa u^+)^2}{2} - \frac{(\kappa u^+)^3}{6} \right]
\]

This similarity law used fully-developed turbulent pipe flow through its development, and is normally applied from the wall into the outer layer defined as \( y^+ > 300 \). The final turbulent similarity profile used in this work is Coles’ law of the wake, given by Equation 3.27:

\[
u^+ = \frac{1}{\kappa} \ln(y^+) + B + \frac{2PI}{\kappa} f \left( \frac{y}{\delta} \right)
\]

where \( PI \) is the term which accounts for the pressure gradient, and \( \delta \) is the boundary layer thickness. The last term includes a function of surface normal height approximated by:

\[
f \left( \frac{y}{\delta} \right) = 3 \left( \frac{y}{\delta} \right)^2 - 2 \left( \frac{y}{\delta} \right)^3
\]

This function produces the 'S' shape experienced by turbulent boundary layer profiles under the influence of an adverse pressure gradient. Since the wall shear must be iterated upon to match these profiles, and this wall shear determines the wall coordinates used for all turbulent comparisons, all will be compared using the current data sets in case the data falls into one or more of the applicable ranges.
3.2.3 Particle Image Velocimetry

Particle image velocimetry (PIV) is a two-dimensional velocity measurement technique which uses the light scattering property of small tracer particles injected into a flow. The seed particles should be small enough (on the order of microns) to follow the flow without disturbing it, and are illuminated with a laser as they pass through the flow plane of interest. The scattered light in the 2D plane is recorded by a CCD camera, and computer algorithms correlate the scattered patterns between two back-to-back images. The correlation produces the distance traveled of each scattered light group between the images, and knowing the time in between successive images produces the vector velocity field in a 2D plane of the flowfield. A more detailed description of the PIV technique can be found in [Raffel et al. 1998]. The PIV system in building 252 uses a 120mJ Solo 120XT PIV Nd:YAG laser from New Wave Research together with a Rosco 4500 fog generator which creates particles on the order of a few microns in diameter out of propylene glycol seeding material. The laser sheet is constructed using a spherical then cylindrical lens setup and is directed into the test section using a laser arm from Dantec. The image is recorded with a 14bit PCO.1600 CCD camera with 1200x1600 pixel resolution, and data reduction is carried out using DPIV software from ISSI. Figure 3.5 shows the PIV setup for the leading edge of the L2F cascade with the laser arm and optics. In this work, PIV data is only used to verify the boundary layer height obtained from the single normal hot-film results.
3.2. TECHNIQUES AND DATA PROCESSING

3.2.4 Shear and Stress Sensitive Film (S3F)

The shear and stress sensitive film (S3F) provides regional surface maps of tangential shear and normal pressure in a single measurement, and is a new addition to the experimental capability at AFRL implemented by Innovative Scientific Solutions, Inc. of Dayton, OH. The S3F technique originated in the 1990s as a direct sensor to measure surface shear forces [Tarasov and Orlov 1990]. Detailed description of how the technique works can be found in [McQuilling et al. 2007], so only a brief summary will be presented here for convenience. The technique relies on the response of a thin elastomer film impregnated with luminescent molecules and doped with tracer particles on its surface. The film deforms under normal and tangential loads, and the tangential displacement measurement is achieved by tracking the movement of the titanium oxide tracer particles between a “wind-off” non-loaded state and a “wind-on” loaded state. The images are recorded by a CCD camera, and cross-correlation algorithms and optical flow techniques determine the displacements. During both states, the film is also illuminated with a light source of a specific wavelength that excites the luminescent molecules within the film. The molecules emit a different wavelength photon in order to relax back to its more stable ground state, and this emission is picked up by the same optically filtered CCD camera. The intensity of the emission is proportional to the thickness of the film, thus providing a regional measurement of normal deformation between the same “wind-off” and “wind-on” states. A ratio between the two conditions also provides a means to cancel out sources of error such as unequal illumination and uneven luminophore dispersion. This ratio calculation effectively makes the S3F a differential pressure gauge with tunable dynamic range calibration by modifying the film thickness, modulus of elasticity, and Poisson ratio. The normal pressure and tangential surface stresses which caused the three-dimensional deformations are determined from an inverse finite element model, whose inputs are the film thickness-sensitive luminescent intensity and the tracer translations between flow-off and flow-on conditions.

S3F was developed in order to measure two-dimensional static pressure on a surface without some of the drawbacks of pressure sensitive paints (PSPs) based on oxygen quenching, such as the need for oxygen in the flow environment and the limited pressure sensitivity, dynamic range, and frequency response [McQuilling et al. 2007]. More background information on pressure and skin friction measurement can be found in Appendix C, while more detail on the S3F data reduction process can be found in Appendix D.
3.3 S3F Development for Low-Speed LPT Use

Initial work carried out in order to assess the efficacy of using S3F in a low-speed environment is now described. The complications of using S3F arise because the low-speed environments result in minimal surface shearing forces due to the passing fluid. Thus, the film properties must be meticulously calibrated for this difficult environment. Indeed, the calibration and reduction methodology which determines the surface forces are still under development for the low-speed regime. All experiments described in this section were conducted in the low-speed wind tunnel (LSWT) located at ISSI in Dayton, Ohio. The ISSI LSWT is a low-turbulence, open-circuit wind tunnel. Screens upstream of the test section condition the flow and one wall of the test section is made of clear polycarbonate to allow optical access.

3.3.1 Experimental Setup

The LPT geometry used herein is the Pack B profile with a 190.5mm axial chord and 203.2mm span. The blade, with a wrap-around 1.5mm deep × 50.8mm wide S3F cavity, was made using the rapid-prototyping capability at WPAFB, and is shown in Figure 3.6(a). The orientation of the blade in the straight ISSI LSWT test section did not allow for proper inlet and exit angles typically used for LPT studies. Here it is necessary to remember the goals of this study. First, the current operational envelope of S3F has previously been restricted to water environments or higher speed air environments. These tests will push that envelope by extending the air flow use to very low speeds on the order of 1-7 m/s in order to investigate such phenomena as the low-Re lapse in efficiency. Second, issues specific to the LPT geometry, such as the varying gradient levels along the suction surface, were investigated and handled before further testing in realistic LPT orientations. Therefore, all results in this section are presented with respect to the camera point of view and are only referenced to regions along the suction surface where maximum gradient changes are expected, such as the leading edge and near the locations of maximum curvature. The blade with S3F inserts and black Mylar strips applied for oil film measurements is shown in Figure 3.6(b). The suction surface of the LPT blade is illuminated with an ISSI LM4 lamp and detected by a 14-bit PCO.1600 CCD camera with 1600 × 1200 pixel resolution. A single low-pass filter is used to distinguish the fluorescent emission while an ISSI timing box controls the excitation-detection sequence. Data is collected and stored on a PC.
3.3. S3F DEVELOPMENT FOR LOW-SPEED LPT USE

(a) Pack B LPT with S3F cavity.

(b) Side and top views of LPT orientation.

Figure 3.6: Pack B LPT test setup at ISSI.
3.3. S3F DEVELOPMENT FOR LOW-SPEED LPT USE

3.3.2 Results

Multi-dimensional skin friction measurement is not a new accomplishment, and as such previous work has developed the proper theory and terminology used to describe such results. Therefore, the terminology as presented in [Tobak and Peake 1982] will be used to aid the description of presented results. The S3F cavity was filled by two separate pieces of elastomer, one for the leading edge region and one for the trailing edge region (as seen in Figure 3.6(a)). Filling the cavity with multiple films instead of one long continuous film was desirable in order to wrap the film around the leading edge and maintain as flush as possible S3F with the blade surface.

\( h = 1 \text{mm}, \ \mu = 150 \text{Pa} \) Film

Results at \( U = 13.5 \text{m/s} \ (Re = 1.83 \cdot 10^6) \) for a film thickness of 1mm and shear modulus of 150Pa are presented for the front cavity in Figure 3.7. A negative spike in streamwise skin friction (\( \tau_x \)) near the leading edge, indicating surface tension facing upstream, reaches just below -21Pa and is a result of the blade orientation in the straight test section. As the oncoming flow is split between the pressure and suction sides near the nodal line of attachment around \( x=10 \text{mm} \), the flow heading towards the pressure side pulls on the film in the opposite (negative) direction with respect to the suction side. This negative spike region produces a local absolute maximum \( \tau_x \)-gradient of \( \sim 6.4 \text{Pa/mm} \). After recovery to positive \( \tau_x \), the remainder of the leading edge strip encounters a mean streamwise friction level near 7.5Pa, with local gradients between 0-4Pa/mm. The cross-stream skin friction (\( \tau_z \)) along section A-A fluctuates around zero, indicating a mostly two-dimensional flow field. The observed 3D influence is believed to be generated by the mounting plates above and below the turbine blade. Waves in the \( -dC_p \) plot along Section A-A in Figure 3.7(c) indicate that the 1mm-150Pa S3F used near the leading edge was too sensitive for a 13.5m/s flow velocity in the current orientation. Further tests will implement an S3F with a better tuned shear modulus for this region.

![Figure 3.7: Front cavity, U=13.5m/s, h=1mm, \( \mu = 150 \text{Pa} \).](image-url)
3.3. S3F DEVELOPMENT FOR LOW-SPEED LPT USE

Figure 3.8: Rear cavity, U=13.5m/s, h=1mm, µ=150Pa.

Results for a similar 1mm-150Pa film in the rear cavity are shown in Figure 3.8 for 13.5m/s flow velocity. Again a negative spike is noticed around x=30-36mm, but is now due to a surface discontinuity near the upstream edge of the S3F. The rear cavity observes a negative mean τₓ of less than a tenth of a Pascal, indicating a flow separation on the latter part of the suction surface. The streamwise friction gradients in this region are significantly smaller than the front cavity, with absolute values less than 0.04Pa/mm. The cross-stream skin friction τz remains in a similar range as τₓ. The difference in skin friction between the front and rear cavities varies by more than an order of magnitude, and illustrates the need for separate S3Fs in each region. The larger mean and gradient levels near the leading edge require a film with smaller sensitivity than the trailing edge. From these preliminary results, the goal was set to develop an S3F with a shear modulus near 30-50 Pa in order to better resolve the expected magnitudes of pressure and shear.

h=1.5mm, µ=25Pa Film

The next film developed had a shear modulus of 25Pa at a thickness of 1.5mm. Skin friction results for this S3F are presented in Figure 3.9 for a flow velocity of 7m/s. Here, a focus of separation can be seen in the lower right portion of the plot, indicating a swirling motion of the fluid above. The streamwise shear component for the region of interest encompassed by the box on the left side is shown on the right of the figure in 3-D space. From these tests, the need became clear to apply a black Mylar strip beneath the S3F in order to reduce reflected excitation light and glare from the white material. This reduced the noise collected with the data, but increased the complexity of model setup since the S3F is difficult to glue to the black strip material. The increased noise levels for speeds lower than 7m/s increased the difficulty of the second data reduction step of transforming the deformation fields and luminescent intensities into shear and pressure fields. It was also found that
in order to best resolve the expected flow features it may be necessary to apply the S3F in small 5-10mm wide strips in the flow direction with 1-2mm gaps in between successive strips, running any length along the span. Since the pressure and shear stress gradient levels change so rapidly along the flow direction due to the LPT curvature, the S3F applied in strips would allow different composition and sensitivity S3F pieces to be placed in appropriate regions to better resolve the occurring levels in their respective locations. A sequential series of displacement fields taken at 0.5Hz with the same 1.5mm-25Pa S3F for a flow velocity of 13.5m/s is shown in Figure 3.10. The x-axis (streamwise direction) and y-axis (spanwise direction) are scaled to the pixel distribution of the camera only in order to show the ability of the S3F to track unsteady events. In Figure 3.10(a), a nodal line of separation appears near $x \approx 230$ highlighted by the ellipse, with an accompanying quasi-steady nodal line of attachment near $x \approx 1150$ highlighted with a box, indicating the presence of an unsteady separation bubble. Although the topological structure remains relatively preserved, the frame capture rate does not exceed the separation frequency and therefore cannot properly resolve the unsteadiness of the event.

Displacement fields for $U = 0.7$ to 4.4m/s are presented in Figure 3.11 for another 1.5mm-25Pa S3F. Evidence of an interaction with the uneven edge of the S3F on the top side of the cavity can be seen here. Similar topological structure can be seen in Figures 3.11(b) through 3.11(e), while in Figure 3.11(f) we see the emergence of a focus of separation near $(x,y)=(400,900)$ highlighted by an ellipse, and a saddle point of attachment near $(x,y)=(700,850)$ highlighted with a five-sided polygon, indicating the swirling 3D flow above. The top edge interaction eventually influences a nodal line of separation near $x \approx 1150$ highlighted with a rectangle, and a larger separation as the flow velocity approaches 4.4m/s in Figure 3.11(f), as seen by the upstream directed deformations occurring in the left half of the plot. The attainment of these displacement fields at low speeds, along with oil film
3.3. S3F DEVELOPMENT FOR LOW-SPEED LPT USE

verifications, signifies that a proper formulation and implementation of the S3F sensor will be able to produce shear and pressure fields in a flow speed range of 1-7m/s after an adequate amount of noise reduction is accomplished. Additionally, diminishing the region of interest (zooming in) can provide up to 10 times the current sensitivity level used in this paper, providing another means to reduce the noise and increase the signal-to-noise ratio for lower speed tests.

Oil-film Comparison

As mentioned earlier, the lowered signal-to-noise ratio at flow velocities below 7m/s complicated the attainment of shear and pressure fields. Therefore, in order to better trust the small translations indicated in the S3F displacement fields, oil film measurements were made directly below the S3F strip near the front cavity. The combined S3F-oil film setup was presented in Figure 3.6(b). The regions of interest for combined measurement are shown in Figure 3.12. For luminescent oil film skin friction measurements, the skin friction is determined by Equation C.1. Oil film results showing $dx/dh$ as a function of time for dynamic pressures of 4.6 and 29.6Pa are shown in Figures 3.13(a) and 3.13(b). These figures illustrate the linear response of the oil film technique over the dynamic range of interest. The combined S3F-oil film measurements are shown in Figure 3.13(c), where the
3.3. S3F DEVELOPMENT FOR LOW-SPEED LPT USE

(a) U=0.7m/s.  
(b) U=1.8m/s.  
(c) U=2.3m/s.  
(d) U=3.3m/s.  
(e) U=3.9m/s.  
(f) U=4.4m/s.

Figure 3.11: Displacement fields, h=1.5mm, $\mu=25$Pa.

S3F measurements are comparable to the oil film results within $\frac{1}{4}$ to $\frac{1}{3}$Pa.

Conclusions

The S3F technique has successfully produced two-dimensional shear fields for flow velocities from 7 to 13.5 m/s on the Pack B low-pressure turbine blade geometry, corresponding to a Reynolds number range of $9.48 \cdot 10^5$ to $1.83 \cdot 10^6$. The goal of applying the S3F technique in a low-speed LPT environment and optimizing geometry related issues has been accomplished in this work. Due to significant differences of over an order of magnitude in mean shear and over two orders of magnitude in shear gradients between the leading and trailing edges, the use of separate S3F strips for each region is recommended. The leading edge region will require a smaller sensitivity S3F than the trailing edge due to the higher mean and gradient levels near the leading edge. An S3F with a shear modulus of 25Pa and a film thickness of 1.5mm has been successfully used to produce deformation fields at flow velocities from 0.7-4.4m/s, corresponding to an $Re$ range of $8.90 \cdot 10^4$ to $5.97 \cdot 10^5$. In order to accurately resolve shear and pressure fields at these lower speeds below 7m/s, the signal-to-noise ratio must be increased above current levels.
3.3. S3F DEVELOPMENT FOR LOW-SPEED LPT USE

Figure 3.12: S3F-oil film combined measurement regions of interest.

Figure 3.13: Oil film measurements.

(a) q=4.6Pa.  
(b) q=29.6Pa.  
(c) S3F-oil film comparison, h=1.5mm, μ=25Pa.
3.4 Chapter Summary

Chapter 3 presented the experimental arrangement and data processing used for the tests both on base and at Innovative Scientific Solutions in Dayton, OH. The techniques included pressure measurement, thermal anemometry, particle image velocimetry (only for verification of boundary layer heights), and the S3F surface stress film. The hot-film data reduction routine employed in this work is different than the standard polynomial fit, and was developed at WPAFB over many years. The intermittency algorithm developed by Clark for use with surface-mounted heat flux gauges is found to work equally well away from the wall. This algorithm uses two parameters which are somewhat subjective in their selection, i.e., a set of these two parameters (the smoothing period and threshold) in a neighborhood of those chosen in this study could produce similar results, while not changing the overall conclusions. These parameters varied with Reynolds number, and were chosen in this work based on examining velocity traces and constructing an overall intermittency which made physical sense. The initial development of S3F which enabled its use in the low-speed air environment on the curved surface of a turbine blade was also presented. These initial S3F tests concluded that film compositions could in fact be formulated to fit the range of dynamic pressures desired for tests on base in the large subsonic wind tunnel. The biggest hurdle to overcome is harvesting enough of the reduced signal magnitude to overcome the noise in the system. And as air velocity is decreased, the signal magnitude decreases as the square of velocity, since that is the quantity upon which dynamic pressure depends.
4

Results

*It doesn’t matter how beautiful your theory is, it doesn’t matter how smart you are. If it doesn’t agree with experiment, it’s wrong.*

– Richard P. Feynman

This chapter presents and compares Wildcat predictions with experimental results. The performance data of greatest relevance to turbine designers, the Reynolds lapse curve, is presented first, followed by boundary layer contours and profiles for L1M and L2F which shed some light onto why each blade performs as it does. Next the S3F surface film results are presented, followed by the transition model validation.

4.1 Reynolds Lapse

Pressure and velocity measurements taken across the wake approximately one chord length downstream of the test blades provide a means to measure the available energy lost from the bulk fluid flow as it is turned by the linear cascade. The regions of lost available energy show up as deficiencies in velocity (or pressure), and constitute loss levels which can be integrated to form a Reynolds lapse curve. Figure 4.1 compares Wildcat predictions of L1M and L2F to previous experimental data on the Pack B and current results for the L1M and L2F airfoils plotted against inlet chord Reynolds number. Both area-averaged and mass-averaged losses are presented as defined by Equations 3.1 and 3.2; mass-averaged losses are slightly lower than area-averaged due to their accounting of the lower velocities in the wake regions. It should also be noted that the Wildcat CFD predictions were essentially run laminar with a turbulent trip at the model-predicted separated-flow transition onset location. Tables 4.1 and 4.2 present the L1M and L2F experimental results in tabular form for use in CFD validation; the units are [m/s] for velocity and [in H$_2$O] for the pressure measurements.
At first glance it is obvious the L2F blade outperforms the L1M and Pack B in the lower Reynolds number regime. Remembering that the aggressive L2F loading allows a 38% reduction in blade count and weight over the Pack B, the L2F answers the call of the gas turbine industry for reduced weight and improved low Reynolds performance, both of which decrease the overall cost of the turbine engine throughout its life cycle. Although the laminar with trip CFD predictions show the L2F stalls below $Re = 1.9 \cdot 10^4$, this result was not duplicable by the facility.

The Pack B is historically notorious for massive separation at lower $Re$, which begins to grow near $Re = 4.5 \cdot 10^4$ as evidenced by the steep rise in loss in both the experiment and CFD. The L1M loss knee begins below $Re = 5.8 \cdot 10^4$, although it is not as steep as the Pack B. This can be attributed to the mid-loading and earlier transition as noted in other work [Bons et al. 2005]. Even so, the L1M does eventually stall somewhere below $Re = 3.4 \cdot 10^4$, a trend also noticed in the CFD results.

The increased losses over the CFD are believed to be a result of the increased turbulent-wetted area from an earlier transition, as well as enhancement of the local laminar shear due to interaction with the turbulence in the freestream which is not accounted for in the predictions. The L2F, however, does not stall at low $Re$ and its experimental loss behavior does not show the loss knee associated with turbines that experience increased low-$Re$ losses due to separation bubbles. The loss knee is formed as the losses dramatically increase with decreasing Reynolds number. We’ll see later this
flow is in fact separated, but the bubble may be so thin that it behaves as if it is attached (without the loss knee). Comparisons drawn later with boundary layer contours obtained through thermal anemometry help explain this behavior. A very encouraging trend is that both the L1M and L2F enjoy reduced losses compared to the Pack B at the higher $Re$ tested in this work; it was initially suspected the attached-flow losses may be higher than the Pack B due to the earlier transitions, but at least up to $Re = 8.6 \cdot 10^4$ the experimental losses remain below the Pack B levels. Somewhere between $Re = 5.1 \cdot 10^4$ and $Re = 5.8 \cdot 10^4$ the L2F losses increase over the L1M; again, this can be attributed to the increased turbulent-wetted area of the L2F.

Table 4.1: Measured conditions across L1M cascade.

<table>
<thead>
<tr>
<th>$Re_{in}$</th>
<th>$Re_{ex}$</th>
<th>$u_{in}$</th>
<th>$u_{ex}$</th>
<th>$P_{t, in} - P_{t, in}$</th>
<th>$P_{t, in} - P_{t, ex}$</th>
<th>$L_{area}$</th>
<th>$L_{flux}$</th>
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</tr>
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<td>0.0064</td>
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Table 4.2: Measured conditions across L2F cascade.

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<th>$Re_{in}$</th>
<th>$Re_{ex}$</th>
<th>$u_{in}$</th>
<th>$u_{ex}$</th>
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<th>$P_{t, in} - P_{t, ex}$</th>
<th>$L_{area}$</th>
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<td>7.5</td>
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<td>0.0060</td>
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<td>0.0632</td>
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</table>
Examine the average inlet and exit velocities from Tables 4.1 and 4.2 more closely, we see both the L1M and L2F airfoils accelerate the flow through the cascade to nearly the same level over the range of $Re$ surveyed, where $u_{ex}$ is approximately equal to $1.6 \cdot u_{in}$. Later it will be noted that these accelerations occur at different levels in different regions which lead to different boundary layer physics between the two airfoils.

Table 4.3 presents measured wake conditions for the L1M and L2F airfoils including average percent exit turbulence level, wake width to the nearest 0.5cm, and wake depth defined as the maximum total pressure drop $(P_{t,in} - P_{t,ex})$ [in H$_2$O] in the wake. Remembering the inlet freestream turbulence is 3.3% for both airfoils, we see the effects of the L1M separation bubble in generating an increased level of unsteadiness as the bubble periodically convects downstream. As Reynolds number increases and the bubble size is reduced, the exit unsteadiness and wake width are reduced. The smaller level of exit unsteadiness and width seen in the L2F wake supports the idea of a very thin separation bubble which does not generate as much periodic unsteadiness as the L1M. Comparing the wake depths and widths, we see the L1M creates a wider and deeper wake than the L2F until somewhere after $Re = 6.4 \cdot 10^4$, where the L2F depth surpasses the L1M even though it remains thinner. Although this study proves unsteady effects are not necessary to design a well-behaved airfoil at aggressive loading levels, numerous higher-lift studies have touted the benefits of wake disturbance energy in re-energizing the separated boundary layer of downstream blade rows. Depending on the design point and heat load considerations of a given turbine engine, the larger L1M wakes may be more beneficial in a given situation especially at lower Reynolds numbers due to their increased turbulence levels.

Table 4.3: Wake conditions.

<table>
<thead>
<tr>
<th>$Re_{in}$</th>
<th>L1M $Tu_{ex}$</th>
<th>Width</th>
<th>Depth</th>
<th>L2F $Tu_{ex}$</th>
<th>Width</th>
<th>Depth</th>
</tr>
</thead>
<tbody>
<tr>
<td>$2.1 \cdot 10^4$</td>
<td>13.5</td>
<td>20.5</td>
<td>0.0125</td>
<td>5.0</td>
<td>13.0</td>
<td>0.0071</td>
</tr>
<tr>
<td>$3.4 \cdot 10^4$</td>
<td>8.8</td>
<td>20.0</td>
<td>0.0184</td>
<td>4.0</td>
<td>13.0</td>
<td>0.0144</td>
</tr>
<tr>
<td>$3.9 \cdot 10^4$</td>
<td>7.5</td>
<td>20.0</td>
<td>0.0221</td>
<td>3.8</td>
<td>13.0</td>
<td>0.0186</td>
</tr>
<tr>
<td>$4.3 \cdot 10^4$</td>
<td>6.8</td>
<td>19.5</td>
<td>0.0259</td>
<td>3.7</td>
<td>13.0</td>
<td>0.0235</td>
</tr>
<tr>
<td>$5.1 \cdot 10^4$</td>
<td>5.7</td>
<td>19.0</td>
<td>0.0416</td>
<td>3.6</td>
<td>13.0</td>
<td>0.0328</td>
</tr>
<tr>
<td>$5.8 \cdot 10^4$</td>
<td>4.4</td>
<td>19.0</td>
<td>0.0470</td>
<td>3.5</td>
<td>12.5</td>
<td>0.0402</td>
</tr>
<tr>
<td>$6.4 \cdot 10^4$</td>
<td>3.9</td>
<td>19.0</td>
<td>0.0502</td>
<td>3.5</td>
<td>12.0</td>
<td>0.0457</td>
</tr>
<tr>
<td>$7.5 \cdot 10^4$</td>
<td>3.6</td>
<td>18.5</td>
<td>0.0437</td>
<td>3.3</td>
<td>12.0</td>
<td>0.0674</td>
</tr>
<tr>
<td>$8.6 \cdot 10^4$</td>
<td>3.5</td>
<td>18.5</td>
<td>0.0518</td>
<td>3.3</td>
<td>11.5</td>
<td>0.0805</td>
</tr>
</tbody>
</table>
4.2 Boundary Layer Contours and Profiles

This section presents boundary layer contours and profiles obtained through thermal anemometry of $U_{\text{mean}}/U_{\text{in}}$, contours of $u_{\text{rms}}/U_{\text{in}}$, skew and kurtosis of the velocity signal, and contours and profiles of intermittency for the L1M and L2F airfoils at $Re$ equal to $2.5 \cdot 10^4$, $5.0 \cdot 10^4$, and $7.5 \cdot 10^4$. It should be noted here that two distinct behaviors were noticed in the previous work which developed the Praisner and Clark attached-flow and separated-flow transition models. While flows remained attached, transition onset occurred where the ratio of a boundary layer diffusion time to the turbulent eddy time scale reached a critical level. The attached-flow model requires the momentum thickness, an integral boundary layer quantity which loses its physical meaning once the flow is separated. Models based on physical quantities are preferred over those that are empirically based due to the extra information provided about the physics involved. Since the attached-flow model can only be applied up to the point of separation, an additional model was necessary for those flows that separate before transition occurs. Therefore, the separated-flow model is instead based on the momentum thickness Reynolds number just before incipient separation, where the momentum thickness remains physically meaningful. This section presents thermal anemometry results with a minimum surface-normal position of 1mm up from the surface of the airfoil. Since this minimal distance may preclude the resolution of a very thin separation bubble, the current data sets are examined with both models in mind, where both transition models will be constructed where available data permits.

Table 4.4 shows the maximum and minimum velocities throughout the measured data zones for the L1M and L2F airfoils for all $3 \ Re$. Previous research from the open literature [Reimann et al. 2006] employing single normal hot-wire sensors to determine separation zones have used a separation zone criteria defined as those regions having mean velocities less than $0.4 \times \text{max}(U_{\text{mean}})$. If there are no regions where the velocity is under this threshold, the flow may react as an attached flow. This table shows the L1M airfoil contains separated flow for $Re = 2.5 \cdot 10^4$ and $5.0 \cdot 10^4$. Since the minimum mean velocity for the L1M at $Re = 7.5 \cdot 10^4$ is greater than the threshold, the flow must either be attached or have a very thin separation bubble below the first measurement point just 1mm up from the surface. This is supported by the Reynolds lapse plot of Figure 4.1, where the L1M losses level off into the gradual decreasing slope somewhere between $Re = 5.1 \cdot 10^4$ and $5.8 \cdot 10^4$ as an attached flow would do. The L2F airfoil is seen without reversed flow (up to 1mm), again agreeing with the behavior reported in Figure 4.1 and associated low wake widths and turbulence levels of Table 4.3. The S3F measurements presented later in this chapter indicate the L2F blade does in fact separate; the bubble therefore must be thinner than 1mm or 0.7% of the axial chord. An interesting possibility now exists that the shear layer above a very thin separation bubble may transition as if attached and not separated. In order to clarify this response, both the attached-flow
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and separated-flow transition models will be constructed where data is available and compared to the model databases as presented in Figure 2.3(a) for the separated-flow model and in Figure 2.3(b) for the attached-flow transition model. As Table 4.4 gives a qualitative way to describe separation zones, the \(0.4 \times \text{max}(U_{\text{mean}})\) criteria is unable to provide separation onset locations which are upstream of the transition locations. Thus, the separated-flow transition model cannot be validated using this criteria for separation.

Table 4.4: \(\frac{U_{\text{mean}}}{U_{\text{in}}}\) maxima and minima.

<table>
<thead>
<tr>
<th>(Re_{\text{in}})</th>
<th>(\text{L1M}) max</th>
<th>(0.4 \times \text{max})</th>
<th>(\text{min})</th>
<th>(\text{L2F}) max</th>
<th>(0.4 \times \text{max})</th>
<th>(\text{min})</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.5 (\times) 10^4</td>
<td>4.2</td>
<td>1.7</td>
<td>0.8*</td>
<td>5.7</td>
<td>2.3</td>
<td>2.5</td>
</tr>
<tr>
<td>5.0 (\times) 10^4</td>
<td>9.5</td>
<td>3.8</td>
<td>3.0*</td>
<td>11.1</td>
<td>4.4</td>
<td>5.9</td>
</tr>
<tr>
<td>7.5 (\times) 10^4</td>
<td>13.9</td>
<td>5.6</td>
<td>7.3</td>
<td>17.6</td>
<td>7.0</td>
<td>8.8</td>
</tr>
</tbody>
</table>

* Separated condition defined by \(0.4 \times \text{max}(U_{\text{mean}})\).

Figure 4.2 presents L1M \(\frac{U_{\text{mean}}}{U_{\text{in}}}\) contours and profiles for all three \(Re\); the contour scaling is identical for all three \(Re\). Also note the differences in contour and profile heights, as the lower \(Re\) flows required a higher profile height to resolve boundary layer features. The blanked white areas of the contours represent the locations where data was not acquired as explained earlier with Figure 3.3. The influence of the passage acceleration can be seen by an increase in near-wall velocity from the first profile at 22.5%SSL up to the fifth profile at 32.7%SSL, after which the near-wall flow decelerates under the adverse pressure gradient. Noting that Wildcat predicted the point of minimum pressure at 47% axial chord (38.5%SSL), we see an earlier velocity peak in the experimental results than predicted. The accelerated profiles resemble those of a jet flow with higher velocities just up from the wall than in the freestream. The eventual deceleration squeezes the near-wall profiles into the familiar boundary layer shapes. By 47.5%SSL, the flow has decelerated enough to detach into a separated free shear layer. The L1M stalls at \(Re = 25k\), as the sharp change in mean velocity beginning near 47.5%SSL and \(y/B_x = 0.006\) grows progressively away from the wall in an unbounded fashion with downstream distance. The stall behavior can be seen more clearly in the profiles of Figure 4.2(a), where the velocities in the upper portion of the profile slow to minimal magnitudes less than 0.2 of the maximum velocity near the surface. In fact, this drastic change in the mean velocity continues to nearly 35% axial chord up, or a full 6.2cm up from the surface at the last profile acquired at 89.5%SSL. Under the shear layer a region of fluid with a momentum...
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Figure 4.2: L1M $U_{mean}/U_{in}$, contours and profiles.
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deficiency recirculates and causes a flow blockage extending far out into the main passage.

As expected, the passage acceleration increases with $Re$ as indicated by the increase in velocity in Figures 4.2(b) and 4.2(c) for $Re = 50k$ and $75k$, respectively. At $Re = 50k$, the near-wall velocity increases until 37.2%SSL where the adverse pressure gradient begins to slow the fluid, 4.5%SSL downstream of the $Re = 25k$ location. For $Re = 75k$, the near-wall fluid accelerates until 42.7%SSL, 10%SSL further downstream than for $Re = 25k$. The L1M then experiences its shear layer detachment near 60.1%SSL at $Re = 50k$, which gives way to a turbulent separation bubble. After this detachment, a mix of turbulent-like near-wall profiles continue to fade into more laminar-like shapes, suggesting the separation bubble remains below 1mm up from the surface. At $Re = 75k$, no such detachment is noticed after the maximum near-wall velocity as the profiles (and contour) appear turbulent and resemble an attached flow. The shape of the shear layer, as detected by the region of changing mean velocity with a knee-like curvature, is different for $Re = 75k$, also lending support to the idea of an attached flow versus a separated one.

Turbulent similarity comparisons for selected L1M mean velocity profiles at $Re = 50k$ are presented in Figure 4.3 plotted in wall coordinates with the log base 10 of $y^+$ on the abscissa. These comparisons were constructed by iterating upon the wall shear ($\tau_w$) until a best fit was obtained between the similarity profiles and current experimental data using the applicable range of $y^+$. The selected profiles presented in this figure illustrate the typical trends seen by all test cases for both airfoils except the stalled L1M at $Re = 25k$ which exhibits no turbulent similarity for any section of the surveyed suction surface. Significant differences include the first “good fit” location, the terminal shape of the data fit to Coles’ law of the wake (Equation 3.27), and the final suction surface location where the terminal shape occurs. A “good fit” location is where the data in the appropriate $y^+$ range is fit to Coles’ law with less error than to Spalding’s law; Coles’ law was chosen due to its accounting of the pressure gradient with the “PI” term. The terminal shape is considered as the earliest location which continues to closely resemble its followers as one progresses downstream. As expected, at no time do any of the profiles match the linear law which is only applicable extremely close to the wall (closer than 1mm). Comparison to the linear law in Figure 4.3(a) at 62.4%SSL for $\tau_w = 0.30Pa$ shows how the acquired data are located out of the viscous sublayer, an expected result since the closest wall data point is 1mm from the surface. Initially, and at different suction surface locations for each Reynolds number and airfoil, the data do not compare well with Spalding’s law or the log-law, with the lower $y^+$ data points residing above Spalding’s law and the higher $y^+$ points falling below, as illustrated in Figure ?? for the L1M $Re = 50k$ case at 62.4%SSL with $\tau_w = 0.30Pa$. Minimal to no agreement is found between data and the log-law or Spalding’s formula, which can be attributed to the transitional flow experiencing a pressure gradient, both of which were unaccounted
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Figure 4.3: L1M turbulent profile similarity comparisons, \( Re = 50k \).
for in the Spalding and log-law development. Instead, the profile begins stretching towards the 'S' shape described by Coles' law of the wake. By 66.9%SSL in Figure 4.3(b), the experimental data can be fit to Coles’ profile with reasonable success using $\tau_w = 0.22\text{Pa}$ and a PI term equal to 0.24. A further increase to 89.5%SSL in Figure 4.3(c) using $\tau_w = 0.07\text{Pa}$ and PI=1.52 shows the terminal shape for the L1M at $Re = 50k$, where the lower and higher $y^+$ locations still in the applicable range exhibit higher $u^+$ values. Also as expected, the wall shear required to fit the data decreases with suction surface distance, while the PI term is seen to increase with downstream distance. The terminal shapes for other test points vary between those seen in Figures 4.3(b) and 4.3(c), illustrating how no profiles acquired during this work completely resemble fully developed turbulent flow.

Figure 4.4: L1M turbulent profile similarity comparisons, $Re = 75k$.

Figure 4.4(a) shows the initial good fit for $Re = 75k$ occurs at 77.5%SSL using $\tau_w = 0.40\text{Pa}$ and PI=0.05. The terminal shape for the L1M is observed by 84.7%SSL in Figure 4.4(b) using $\tau_w = 0.30\text{Pa}$ and PI=0.35, where the higher $y^+$ locations never rise above Coles’ law. Comparing Figures 4.3 and 4.4 shows how the first occurrence of a good fit moves downstream with increasing Reynolds number. This trend is also typical for the L2F airfoil. The PI term increases with downstream distance but decreases with increasing Reynolds number, trends also observed with the L2F data.

Figure 4.5 presents L2F $U_{\text{mean}}/U_{\text{in}}$ contours and profiles for all three $Re$ where the contour scaling is the same as the L1M scaling. The topology for all three $Re$ remains similar, resembling that of the attached L1M $Re = 75k$ case with a knee-like curvature in Figure 4.2(c). The height for the L2F contours and profiles remains at 20mm (11.2% axial chord) since this was sufficient to resolve the boundary layer features. In contrast to the L1M, the bulk of the L2F passage acceleration has already occurred further upstream than the first profile, here at 19.9%SSL for the L2F. As a result, the profile point 1mm up from the surface experiences relatively constant deceleration throughout
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Figure 4.5: $U_{mean}/U_{in}$ contours and profiles.

(a) $Re = 25k$.
(b) $Re = 50k$.
(c) $Re = 75k$. 
the entire flowfield for all three \( Re \). This is due to the forward loading characteristic of the L2F, where the pressure minimum occurs further upstream and the remaining pressure increase to the trailing edge is spread out over a longer surface distance. Remembering Wildcat predicted the pressure minimum to occur at 27% axial chord (24%SSL), we again see an earlier velocity peak in the experimental results than predicted with Wildcat. This constant deceleration gradually retards the jet-like profiles of upstream locations into those more similar to zero pressure gradient boundary layer profiles of downstream locations. These downstream profiles also resemble turbulent attached flow profiles, which agree with the Reynolds lapse behavior of Figure 4.1.

![Figure 4.6: L2F comparison to Falkner-Skan; \( \beta = -0.199, Re = 25k \).](image)

Laminar comparisons against the Falkner-Skan family of similarity laws with \( \beta = -0.199 \) are presented in Figure 4.6 for L2F airfoil profiles which are upstream of separation at \( Re = 25k \). The maximum adverse pressure gradient before separation is defined by \( \beta = -0.199 \), and this figure shows how the near-wall region’s jet-like shape, where the velocity increases going towards the wall from the freestream, does not match the wedge flows represented by the Falkner-Skan solutions. This result is typical of all laminar profiles for all three Reynolds numbers upstream of separation where Falkner-Skan would be applicable. L2F turbulent similarity comparisons against Spalding’s and Coles’ laws are presented in Figure 4.7 for all three Reynolds numbers. For \( Re = 25k \) in Figure 4.7(a), the first good fit location occurs at 70.0%SSL using \( \tau_w = 0.08Pa \) and PI=0.22, and the terminal shape seen in Figure 4.7(b) at 90.4%SSL using \( \tau_w = 0.48Pa \) and PI=0.48 matches Coles’ law of the wake extremely well for \( y^+ > 50 \). Increasing the Reynolds number moves the initial good fit downstream to 82.0%SSL for both \( Re = 50k \) and \( 75k \), as seen in Figures 4.7(c) and 4.7(e) using \( \tau_w = 0.24Pa \) and PI=0.14 for \( Re = 50k \) and \( \tau_w = 0.47Pa \) and PI=0.04 for \( Re = 75k \). The
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Figure 4.7: L2F turbulent profile similarity comparisons.

terminal shapes are slightly different, however, as the $Re = 50k$ case remains below Coles’ law with increasing $y+$ while the $Re = 75k$ case first shifts above and then falls below Coles’ law. Again the
similar L1M trends are noticed where the PI terms increase with downstream distance and decrease with increasing Reynolds number, while the shear stress follows the opposite trends. In addition, the higher \( y^+ \) data points in the L2F profiles do not lie above Coles’ law as often as the higher \( y^+ \) points of the L1M. As only the near-wall 30 to 35 data points were used in these comparisons, it is quite possible that the L2F profiles deviate from Coles’ law in a similar manner to the L1M further out into the freestream. As with the L1M, the deviation from the similarity laws are believed to be caused by an incomplete transition to turbulence and varying pressure gradients.

Figure 4.8 presents contours of \( u' \) for all three \( Re \) for the L1M. The same heights over the various \( Re \) used for the mean velocity are presented in these figures as well in order to resolve the boundary flow features. Immediately we notice the severe degree of unsteadiness \( u' \) in the separated and stalled free shear layer for \( Re = 25k \) in Figure 4.13(a). The first sign of near-wall unsteadiness occurs at 42.7\%SSL. As expected from the mean velocity contours, the unsteadiness begins to sharply increase near the detachment location of 47.5\%SSL, spreading very rapidly in the downstream and cross-stream directions, reaching a peak of 47.1\% unsteadiness in the core near 62.4\%SSL and as low as 7.3\%Bx up from the surface. Again the unsteadiness continues unbounded with downstream distance, matching the rapid change seen in the mean velocity contours. Although this high level of unsteady flow convects downstream into the wake producing the average exit turbulence of 13.5\% as reported earlier in Table 4.3, it is still not enough turbulent mixing to force a reattachment of the separation. This means the adverse pressure gradient experienced by the mid-loaded L1M is too severe for its boundary layer to overcome without massive separation at low \( Re \). Increasing \( Re \) to 50k in Figure 4.13(c) greatly reduces the unsteadiness compared to \( Re = 25k \), where \( u' \) begins to grow further downstream at 52.6\%SSL now, with a peak value of only 34.1\% occurring near 66.9\%SSL at 1mm (0.6\%Bx) up from the surface. A further increase to \( Re = 75k \) in Figure 4.13(e) also sees the initial rise of unsteadiness at 52.6\%SSL and reduces the peak unsteadiness to just 20.7\% near 72.8\%SSL at 1mm up from the surface. The L1M \( Re = 75k \) case is considered attached (Table 4.4), while at lower \( Re \) the flow is considered separated, which corroborates with the loss behavior as noted in Figure 4.1. However, the maximum unsteadiness location for both the \( Re = 50k \) and \( Re = 75k \) cases are at the lowest profile point acquired nearest the wall, thus suggesting an attached-flow transition as opposed to transition atop a separation bubble. This contradiction supports the idea that separated-flow transition in the shear layer atop a very thin separation bubble may in fact behave like attached-flow transition.

Examining the skew and kurtosis of the L1M velocity signals in Figure 4.9 lend some insight into the transition process. The contour scales for all three Reynolds numbers are the same for skew and the same for kurtosis for ease of comparison. As explained earlier in Chapter 3, regions of both
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Figure 4.8: L1M urms/Uin contours.

(a) Re = 25k.

(b) Re = 50k.

(c) Re = 75k.
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Figure 4.9: L1M skew and kurtosis contours.
negative skew and positive kurtosis have been identified as transition indicators. This combination of skew and kurtosis first occurs near 47.5%SSL just 3.3%Bx up from the surface for the L1M at $Re = 25k$, suggesting the beginnings of a separated-flow transition away from the surface. Following the separated free shear layer downstream and away from the surface after 47.5%SSL, regions of alternating sign skew are tied with low magnitude kurtosis, suggesting a weakly intermittent transition process which never fully matures into turbulence, a trend also seen later with intermittency contours of Figure 4.12(a). Bons’ description of the alternating sign skew in regions of positive kurtosis indicates the merging of smaller momentum laminar packets with higher energy turbulent pockets [Reimann et al. 2006]. As Reynolds number is increased to 50k, levels of alternating sign skew and positive kurtosis are noticed at the near-wall profile location near 57.9%SSL. But in this case, the larger magnitude values of skew and kurtosis occur away from the wall, slightly downstream at 62.4%SSL and up 4.1%Bx from the surface, again suggesting a mixture between attached-flow and separated-flow transition. In order to construct the transition models for the next section, the transition location will be identified as the first streamwise location where the negative skew and positive kurtosis condition applies, in this case near the wall at 57.9%SSL. As $Re$ is further increased to 75k, the skew remains slightly negative but growing progressively positive downstream and away from the wall. The kurtosis, however, indicates a burst near 57.9%SSL, the same streamwise location of the $Re = 50k$ increase in kurtosis. The variation of kurtosis follows that of skew in reducing magnitude with downstream and surface-normal distance. The transition location indicated by skew and kurtosis for the L1M at $Re = 75k$ is therefore 57.9%SSL.

Figure 4.10 presents contours of $urms/Uin$ for all three Reynolds numbers for the L2F airfoil. The contours for all three quantities are scaled the same as the L1M for ease of comparison. Again, the boundary layer physics were resolved with the smaller profile height, so surface-normal scale must be kept into consideration for full appreciation of the differences between the L1M and the L2F. At first glance, the L2F aerodynamics significantly reduce the amount of unsteadiness when compared to the L1M airfoil. For $Re = 25k$ in Figure 4.14(a), the unsteadiness first begins to increase near 52.6%SSL, compared to 42.7%SSL of the stalled L1M at $Re = 25k$. Therefore, the stronger acceleration of the front-loaded L2F pushes downstream the initial growth of unsteadiness compared to the mid-loaded L1M. The maximum unsteadiness of 28.7% occurs in the near-wall at 65.3%SSL, suggesting an attached-flow transition even at the lower Reynolds number value of 25k. The intermittency contours presented later corroborate the appearance of near-wall transition for all three $Re$, again suggesting attached-flow instead of separated-flow transition. Initial growths of unsteadiness for $Re = 50k$ and $Re = 75k$ begin near 48.1%SSL and 44.8%SSL, respectively. As $Re$ is increased beyond 25k, the overall area of unsteadiness decreases significantly with $Re$ and maximum
Figure 4.10: L2F urms/Uin contours.

(a) $Re = 25k$. 

(b) $Re = 50k$. 

(c) $Re = 75k$. 

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Figure 4.11: L2F skew and kurtosis contours.
levels remain in the near-wall locations, 19.5% at 65.3%SSL for $Re = 50k$ and 17.4% at 57.2%SSL for $Re = 75k$. Thus we see an increase in $Re$ causes an earlier appearance of unsteadiness.

Examining the skew and kurtosis of the velocity signal in Figure 4.11, we can gain some insight into the transition process for the L2F airfoil. The contour scales for all three $Re$ are the same as the L1M for ease of comparison. For $Re = 25k$, the combination of negative skew and positive kurtosis occurs near 54.9%SSL not in the near-wall location but just above at 1.4% axial chord above the surface. This location even so slightly away from the wall suggests a separated-flow transition, even though the maximum unsteadiness suggests an attached-flow transition. This contradiction again lends support to the argument that very thin bubble separated-flow cases may transition as if attached. Again we notice regions of alternating sign skew progressing downstream and away from the surface with downstream distance, signaling the interplay between the laminar and turbulent pockets. Increasing $Re$ to 50$k$ sends the skew-kurtosis condition upstream to 48.1%SSL in the near-wall location, signaling an attached-flow transition. The same trend applies to $Re = 75k$, where the skew-kurtosis condition appears at 44.8%SSL, even though now the skew has reached minimal levels when compared to the other 5 cases. Both skew and kurtosis continue to occur away from the wall with downstream distance.

Figure 4.12 presents intermittency contours obtained through Clark’s algorithm for both airfoils at all three $Re$. The L2F regions of intermittency remain below the regions for the L1M for all three $Re$ tested, even though the L1M $Re = 75k$ case approaches the lower height of the L2F $Re = 75k$ case. For example, the maximum surface-normal height of the intermittency distributions for the L2F at $Re = 25k$ is nearly 10% axial chord, while the L1M reaches above 30% of its axial chord. This trend is expected since previous discussion has concluded the L2F Reynolds characteristic behaves more like attached-flow transition while the L1M may experience a low Reynolds regime of clearly separated-flow transition with its increased losses seen back in Figure 4.1. Classical methods of transition identification normally consider the first appearance of intermittency as the location of transition onset, and this trend will continue here. As this work considers two separate methods of transition onset identification (skew-kurtosis criterion and intermittency), both methods will be used later in their own constructions of transition parameters and compared against the models presented in Figure 2.3.

At $Re = 25k$ for the L1M, the first appearance of intermittency occurs at 52.6%SSL, just 9.9%SSL downstream of where unsteadiness ($urms/Uin$) first appears. Figure 4.12(a) shows this occurs away from the wall in the separated shear layer of the stalled L1M. Transition to turbulence never fully completes, and the intermittency eventually dies out by 89.5%SSL. The L2F in Figure 4.12(b) resembles an attached flow intermittency pattern with its core closer to the wall than the L1M at
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Figure 4.12: L1M and L2F intermittency contours.
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$Re = 25k$. Intermittency first increases near 54.9% SSL, just 2.3% SSL downstream of where $urms$ does the same. This distance from initial unsteadiness to the start of transition (intermittency) is shorter for the L2F than the L1M, suggesting the L2F forces a stronger bursting of turbulent energy when the flow finally begins to transition. Increasing $Re$ to 50$k$, the L1M intermittency now resembles that of an attached flow where the maximum intermittency follows the $urms$ location near the wall. Here intermittency first occurs near 60.1% SSL, 7.5% SSL downstream of the initial unsteadiness. Figure 4.12(c) shows the intermittency peaks near 66.9% SSL at 0.91 and then begins to drop with downstream distance. The turbulence spreads away from the wall, mixes with the freestream and then experiences some level of decay where the intensity decreases. Remembering the L1M loss knee in Figure 4.1 begins to level out around $Re = 50k$, we see the transition helps the L1M re-energize the boundary layer for higher $Re$, providing a reduced overall loss than the massively separated condition. The L2F at $Re = 50k$ in Figure 4.12(d) sees the first burst of intermittency near 48.1% SSL in the near-wall location, the same profile location where unsteadiness was first noticed. Again the L2F aerodynamics reduces the surface-normal extent of turbulence over the L1M, and again we notice an incomplete transition after the peak intermittency of 0.79 at 65.3% SSL. Since the L1M peak value is higher than the L2F peak at $Re = 50k$, the L1M experiences a slightly greater degree of turbulent bursting than the L2F, although the L2F transitions well before the L1M. Increasing $Re$ to 75$k$ in Figures 4.12(e) and 4.12(f) shows intermittency patterns which are classically thought of when transition to turbulence completes: a laminar flow begins to transition and eventually goes fully turbulent to remain turbulent thereafter. Intermittency increases near 60.1% SSL for the L1M, a distance of 7.5% SSL downstream of initial $urms$; the L2F again sees intermittency at the same location as $urms$ at 44.8% SSL. This trend again suggests the L2F transition mechanism is much more efficient at promoting transition than the L1M, where initial $urms$ takes longer to initiate transition (intermittency).

Another way of looking at the unsteady development for each airfoil is to examine the centroids of $urms/Uin$ and intermittency at each profile location and examine where the maximums occur compared to the centroid locations, which provide information such as where the core of unsteady activity is located at each profile. The centroids are constructed with the non-dimensional surface-normal distance as the y-axis and the non-dimensional parameter value as the x-axis. Using this methodology, the area of $urms/Uin$ and intermittency ($\gamma$) at each profile location can be thought of as the total activity at that location. The $xbar$ components represent the strength of the core defined by the centroid, while the $ybar$ components represent the non-dimensional surface-normal distance of the core. The $ymax$ tag identifies the non-dimensional surface-normal distance where the maximum value is located. Figures 4.13 and 4.14 present this data against percent suction surface
distance along with boundary layer and momentum thicknesses for the L1M and L2F airfoils. The y-axis of these graphs represents $y/Bx$ for the ybar, ymax, $\delta$, and $\theta$ plots, and is the total area value or xbar of the centroid for the area and xbar plots.

In Figure 4.13(a) for the L1M at $Re = 25k$, the urms/Uin area increases at 42.7%SSL where the first sign of unsteadiness appeared in Figure 4.8. A sharp jump is seen before the shear layer detachment at 47.5%SSL both in the urms/Uin area and now xbar, or magnitude of the core location. This location also marks where transition onset occurs using the skew-kurtosis condition. The strength of the core continues to rise until nearly 60%SSL, where it levels off through to the last profile at 89.5%SSL. This leveling off location corresponds roughly to where the width of the most turbulent portions of the urms/Uin profiles slow their streamwise growth. The area, or total activity, continues to grow unbounded just as expected from earlier urms contours. The ybar and ymax locations appear to remain congruent throughout the measured zone, indicating the unsteady activity is mostly centralized. Figure 4.13(b) shows the small burst in intermittency appears in the regions which grow just after where the urms begins, with a resulting increase in boundary layer height and momentum thickness. Near 39.9%SSL the maximum intermittency bursts away from the surface near $0.05 y/Bx$, vanishes after 42.7%SSL, and has another brief burst again at the same approximate height near 54%SSL. The onset of transition obtained with an intermittency level above 0.02 (above the threshold using Clark’s algorithm) occurs at 49.5%SSL for the L1M at $Re = 25k$. The maximum intermittency occurs below the core intermittency. Progressing downstream increases the boundary layer thicknesses with continuing unsteadiness and dying intermittency. The core and maximum urms occur under the boundary layer until 52.6%SSL, after which the core and maximum urms lie outside the boundary layer and into the freestream. Core intermittency lies inside the boundary layer over the entire length surveyed. It’s interesting to note that unsteadiness continues unbounded while intermittency levels off and then decreases. The maximum unsteady core activity occurs at the same location as the maximum intermittency core near 62.3%SSL, after which the total intermittency activity begins to decline. The core intermittency strength then declines at a much faster rate than the total activity (area), while the total unsteady activity (urms area) continues to grow unbounded. This may support the argument that there is no re-laminarization as intermittency falls off since the unsteadiness continues to grow.

Increasing $Re$ to 50k for the L1M in Figure 4.13(c) causes a small jump in total unsteady activity away from the wall around 0.1 $y/Bx$ and 30%SSL which dips at 37.2%SSL then resurges at 44.6%SSL. The total activity continues to increase gradually until the skew-kurtosis transition onset location of 57.9%SSL, where it grows until nearly 70%SSL. A small dip at profile locations 8, 9, and 10 can be seen and is a result of the limited profiles available in those locations; the data in
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Figure 4.13: L1M unsteady development.
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these areas is believed to remain consistent with surrounding points. The same trend is seen in the L1M case at $Re = 75k$, and with all L2F cases at profile locations 7 and 8. The core strength ($x_{bar}$) of urms activity follows the total activity, again suggesting a centralized distribution of unsteady activity which remains near the same height away from the surface all along the blade, as seen with the near-constant $y_{bar}$ location. This $y_{bar}$ location is outside of the boundary layer until 66.9%SSL, where it remains under the boundary for the duration of suction surface tested. The maximum unsteadiness remains below the core location all along the surface. Figure 4.13(d) shows transition onset by intermittency occurs at 60.1%SSL where the total activity far surpasses that at $Re = 25k$, while the core intermittent activity strengthens and then decreases after 70.0%SSL. This core intermittency (greater than 0.02) remains below the boundary layer at all times. This decrease in core strength is accompanied by an increase in total strength, suggesting the turbulence spreads more rapidly than it weakens, covering a greater physical area with downstream distance at the expense of core strength. Again the boundary layer thicknesses begin to grow near the point where intermittency increases instead of where unsteadiness or the skew-kurtosis condition occurs.

As $Re$ is increased to 75$k$ in Figures 4.13(e) and 4.13(f) for the L1M, another jump in total and core urms and intermittency is seen over $Re = 25k$ and 50$k$ as expected due to the higher freestream velocity. The gradual buildup in total urms activity begins near 54.6%SSL and continues to the last profile at 89.5%SSL. The core of urms activity continues growing more rapidly after the skew-kurtosis transition onset at 57.9%SSL, while core intermittent activity is seen to rise after the intermittent transition location of 60.1%SSL up to its maximum strength at 70.4%SSL. Again the trend where the total intermittency grows while the core strength declines suggests a spreading of intermittent influence while trading off strength. The maximum unsteady and intermittent behavior continues to lie inside the core locations as evidenced by comparison of the $y_{bar}$ to $y_{max}$ plots. The maximum urms and maximum and core intermittency remain below the boundary layer, while the core urms is generated in the freestream until somewhere between 72.8 and 75.1%SSL where it shifts under the boundary layer. The boundary layer thicknesses again grow with intermittency instead of with unsteadiness. This trend suggests the Clark intermittency algorithm picks up on the activity that is somehow linked to growth in boundary layer thicknesses. This makes physical sense as well, since intermittency is associated with turbulent eddies which by their nature expand and mix the local fluid, resulting in the increase in boundary layer height. The original purpose of the algorithm was to track turbulent spots, which have been observed to cause increases in displacement and momentum thicknesses [Clark et al. 1994]. It is encouraging that the current data sets and the previous development of the algorithm show increases in boundary layer thicknesses both on and away from the wall as intermittency increases.
4.2 BOUNDARY LAYER CONTOURS AND PROFILES

Figure 4.14: L2F unsteady development.

(a) \( u_{rms}, Re = 25k \).

(b) \( \gamma, Re = 25k \).

(c) \( u_{rms}, Re = 50k \).

(d) \( \gamma, Re = 50k \).

(e) \( u_{rms}, Re = 75k \).

(f) \( \gamma, Re = 75k \).
The centroids of urms and intermittency for the L2F airfoil are presented in Figure 4.14 for all three Reynolds numbers. At \( Re = 25k \) in Figures 4.14(a) and 4.14(b), we immediately notice a lower range of parameter values when compared to any of the L1M cases. Compared to the L1M at \( Re = 25k \), the L2F has a reduced total urms activity which begins to rise near 52.6%SSL, just before the skew-kurtosis transition at 54.9%SSL. The core strength also begins to rise near 52.6%SSL and continues to increase while the total activity levels off after its peak at 70.0%SSL. This trend suggests the L2F unsteady activity becomes more compact after 70.0%SSL since the total activity remains constant while the core strength increases. The maximum unsteadiness remains under the boundary layer for most of the blade, while the core urms remains outside the boundary layer until 65.3%SSL where it shifts underneath. Again we see the rise of the boundary layer thicknesses with intermittency near 54.9%SSL, while soon after the core of intermittent activity falls significantly lower to remain under the boundary layer. Just as with the L1M, the L2F core intermittency falls under the boundary layer just as it begins to grow. It’s interesting to note the L2F urms activity continues to rise while the intermittency total activity peaks near 72.4%SSL and then begins to decline as the core shifts under the boundary layer. This intermittency core however peaks slightly earlier than the total activity around 65.3%SSL, suggesting an expansion of influence at the expense of core strength. After that, both core and total strength decline. The maximum urms and intermittency locations remain relatively coincident under the boundary layer at all times.

As Reynolds number is increased to 50k in Figures 4.14(c) and 4.14(d), there is an increase in core and total urms activity over \( Re = 25k \), which is again expected since the faster freestream supplies more energy to the unsteadiness. Again we see a rise in core urms activity at the skew-kurtosis onset location of 48.1%SSL, which is immediately followed by a rise in core intermittent activity at the intermittency onset location of 50.3%SSL. Unlike \( Re = 25k \) however, this time the core and total unsteadiness continue to rise to the last profile at 90.4%SSL, suggesting a continual growth in unsteady energy. This is not true for intermittency, as its core gains strength until 63.0%SSL and then slightly decreases, while the total intermittent activity seems to grow until the last profile location. Again this indicates a spreading influence of turbulence as it loses strength for coverage. The maximum and core intermittency remains under the boundary layer, while the core unsteadiness remains outside until 72.8%SSL where it falls under the boundary layer.

A further increase to \( Re = 75k \) in Figures 4.14(e) and 4.14(f) again shows the expected increase in total and core urms activity with the increased levels over the \( Re = 25k \) and 50k cases. Here the initial unsteady growth is seen at 44.8%SSL, the same location as the intermittency rise and transition onset location defined by both skew-kurtosis and intermittency. Here again the total and core urms activity continue to increase through the measured region. The intermittency core
strength levels off after 63.0%SSL while the total activity remains growing until the end. This again suggests a spreading of turbulent influence as core strength remains constant, a trend also seen back in the contour of Figure 4.12(f). The core and maximum intermittency remains below the boundary layer again, while the core unsteadiness falls under the boundary layer only after 79.6%SSL.

As expected from these plots, we notice the boundary layer height decreases in size with increasing $Re$ for both airfoils. The total urms and intermittency activities (areas) also increase with $Re$ due to the faster fluid pumping more energy into the unsteady processes. The L2F levels remain below the L1M however, even though transition initiates earlier. We also see that the core of urms is closely tied to the skew-kurtosis onset condition. This makes physical sense, because the skew and kurtosis are measures of the quality of the velocity signal, i.e. they measure the departure from the unfluctuated state. So as variation is introduced in velocity due to a transition or unsteady process, the core urms also reflects this change. We also see that the core urms remains outside of the boundary layer for longer distances before falling under as $Re$ is increased; these occurrences are at 52.6, 66.9, and 72.8%SSL for the L1M and 65.3, 72.8, and 79.6%SSL for the L2F. This is because the faster freestream initiates unsteadiness earlier while simultaneously forcing the lower boundary layer height. Thus we see a more forward-loaded blade maintains its core unsteady activity outside of the boundary layer further downstream than the mid-loaded airfoil, even though the total unsteadiness is not greater in the wake (Table 4.3). This difference may help explain why an earlier transition with a lower amount of total unsteadiness can still maintain a well-behaved flowfield even at lower Reynolds numbers. The maximum urms remains under the core urms at all times, signalling that unsteadiness is connected to the presence of the wall boundary, either generated or at least amplified in the near wall region. The core intermittency decreases its strength ($xbar$) faster than the intermittent total activity (area), signalling that the turbulent activity spreads away from the wall at the expense of core strength. The boundary layer growth is connected with the rise in intermittency and not the rise in urms. Therefore the mixing caused by the turbulent eddies is responsible for the boundary layer growth instead of general flow unsteadiness (urms). It is also interesting to note that unsteady activity can increase while turbulent unsteadiness decreases, showing unsteadiness and intermittency are not direct functions of each other.

4.3 S3F Measurements

As mentioned earlier, thermal anemometry requires the positioning of a probe throughout the flowfield in order to acquire information. This technique is limited near surfaces, since the thin sensor element cannot touch any object or it will immediately be destroyed. The anemometry results pre-
sent in the last section illustrate how thin the separation bubbles in this study truly are - less than 1mm in the surface-normal direction. As such, thermal anemometry was unable to identify the separation dynamics needed to validate the separated-flow transition model. S3F can provide just this, as it measures the forces directly on the surface instead of the flowfield above. The S3F tests conducted at AFRL were completed in two rounds; the first placed separate patches of S3F for the leading and trailing edges, and the second round placed one continuous patch from the trailing edge to nearly 42%SSL. Round 1 results were intended to check the formulations of the S3F sensor and make sure the stiffness was properly selected for the local stress levels. The separation onset locations obtained in round 1 for the leading edge are used in the next section to validate the separated-flow transition model. Round 2 examines the trailing edge region with another S3F patch, and these results were to be compared with the round 1 trailing edge data to gain more insight into the data reduction process, thus helping ISSI obtain a better feel for the film’s performance over two test points with the same flow conditions. We'll see later that obtaining results with S3F in these low flow speeds is still a challenging task, as round 1 produces different magnitude friction forces on the trailing edge than round 2. In fact, the friction patterns are also different.

Figure 4.15 presents round 1 S3F results for the leading edge region of the L2F blade. These results cover approximately 5 to 35%SSL. Figures 4.15(a), 4.15(b), and 4.15(c) show the displacement maps with magnitude as the colorbar, and are oriented with the flow coming from the right and going to the left. These figures show the L2F blade indeed separates, as the nodal line of separation can be seen for all three Re where the downstream (left) facing vectors converge with the upstream (right) facing vectors. The Re = 25k case shows a separation line which is a function of airfoil span (y-direction), indicating an unsteady separation onset location. The other two Re maps display a separation line more consistent with span, and is located slightly further downstream than Re = 25k. Figure 4.15(c) also indicates a small separation closer to the leading edge, as seen with the nodal line of reattachment where the vectors diverge away from the line. During the course of the S3F round 2 tests, we found the blades slightly twist under the wind-on loaded state, and this may explain why the nodal line of reattachment seen in the bottom portion does not exist in the upper portion. This twisting may in fact cause the slight separation seen in this figure. Figure 4.15(d) presents the information which will be used in validating the separated-flow transition model. This figure presents the streamwise (x-dir) and transverse (y-dir) surface shear results averaged over the span for the maps presented in Figures 4.15(a), 4.15(b), and 4.15(c). Here the flow is coming from the left going to the right, so positive friction is directed downstream and negative friction is directed upstream, indicating the reverse flow inside of a separation bubble. The small magnitude transverse friction (y-dir) indicates a mostly two-dimensional flow for all three Reynolds numbers with slight
4.3. S3F MEASUREMENTS

(a) $Re = 25k$.

(b) $Re = 25k$.

(c) $Re = 25k$.

(d) Composite.

Figure 4.15: S3F round 1: Force distributions near L2F leading edge, 5-35%SSL.

variation possibly due to the slight twisting of the blade under the applied load. The streamwise surface shear (x-dir) for $Re = 25k$ indicates a separation at approximately 26%SSL where the friction changes sign from positive (downstream) to negative (upstream). This location will be used later to validate the separated-flow transition model, where the model information will be taken from the nearest boundary profile at 25.2%SSL. Increasing $Re$ moves this separation location slightly further downstream, where the $Re = 50k$ case is just upstream of the 75k case. Model validation for these two latter cases will require boundary information from their nearest profile, both selected as...
28.0%SSL. Also as expected in this figure we see that friction is proportional to and increases with $Re$. Table 4.5 shows the experimental separation onset locations along with Wildcat predictions in percent suction surface distance. All predicted onset locations occur further downstream than those found by experiment, although the proximity of the two methods is encouraging.

<table>
<thead>
<tr>
<th>Method</th>
<th>$Re = 25k$</th>
<th>$Re = 50k$</th>
<th>$Re = 75k$</th>
</tr>
</thead>
<tbody>
<tr>
<td>S3F</td>
<td>25.2</td>
<td>28.0</td>
<td>28.0</td>
</tr>
<tr>
<td>Wildcat</td>
<td>27.5</td>
<td>29.0</td>
<td>29.0</td>
</tr>
</tbody>
</table>

Figure 4.16 presents the round 1 streamwise S3F results for all three $Re$ from 67%SSL to the trailing edge of the L2F airfoil. Again the flow is left to right, so a positive shear stress indicates downstream flow while a negative stress indicates upstream flow (separation). This figure shows the friction forces experienced in this flow at the trailing edge are less than 1 Pascal. We also see that
the $Re = 25k$ and $50k$ cases are separated by the beginning of the patch, and both reattach near 92-94%SSL. It is not clear whether or not the separation noticed here is continued from the leading edge or if it caused by a disturbance of the film’s upstream edge. The $Re = 75k$ case appears attached as the patch begins, signalling that the separation near 28%SSL must have reattached somewhere before 67%SSL. The flow again briefly separates near 77%SSL and then quickly reattaches before the end of the blade near 92%SSL. This flow description will be contradicted by the round 2 results, again showing the difficulty experienced when resolving friction forces on the order of 1 or less Pascals. Again we must remember we are trying to not only resolve such low differences in friction which is a great achievement in its own right, but we are also simultaneously cancelling model and tunnel movement.

Figure 4.17 presents the second round of S3F results for the L2F airfoil using the single continuous patch on the latter portion of the airfoil. In this figure, two data sets have been combined into one global picture, each data set separated by a small distance in the middle of the plot. Breaking up the data set into two areas was necessary to maintain proper camera resolutions and focusing over the large patch covering a substantial length of the trailing edge region. This figure shows time-averaged friction force distributions for the L2F airfoil over Reynolds numbers from $30k$ to $75k$ covering 41.8 to 100%SSL. Again the flow is from left to right, so positive shear indicates downstream flow and
negative shear indicates reverse flow in a separation bubble. During this round of S3F tests, it was initially found that the tunnel vibration induced inaccuracies in the data reduction due to excess pixel movement. The sensitivity of S3F dictates that even a few pixels of model or tunnel movement registered by the CCD camera must be cancelled out in order to provide accurate results. In addition, it was further found that the blades twist under the applied wind load, which further complicates the reduction routine. In order to alleviate these problems, the camera and LED apparatus were bolted to the tunnel and a strip of marker dots were applied to the blade surface under the S3F patch, providing a means to cancel model movement during data reduction.

It should again be noted these round 2 results show a different flow pattern than the earlier round 1 results. This discrepancy illustrates the reduction process still needs more development before accurate skin friction can be obtained at the flow speeds encountered in this work. Looking at the trailing data set (right side), an obvious difference between round 1 and 2 results is how the L2F airfoil remains separated through to the trailing edge, since the friction forces at the trailing edge (right side) remain negative, indicating an upstream-directed friction force on the surface. In fact, the flow must remain separated at the trailing edge more often than not or the time-averaged friction would be positive (facing downstream). This is in direct contradiction to round 1 S3F and to Wildcat’s CFD predictions. For \( Re \) equal to 30\( k \), 35\( k \), and 45\( k \), the L2F experiences lower friction levels on the order of 0 - 1 Pascals; again, these levels do not agree with round 1 results. The \( Re = 45k \) case also sees higher amplitude frequency content compared to 30\( k \) and 35\( k \), seen by the waviness of the friction profile. There is also an abrupt reattachment that almost immediately returns separated near 72\%SSL, as seen by the positive hump in friction. Friction levels again increase for \( Re \) equal to 50\( k \), 60\( k \), and 65\( k \), averaging nearly a 1.5 Pascal difference between the loaded (wind-on) and unloaded (wind-off) states. This could be due to an earlier transition with increased Reynolds number causing a more turbulent flow to inflict a heavier degree of friction on the surface. Increasing to \( Re = 70k \) increases the overall friction as expected, but we see some discrepancy for the \( Re = 75k \) case where the negative friction force increases its magnitude as you progress upstream. This result seems non-physical since the friction due to an increase in turbulent activity should increase over lower Reynolds numbers, and maintain a gradual decrease with distance to the trailing edge of the airfoil. The pattern seen in Figure 4.17 shows a different story where the surface shear under the separation bubble decreases magnitude much differently than the other flow speeds.

The left side of Figure 4.17 shows the upstream portion of the S3F patch along 41.8 to 67.9\%SSL, where separation onset locations can be seen as the shear stress goes from positive (facing downstream) to negative (facing upstream). For comparison reasons, the separation onset locations for
4.4 Transition Model Validation

This section collects data presented in earlier sections in order to test the validity of the separated-flow and attached-flow transition models. Wildcat predictions of transition model quantities are also included for comparison where appropriate. It was initially intended that this study would validate the separated-flow transition model used during the design process of the L2F airfoil. Wake traverse and thermal anemometry results presented earlier suggest the L2F flow behaves more like attached flows instead of separated flows, but the S3F friction measurements (and Wildcat predictions) clearly indicate a separated flow. As such, both situations will be formulated with available data and compared to model equations. Wildcat predictions showed separation bubbles which should have been resolved by examining the flowfield 1mm up from the surface of the blade (as close as this author was comfortable going next to the surface with a boundary layer traverse which was manually positioned). However, during the course of this work, these separation bubbles are found to be much thinner than expected, and as such the behavior of the airfoils resembled that of attached flow and attached-flow transition. In addition, the separated-flow transition model requires knowledge of the separation onset location, and thermal anemometry using the $0.4 \times \text{max}(U_{\text{mean}})$ criterion to judge separation locations as used by other published work when employing single normal hot-film sensors (which provide only magnitude, not direction) to resolve the boundary layer was insufficient to provide the required information. This criterion proved unsuccessful in identifying an onset location more upstream of the transition locations for the L1M $Re = 25k$ and $50k$ tests. Therefore, transition locations identified by the skew-kurtosis condition and intermittency, along with separation onset locations obtained through the S3F technology will be used for comparison to the model equations.
### 4.4. TRANSITION MODEL VALIDATION

Table 4.6: Attached-flow transition model experimental conditions$^\ast$.

<table>
<thead>
<tr>
<th>Variable</th>
<th>Original Range</th>
<th>L1M</th>
<th>L2F</th>
</tr>
</thead>
<tbody>
<tr>
<td>$Re_\theta$</td>
<td>73 - 856</td>
<td>171/217</td>
<td>87/89</td>
</tr>
<tr>
<td>$K \cdot 10^6$</td>
<td>-1.9 - 4.8</td>
<td>-2.8/-1.6</td>
<td>-3.2/-4.0</td>
</tr>
<tr>
<td>$K \cdot Re_{\theta}^2$</td>
<td>-0.15 - 0.06</td>
<td>-0.08/-0.08</td>
<td>-0.02/-0.03</td>
</tr>
<tr>
<td>$M$</td>
<td>0.05 - 1.24</td>
<td>&lt; 0.2</td>
<td>&lt; 0.2</td>
</tr>
<tr>
<td>$T_g/T_w$</td>
<td>1.0 - 1.41</td>
<td>1.0</td>
<td>1.0</td>
</tr>
<tr>
<td>$T_u[%]$</td>
<td>0.11 - 5.09</td>
<td>3.3/7.0</td>
<td>3.9/7.4</td>
</tr>
<tr>
<td>$\lambda/\theta$</td>
<td>4.26 - 66.2</td>
<td>117/59.2</td>
<td>26.3/33.6</td>
</tr>
<tr>
<td>$Pr$</td>
<td>0.71 - 0.71</td>
<td>0.71</td>
<td>0.71</td>
</tr>
</tbody>
</table>

$^\ast$ Some boxes contain two values separated by ‘/’, the first using the skew-kurtosis condition to identify transition onset, the second using intermittency.

$^\dagger$ Stalled condition.

Tables 4.6 and 4.7 present the range of experimental conditions over which the attached-flow and separated-flow transition models were originally formulated, as well as the experimental conditions of the current study. The attached-flow conditions are taken where transition onset occurs, and include the momentum thickness Reynolds number, the $K$ acceleration parameter (Equation 3.17), the Pohlhausen pressure gradient parameter defined as $K \cdot Re_{\theta}^2$, the local Mach number $M$, the gas to wall temperature ratio $T_g/T_w$, the ratio of integral length scale to momentum thickness $\lambda/\theta$, and the Prandtl number, $Pr$. The separated-flow conditions are taken at the location of separation onset, and include the momentum thickness Reynolds number $Re_{\theta-sep}$, the ratio of the length from separation to transition divided by the suction surface distance to separation onset $L/S_{separation}$, $M$, $T_g/T_w$, $Tu$, $Pr$, the ratio of turbulent eddy integral length scale to momentum thickness $(\lambda/\theta)_{separation}$, and $K \cdot Re_{\theta}^2$.

Except for the stalled L1M at $Re = 25k$, all tests exhibit boundary layer characteristics similar to those used during the attached-flow model development, as seen by the ranges of $Re_\theta$ and $\lambda/\theta$ in Table 4.6. The levels of acceleration are a different matter, however, as all cases except the $Re = 25k$ L2F using the skew-kurtosis condition for transition onset exceed the acceleration or deceleration used for initial model development. The degree of deceleration far exceeds initial levels, as these airfoils experience a greater total adverse pressure change on the suction surface which must be overcome. Thwaites’ separation criterion [White 2006] says any flow experiencing a deceleration
greater (more negative) than -0.09 based on $K \cdot Re^2_\theta$ will undergo separated flow; the decelerations experienced by these airfoils at transition onset are approaching but remain less than this value, so separation can not be guaranteed solely on the basis of deceleration. This is a direct result of the spreading of the adverse pressure gradient over the L1M and L2F airfoils. The low loss levels seen in Figure 4.1 for these airfoils at the transition onset decelerations seen in this table are a good indication of the effectiveness of the current design philosophy (move loading and transition upstream) and modeling capability to extend the design space for higher-work turbomachinery blade designs. The levels of local boundary edge turbulence also extend beyond the initial range, which would be a welcome addition considering the higher levels of localized turbulence seen in a real gas turbine engine.

Table 4.7: Separated-flow transition model experimental conditions$^*$.  

<table>
<thead>
<tr>
<th>Variable</th>
<th>Original Range</th>
<th>$Re = 25k$</th>
<th>$Re = 50k$</th>
<th>$Re = 75k$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$Re_{\theta -sep}$*</td>
<td>29 - 360</td>
<td>54.6 (86.3)</td>
<td>107.2 (130.1)</td>
<td>140.0 (161.3)</td>
</tr>
<tr>
<td>$L/S_{separation}$†</td>
<td>0.1 - 3.1</td>
<td>1.2/1.3 (0.8)</td>
<td>0.7/0.8 (0.5)</td>
<td>0.6 (0.4)</td>
</tr>
<tr>
<td>$M$</td>
<td>0.1 - 0.8</td>
<td>&lt; 0.2</td>
<td>&lt; 0.2</td>
<td>&lt; 0.2</td>
</tr>
<tr>
<td>$T_g/T_w$</td>
<td>1.0 - 1.1</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
</tr>
<tr>
<td>$Tu[%]$</td>
<td>0.14 - 2.9</td>
<td>1.8</td>
<td>1.6</td>
<td>1.7</td>
</tr>
<tr>
<td>$Pr$</td>
<td>0.71</td>
<td>0.71</td>
<td>0.71</td>
<td>0.71</td>
</tr>
<tr>
<td>$(\lambda/\theta)$_{separation}</td>
<td>13 - 540</td>
<td>333</td>
<td>565</td>
<td>688</td>
</tr>
<tr>
<td>$K \cdot Re^2_\theta$</td>
<td>-0.98 - -0.03</td>
<td>-0.004</td>
<td>-0.004</td>
<td>-0.014</td>
</tr>
</tbody>
</table>

$^*$ Some boxes contain two values separated by ‘/’, the first using the skew-kurtosis condition to identify transition onset, the second using intermittency.

† Wildcat predictions for $Re_{\theta -sep}$ and $L/S_{separation}$ are included in parentheses.

Table 4.7 shows the experimental conditions for the L2F are mostly within those used for the separated-flow transition model development; since the S3F sensor was only applied to the L2F airfoil, no separation onset location was identified for the L1M. The first variant is the ratio of integral length scale to momentum thickness for $Re = 50k$ and $75k$, which exceeds the upper bound used for model development. This implies one of three conditions: either the eddy lengths are larger than found in development, the momentum thicknesses are smaller due to a more energy-preserved
boundary layer, or a combination of the two. The second variant is the range of the Pohlhausen pressure gradient parameter \( (K \cdot Re^2) \), where all three Reynolds numbers experience less deceleration than cases used for model development. This is due to the forward loading characteristic of the L2F, where a higher total pressure change from the leading edge to the trailing edge was spread over more suction surface distance, allowing the smaller pressure gradient at separation onset. This table also shows Wildcat predictions of momentum thickness remain above the experimentally determined values. This means Wildcat predicts more lost momentum by separation onset than found by experiment, a trend opposite than expected since the predictions do not account for the effects of freestream unsteadiness on pre-transitional boundary layers. Remembering the separated-flow model (Equation 2.7), this greater momentum thickness causes a shorter length from separation to transition onset, a trend also observed in Table 4.7. Overall, the current tests extend the validation of the separated-flow transition model for airfoils with higher loading levels at similar conditions with less deceleration at separation onset compared to those test cases used for model development.

Table 4.8: Transition onset locations [%SSL].

<table>
<thead>
<tr>
<th></th>
<th>L1M</th>
<th>L2F</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>( Re = 25k )</td>
<td>( Re = 50k )</td>
</tr>
<tr>
<td>( s/k )</td>
<td>47.5(^\dagger)</td>
<td>57.9</td>
</tr>
<tr>
<td>( \gamma )</td>
<td>49.5(^\dagger)</td>
<td>60.1</td>
</tr>
<tr>
<td>Wildcat</td>
<td>70.4</td>
<td>61.2</td>
</tr>
</tbody>
</table>

\(^\dagger\) Stalled condition.

Experimental transition locations have been defined by two distinct methods in this work: the skew-kurtosis condition and with intermittency. These two experimental locations along with Wildcat predictions are presented in Table 4.8 in percent suction surface for the L1M and L2F airfoils at all three \( Re \). This table shows that negative skew with positive kurtosis occurs ahead of intermittency or at best remains coincident; model comparisons will be presented for both locations. The skew-kurtosis condition may reflect the behavior of laminar boundary layers before the onset of transition, which was accounted for in the “quasi-laminar” modeling used in model development. For the L1M, transition occurs at the same location for the un-stalled cases (at least within 2.5%SSL, the approximate distance between each profile) using both identification methods. The L2F airfoil transitions just after the mid-surface distance at 54.9 or 57.2%SSL for \( Re = 25k \). As expected, the onset location moves upstream with Reynolds number, and the L2F transitions ahead of the L1M. Again, this is due to the more forward loading of the L2F. For the L1M, Wildcat using the
4.4. TRANSITION MODEL VALIDATION

The separated-flow transition model predicts transition onset relatively close to the experimental locations, except for the unpredicted stall at $Re = 25k$. Wildcat predicts earlier transition locations for the L2F, probably due to the over-prediction of the momentum thickness at separation onset used to construct the transition model, which leads to the shorter lengths between separation and transition onset.

Figure 4.18 presents both transition model equations plotted along with the experimental comparisons constructed with available data. Figure 4.18(a) shows the separated-flow model (Equation 2.7) and current data points for the L2F airfoil where separation onset information was available from the S3F. This figure shows both model constructions using round 1 S3F data on the leading edge region of the L2F airfoil along with round 2 data. Here we notice the model equation only agrees with the separation onset locations obtained through the round 1 S3F data, and we see the L2F matches the curve nearly exactly for $Re = 25k$, suggesting an accurate prediction of the length between separation and transition onset atop the bubble. The model is seen to under-predict this length for both $Re = 50k$ and $75k$ using both the skew-kurtosis and intermittency conditions. The front-loaded L2F sees the brunt of its acceleration before the first profile at 19.9%SSL, so the flow speed is near its highest when it separates around 28%SSL. This could explain why the $Re = 50k$ and $75k$ cases see a longer length from separation to transition than predicted with the model. Using the round 2 S3F data on the trailing edge region causes a severe over-prediction by the model for both $Re = 25k$ and $75k$. It is therefore suspected the onset locations obtained through the S3F round 2 data may in fact be artifacts of an upstream S3F patch disturbance; if this is the case, the true separation onset locations would be further upstream than those cited earlier, resulting in an increased length than plotted in the figure, which would in turn improve the agreement between the model and the experimental results. Thus, the round 1 S3F data along with boundary anemometry results provide more than satisfactory agreement with the separated-flow transition model, validating its use for turbomachinery design purposes.

Several observations presented earlier in this chapter have suggested airfoils with very thin separation bubbles may transition as if attached. In order to clarify this response, all non-stalled cases have been plotted along with the attached-flow model in Figure 4.18(b). This figure shows very close agreement between the experimental data using the skew-kurtosis condition for transition onset, and the intermittency cases are not far off either. This trend suggests very thin separation bubbles may transition as if attached, since the proper ratios of flow quantities necessary for attached-flow transition are not far off from those in the database used for model construction. Examining Figure 4.18(b) more closely, we see certain flow cases and transition onset criteria produce closer agreement with the model curve. For example, the L2F at $Re = 25k$ and $50k$ match the curve closer using the
4.4. TRANSITION MODEL VALIDATION

Figure 4.18: Transition model experimental validation.
skew-kurtosis condition, and at 75$k$ the two onset criteria matched. The L1M data using the skew-
kurtosis condition are also closer to the model than using the first sign of intermittency as the
transition onset location. If one imagines removing the data using the intermittency onset criteria,
then all experimental data using the skew-kurtosis method fit the curve very well. This trend also
suggests separated flows with very thin bubbles may transition as attached flows.

Slight manipulation of Equation 2.8 leads to the following relationship from the attached-flow
model which holds at the onset of transition [Praisner and Clark 2007]:

$$100 \frac{u' \theta^2}{X \nu} = 7.0 \pm 1.1$$

The relation on the left-hand side is found to equal $7.0 \pm 1.1$ for all cases in the database used
for model construction. The left-hand side is plotted for all experimental test cases in Figure 4.18(c)
along with the original model database and confidence interval. The only test case which fits inside
the confidence interval is the L2F at $Re = 25k$ using the skew-kurtosis transition onset. The S3F
measurements presented earlier show the L2F is in fact separated at $Re = 25k$, which lends support
to the idea that very thin separation bubbles may transition in the shear layer similar to attached-
flow transition. To the contrary, all other test cases lie outside of the confidence intervals. Since
separation bubbles decrease their height with increasing Reynolds number, the $Re = 50k$ and 75$k$
L2F cases should have a separation bubble which is thinner than the $Re = 25k$ case. Furthermore,
if separated cases can transition as if attached, the thinner separated cases should behave more like
attached-flow transition unless bubble thickness is not the only factor which influences the change
between separated-flow and attached-flow transition behavior. However, the $Re = 75k$ tests and
those using intermittency for transition onset are clearly not within the confidence intervals for
the Praisner-Clark number. This tells us there are still significant differences between flows that
transition atop separation bubbles and those that remain attached.

4.5 Chapter Summary

Chapter 4 presents the detailed experimental results obtained in the low-speed wind tunnel on base,
and it serves to provide an extensive database which can be used for CFD validation and further
model development. This chapter begins with the Reynolds lapse performance curves of the Pack
B, L1M, and L2F airfoils. Here it is seen the L2F outperforms both the L1M and Pack B in the low
Reynolds regime. Unlike the Pack B and L1M airfoils, the L2F remains stall-free in the low Reynolds
number regime, and the loss behavior resembles more of an attached flow rather than separated as
it gradually increases with decreasing $Re$. Also surprising was the reduced losses at higher Reynolds
number, as it was initially suspected that the earlier transition may cause increased losses due to
an increased turbulent-wetted surface area. Thus we have a higher-lift airfoil with an improved Reynolds lapse characteristic. The L2F also enjoys smaller wakes with decreased turbulence levels which lend support to the idea of a very thin separation bubble. Other observations corroborate this idea, where boundary layer traverses just 1 mm up from the surface fail to capture the separation bubble. Several indicators in this work support the idea that very thin separation bubbles may transition as if attached. For example, urms/Um and intermittency contours show unsteadiness and intermittency can build in the profile locations nearest to the wall, while the skew-kurtosis transition indicators can occur away from the wall; this mix of locations marking transitional activity suggests a mixture of attached-flow and separated-flow transition. In support of separated-flow transition, the alternating skew and higher kurtosis levels seem to occur only in the separated shear layer extending away from the wall with downstream distance. In fact, the Reynolds lapse curve in Figure 4.1 shows L2F behavior that appears attached (gradual decrease in loss with increasing Reynolds number) even though the S3F measurements clearly indicate a reversed flow at the surface of the blade. The skew-kurtosis transition onset criteria was found to coincide with a rise in core urms activity, and this core activity remains outside of the boundary layer until a critical distance when it falls below. This critical distance moves downstream with increasing Reynolds number, and the more forward-loaded L2F allows this unsteady core to remain outside of the boundary layer for longer distances. This trend may help explain why the earlier but lower levels of turbulent activity with the L2F can sustain a more well-behaved, non-massively separated boundary layer, as core activity below the boundary layer may be somewhat dampened. The boundary layers are also connected to the intermittency, as it is seen that Clark’s algorithm picks up on the activity which causes the boundary layer growth mechanisms. The core and maximum intermittencies for all cases remain under the boundary layer once growth begins at the intermittent transition onset location. Intermittency transition onset is found to follow the skew-kurtosis condition, so the skew-kurtosis condition may in fact be the first indicator of transitional activity which is damped in the boundary layer until a level when intermittency increases, signalling a fluid motion that is dominated by turbulent spot growth which in turn causes the enhanced growth of the boundary layer.

As far as transition model validation, the separated-flow model was validated using a combination of S3F and thermal anemometry results. The goal of producing a higher-lift well-behaved airfoil was achieved using the separated-flow model, and its subsequent experimental validation prove the general utility of the model in RANS-based turbomachinery design systems. The attached-flow model was also constructed where possible, and it was seen that separated-flow cases with thin bubbles using the skew-kurtosis transition onset criteria do in fact fit reasonably well onto the attached-flow model curve of Figure 4.18(b). The second form of the attached-flow model presented
in Equation 4.1, which relates physical time scales at the onset of transition, shows that the separated flows which appear to transition as attached flows in Figure 4.18(b) do not in fact truly transition as attached, i.e. the time scales required for attached-flow transition are not in the proper ratios as found in the database used for model development. It was also seen that bubble thickness is not the only factor which changes the attached-flow behavior to separated-flow transition.

The time-averaged S3F measurements obtained in this work were a substantial achievement considering the difficulties associated with a low-speed air environment on a curved surface which deforms under the applied wind load. The data reduction routine for lower-speed tests, where the system noise tends to match or overtake the strength of the sensor signal, still requires development to produce accurate skin friction and normal pressure results. For example, the two rounds of S3F results taken in the trailing edge region of the L2F airfoil produce two different magnitudes of forces, when they should have concurred with each other. Another method of obtaining skin friction data is still required for satisfactory confidence in the technique at these low flow speeds, although initial development of the S3F sensor at ISSI showed agreement with oil-film results within less than a Pascal.
5

Discussion

Remember, then, that science is the guide of action; that the truth it arrives at is not that which we can ideally contemplate without error, but that which we may act upon without fear; and you cannot fail to see that scientific thought is not an accomplishment or condition of human progress, but human progress itself.

– William Kingdon Clifford

The current study sought to produce a high-lift low-pressure turbine blade while validating the use of Praisner and Clark’s transition modeling used in the design cycle. The L2F turbine was produced that allows a 38% decrease in blade count over the Pack B geometry which is representative of airfoils used in current gas turbine engines. The L2F also enjoys an improved Reynolds lapse characteristic, which will allow higher altitude flight, longer loiter times, and reduced fuel consumption. All of the above improvements will lower the total cost of the gas turbine engine, a trend highly sought after from the mature gas turbine industry.

In addition, a shear and stress sensitive film was employed in a very difficult test environment, in hopes to give information useful for transition model validation. The S3F sensor was previously used in water and higher speed air flow environments where the dynamic pressures induce a high level of stress on the surface. In this work, S3F was employed in flow speeds as low as 2 m/s on a curved surface which deforms and twists under the loaded state. The goal was set to acquire time-averaged measurements on the suction surface of the L2F blade in a linear cascade.

Conclusions for this work are now given, followed by recommendations for future work.

5.1 Conclusions

This work has successfully used Clark’s Turbine Design and Analysis System to develop the high-lift LPT designated “L2F”. The separated-flow transition model of Praisner and Clark was used in the
5.1. CONCLUSIONS

The design cycle, and the resulting \( Z_w = 1.59 \) L2F airfoil provides 38% more lift per blade over an LPT similar to those currently in service today (the Pack B). This increase in lift provides two main options to the turbine engine designer: reduce the required blade count by 38% (which reduces the weight and fuel consumption of the engine), or increase the power extracted from the turbine section by 38%. Through this work, we see the aerodynamic effect of pushing the loading forward, as an earlier transition allows a greater suction surface adverse pressure gradient to be overcome without massive separation even at low Reynolds number. In fact, the improved Reynolds lapse characteristic of the L2F significantly decreases the separation losses, thereby reducing the fuel consumption and overall cost of the engine while providing a higher altitude flight ceiling. The earlier and increased passage acceleration of the L2F also delays the growth of bulk unsteadiness and pushes it towards the wall, a trend which increases with Reynolds number. The earlier transition also forces thinner boundary layers and narrower wakes, effectively leaving more of the passage to enjoy the lower losses associated with inviscid flows.

The separated-flow transition model used in the design cycle was validated in this work with thermal anemometry and S3F measurements. The model was formulated inside the Pratt & Whitney design system which also employs additional modeling not utilized in the current study. Therefore, this work also validates the general utility of the model for RANS-based turbomachinery design systems. Although the acquired experimental data suggests that airfoils with very thin separation bubbles may transition as if attached, comparisons of the experimental data to the separated-flow model and its associated attached-flow model show that the boundary layer properties required for attached-flow transition are generally not in the proper ratios when separated. Therefore, separated-flow cases with thin separation bubbles do not in fact transition as if attached. It was also found that separation bubble height is not the only factor which switches the attached-flow transition mechanism to separated-flow transition.

This work also proves that steady CFD with adequate transition modeling can still produce a well-behaved airfoil. Unsteady modeling techniques such as wakes and blade interactions were not necessary even at the incredibly high loading levels in this work. This was only possible due to the separated-flow transition modeling of Praïsner and Clark employed in the design cycle.

The extensive database of experimental data produced in this work on the L1M and L2F airfoils can also be used for CFD code validation. Boundary layer data has been collected at various profile locations along the suction surface of each blade at 50kHz for 5 second records starting 1mm up from the surface and extending far enough into the freestream to resolve the boundary layer heights. Traverses in the wakes of both airfoils provide wake widths and depths along with pressure loss characteristics. The S3F technique has provided surface friction data which gives locations of
5.2. FUTURE WORK

The S3F technology developed by ISSI has undergone substantial development in this work. The operational envelope of the technique has been extended to low-speed air environments on curved surfaces, and the time-averaged friction measurements presented in this work are the first of their kind. Simply obtaining the time-averaged surface friction results in this work is a monumental achievement when considering the decreased signal levels surrounded by relatively larger noise levels. New composition films have also been developed for this regime, and data reduction techniques are being improved which can account for the difficulties encountered in this work, namely low-speed environments on curved surfaces which translate and deform under the wind-on loaded state. As S3F uses optical data acquisition methods which are highly sensitive to even a few pixels in model movement, the issues being discovered and addressed in this work will undoubtedly open the door for the technique to be applied in a wider range of tests than previously possible. At the end of this work however, oil-film interferometry is still required to validate the magnitudes of friction forces measured with the S3F sensor, highlighting the need for further data reduction development in this flow regime.

5.2 Future Work

The fact that separated flows with thin bubbles have been observed to transition similar to attached flows begs several questions: if thin bubble separated flows can transition as if attached, what are the critical flow physics that switches attached-flow behavior to separated-flow behavior? Bubble thickness cannot be the only factor which influences this change, so what are the other important factors? What could knowing this critical situation do for designers? Is this merely a result of a higher magnitude pressure gradient forcing a transition near a reattachment location? Further study with a range of airfoil shapes producing a range of bubble thicknesses, although exhaustive, could shed some light onto the answers to these questions. In addition, techniques such as PIV taken at a fast enough rate could answer the question of whether or not flows with very thin separation bubbles transition in the shear layer above the bubble or instead inside of the bubble. If they do in fact transition above the bubble, perhaps the correct amount of momentum thickness for the attached-flow model would be obtained by integrating over the distance from the top of the bubble to the boundary layer height.

It has been suggested that profile loss accounts for 1/3 of the total pressure loss experienced by the airfoil [Denton 1993]. The aggressive loading levels achieved with front-loaded designs force a higher magnitude pressure gradient to push the flow in the upstream portions of the airfoils. This
increased upstream pressure may change the amount of loss production in the upstream portion of the blade, and further study could provide information to help tailor the profile geometry for lower loss generation in the presence of higher pressure gradients, thus producing an improved leading edge design critical for front-loading and the associated benefits explained in the current study.

In addition, the current modeling produced a well-behaved airfoil at extremely large loading levels compared to current designs. But what is the maximum loading we can achieve in a subsonic environment without massive losses? Further design work using the current philosophies and modeling capability could tell the turbine designer how much bigger his design space has become. In addition, further study of the highly-loaded designs in environments more realistic to the turbine engine, such as annular cascades and rotating rigs, could provide more realistic results including the effects of endwalls, compressibility, secondary flows, and wake-blade interactions.
More on Higher-Lift Design Efforts

This appendix includes other LPT design work which was not described in the Introduction, but was included in Tables 1.1 and 1.2.

In 1997, Cicatelli and Sieverding experimentally studied the effects of vortex shedding with a front-loaded LPT with low suction surface flow turning at an exit isentropic Mach number of 0.4, \( Re = 2 \cdot 10^6 \), and FSTI=1.15\% [Cicatelli and Sieverding 1997]. They found that the shape of the shedding frequency spectrum is influenced by both the pressure and suction sides, but the difference in the transverse transport of fluid causes the pressure side vortex component to dominate the suction side contribution. They also found that their transitional boundary layer spectra contained a dominant bandwidth with two distinct maxima instead of a single dominant frequency.

In 2000, Wolff et al. employed hot-wire anemometry to study the boundary layer behavior on a high-lift LPT cascade similar to the Pack-B profile [Wolff et al. 2000]. They found that the becalmed region after the wake impingement combines laminar and turbulent benefits of the full velocity profile. The addition of wakes allows the flow to withstand a larger adverse pressure gradient without separation. They found that wakes initially increase the boundary layer thickness followed by a decrease in thickness caused by the accelerated flow before the next wake hits. The wakes also increase the local wall shear stress while shifting the region of loss production upstream of the impingement location.

In 2001, Dennis et al. used a 2D Navier-Stokes finite volume flow solver with a \( k - \epsilon \) turbulence model to optimize an airfoil for minimal pressure loss [Dennis et al. 2001]. In this work they incorporated a tournament selection genetic algorithm (GA) using elitism together with sequential quadratic programming (SQP) techniques to optimize the airfoil geometry. Their fitness function was dependent on the outlet dynamic pressure, total lift, exit flow angle, mass flow rate, blade cross-sectional area, and a maximum thickness criterion which provided for mechanical and thermal feasibility. They found that the global aerodynamic objectives are better met through shape op-
timization instead of inverse design, where the airfoil geometry is modified until a pre-determined pressure distribution is achieved. Although near-wall accuracy is left to be desired with the \( k-\epsilon \) turbulence model, Dennis et al. found the best overall design to be thicker and more loaded in the leading edge region.

Howell et al. in 2001 experimentally studied high lift profiles with \( Zw \approx 1 \) for aft-loaded LPTs over \( Re \) from 100,000 to 210,000 at a FSTI of 0.5% [Howell et al. 2001]. With hot-wire anemometry, they compared their losses to Thwaites’ approximation for a laminar flow [White 2006] in order to assess the separation-induced effects. They found that as the loading increased, the bubble reattached earlier with a decreased length, while the bubble height and overall losses increased. The authors suggest these trends are due to an increase in the deceleration caused by the separation which in turn increases the receptivity of the boundary layer to disturbances. Additionally, the authors studied wake effects and found that the boundary layer is most receptive to wake-induced transition if the Reynolds number based on momentum thickness (\( Re_{\theta} \)) is near 250 at the point of separation onset. Howell et al. also found that the pressure distribution is not particularly sensitive in determining the value of \( Re_{\theta} \) just before separation. The authors found that aft-loading the LPT decreases the losses unless the flow is separated, in which case the losses are substantially increased. They found wakes to be beneficial for aft-loaded profiles, with proper wake frequencies always causing reattached flow, resulting in lower losses with a 15% increase in lift. Interestingly enough they found that doubling the wake frequency actually increased the losses sustained in their cascade. Their results suggest a reduction in blade count by 15%.

In 2002, another group including Howell experimentally studied the so-called “high-lift” and “ultra-high-lift” concepts of the Rolls-Royce Deutschland BR710 and BR715 LP turbines with \( Zw \approx 1.05 \) over \( Re \) from 60,000 to 120,000 [Howell et al. 2002]. They found that turbulent flow features in the boundary layer traveled around 90% of the freestream velocity near the leading edge, slowing to around 50% near the trailing edge. This difference was attributed to the changing pressure gradient along the suction surface of the blade. Again generating wakes upstream of the test section, they also found that the wake generates a turbulent spot on the surface and then quickly overtakes the leading edge of that spot, subsequently generating a new turbulent spot ahead of the previously generated spot on the surface. This process excites the shear layer and causes a roll-up into vortices which shed downstream. Thus a single wake can influence multiple locations along the surface through this “leap-frogging” effect. They found that this process can give the wake (which is now “convecting” inside the boundary layer) a region of influence which at times can locally travel faster than the freestream. Each of these turbulent spots eventually subside into a calmed region which is in turn subjected to the next incoming wake. The calmed regions have a full velocity profile and low entropy.
generation which can itself inhibit spot generation and withstand a larger adverse pressure gradient than laminar boundary layers alone [Schulte and Hodson 1996; 1998]. A nice visual observance of turbulent spots can be found in the work of Anthony et al., where fast-response heat flux gauges were employed to track a turbulent spot and its surface shape development during bypass transition [Anthony et al. 2005]. Howell et al. also found that the boundary layers of higher-lift LPTs do not exhibit as much of a becalmed effect after a wake passing as was found on lower lift profiles. Again this group found that \( Re_{\theta} \sim 250 \) was optimal for wake-induced transition in a separated laminar boundary layer, and additionally they found that wake-induced transition can only occur if the flow is already separated. This implies that there is some mechanism in the separated condition which allows the amplification of the wake disturbances into transition. They also conjectured that low-speed tests exhibit all the important features of higher-speed, rotating rig tests only when wakes are introduced.

In 2004, Sieverding et al. used a 3D flow solver (MISES) which coupled the Euler (inviscid) and integral boundary layer equations with the Abu-Ghannam and Shaw turbulence model at a freestream turbulence level of 5% to optimize a compressor blade for optimal range and performance [Sieverding et al. 2004]. The geometry was specified by 4th order Bezier curves with 2 linear patches, and they found that GAs with elitism and mutation worked better to define the optimal shape than gradient search methods and neural networks. Their constraints included the exit flow angle, moment of inertia, cross-sectional area, and the number of turning points used to define the geometry. They also found that the boundary layer shape factor, \( H \), rises through transition but flattens when encountering the favorable pressure gradient along the compressor blade. Other recent compressor design work includes that of Sonoda et al., where they attempted to optimize an airfoil for low-\( Re \) conditions using a modified Navier-Stokes solver with Chien’s \( k - \epsilon \) turbulence model [Sonoda et al. 2004]. Trying to minimize the total pressure loss and deviation gas flow angle, they found that a front-loaded compressor blade leads to an earlier boundary layer transition which delays or eliminates the separation creeping up from the trailing edge.

In 2004, a group at the von Karman Institute for Fluid Dynamics investigated the performance of a “very high lift” LPT blade with an emphasis on the prediction of separation [Houtermans et al. 2004]. Their experimental study focused on a front-loaded blade with a quoted \( Zw = 1.47 \) over \( Re \) from 50,000 to 200,000 at a FSTI=0.6%. Their blade suffered increased losses as \( Re \) was increased. The separation bubble investigations attempted to verify prediction methods presented in other works for locations of separation onset, maximum bubble displacement, bubble length, point of pressure recovery in the bubble, transition onset, and transition length. Models of [Mayle 1991], [Roberts 1975], [Walker 1993], [Hatman and Wang 1999], and [Yaras 2001; 2002] among others
were compared, but none successfully matched the experimental data for the high-lift blade. This illustrates the difficulty of constructing a transition model which can consistently and accurately predict separation bubble and transition phenomena for a low-pressure turbine blade over varied loading. A goal of the current work is to assess the accuracy of Praisner and Clark’s transition model which was constructed for use over varied loading characteristics in a design capacity. Houtermans et al. also suggest that the position of the suction side velocity peak (pressure minimum) strongly influences the overall performance, similar to others’ findings [Curtis et al. 1997].

In 2004, Stieger et al. experimentally studied wake-induced transition in a laminar separation bubble in a cascade of their aft-loaded “ultra-high-lift” \( Zw \approx 1 \) T106 LPT blade [Stieger et al. 2004]. They found that the wake-boundary layer interaction was much different once inside the separation bubble, and that large amplitude pressure fluctuations arise within the bubble which are indicative of coherent flow structures within the bubble. Their wake-induced flow structures traveled at nearly half the freestream velocity, but were not found to be solely a function of the wake passing frequency, which suggested evidence of non-linear interactions within the boundary layer. They also found that only a select band of frequencies can be amplified in the boundary layer to form coherent structures which may lead to a Kelvin-Helmholtz vortex roll-up.

In 2005, Zhang and Hodson experimentally studied the boundary layer development of their “ultra-high-lift” \( Zw = 1.19 \) mid-loaded T106C LPT when subjected to surface trips and unsteady wakes for \( Re \) from 134,000 to 255,000 [Zhang and Hodson 2005]. They found that the optimal wire diameter needed to decrease the separation-induced losses changed with \( Re \), but was effective over both steady (no wakes) and unsteady (wake perturbations) environments. Ironically, as \( Re \) was increased beyond a limit, the separation bubble size decreased but the unsteadiness created by the trip wire caused a greater turbulence-wetted surface area which increased the overall losses. Placing the trip near the bubble also increased the losses when compared to an upstream placement due to the small separation bubble created by the trip wire merging with the pressure-gradient-induced bubble. Their suggestion for LPT design, which surprisingly agrees with Howell’s observation, was to aft-load the profile only if trips could be used to enforce disturbances which would control the separation. Their data also suggests that a Kelvin-Helmholtz vortex roll-up is only possible when the separation bubble reaches a minimum height. This minimum height allows for more pressure peak variation in the unsteady bubble pressure. A successful surface trip mitigates the effect of the Kelvin-Helmholtz roll-up encountered in the separated boundary layer. The combined effect of trips and wakes reduced the losses because of the absence of strong roll-up vortices and the smaller separation bubble in between wakes. The authors believe the loss reduction is a compromise between the positive effects of a reduced separation bubble and the full boundary layer velocity profile of the
calmed region and the negative effects of a larger turbulent-wetted area. The optimum trip location
was found to be midway between the velocity peak and the onset of separation.

Closed-loop separation flow control methods using the L1M profile were computationally studied
by Brehm et al. in 2006 for \( Re = 20,000 \) and \( FSTI = 0.3\% \) [Brehm et al. 2006]. In order to get a
computational baseline flow on the L1M, they employed a 5\(^{th}\) order accurate upwind Roe scheme for
the convective fluxes, a 4\(^{th}\) order accurate viscous discretization, and a 2\(^{nd}\) order accurate Adams-
Moulton time integration for their 2D finite volume implicit solver. A normal blowing slot was chosen
for the flow control method as it is easier to implement in CFD codes (compared to, say, a VGJ hole
angled and skewed to the freestream). They conducted an open-loop parameter study which was
compared to two closed-loop methods, a proportional-differential (PD) feedback controller and a
neural network trained by the open-loop parameter study which models the L1M flow (downstream
pressure signal) and adjusts the forcing signal as necessary. The open-loop control most effective
frequency changed from 8 to 7 Hz as the blowing amplitude was increased, but above a critical
blowing ratio the jet acted as more of an obstruction than a separation controller. The closed-loop
PD controller achieved a 21\% reduction in the ratio of normal-to-axial aerodynamic forces (current
goal). Their neural network showed a promising ability to model flow control conditions inside
the open-loop parameter study box. All control methods resulted in smaller-than-separation rollers
which propagated off of the suction surface. Future work will focus on real-time feedback control
using modified neural networks.
AFRL’s LSWT Turbulence Characterization

This section presents the measurements taken in AFRL’s Low-Speed Wind Tunnel in order to characterize the turbulence development in the test section inlet along the wind tunnel streamwise direction when a turbulence generating grid is placed upstream of the test section. The nominal background turbulence intensity without the grid was measured to be less than 0.5%, but a higher degree of turbulence is required for the cascade tests in order to simulate the low-pressure turbine environment. The grid is located 90.5 inches (2.3m) upstream of the leading edge of the test blade. Figure B.1 shows the locations of the measurement points within the wind tunnel streamwise cross-section, including the centerline (CL) and 4 additional points each 1.5 inches (0.04m) horizontally and vertically away from the center. The additional points were taken in order to ensure the results were not biased by a probe position behind or in between one of the grid bars. When using grids to generate turbulence, two events compete to determine the eddy scales - a spectra manipulation effect whereby the grid chops and redistributes the existing background turbulence scales proportional to the grid size, and a wake effect which can decrease the length scales with high frequency fluctuations [Roach 1986]. Given enough time, the wake effect smooths out after approximately 10 bar diameters downstream and the spectra manipulation effect dominates with scales increasing with distance in the downstream direction.

Figure B.2 shows the freestream turbulence development along the wind tunnel from the grid to the cascade face. As expected, an exponential is observed which decays down to approximately 3.4% just 5 inches (0.13m) before the leading edge of the test blade. Roach’s streamwise turbulence intensity correlation for a square-mesh grid with round bars is also plotted in the figure and is shown.
Figure B.1: Wind tunnel cross-section turbulence measurement locations.

Figure B.2: LSWT FSTI development.
in Equation B.1:

\[ FSTI = 0.80 \left( \frac{x}{d} \right)^{-5/7} \] (B.1)

where \( x \) is the downstream distance and \( d \) is the bar diameter. For the current setup, the correlation estimates \( FSTI = 3.3\% \) at this location, within \( 2.9\% \) of the measured value. Roach suggests that the normal components of turbulence, although not measured here, follow the ratio \( (u'^2 / v'^2) \sim (w'^2 / u'^2) \sim 1.25 \) resulting in normal \( FSTI \) of approximately \( 3.0\% \).

Turbulence scales are computed according to the methods described by Bernard and Wallace 2002, and these are briefly described here for convenience. The integral scale is computed by first computing the autocorrelation of the hot-wire signal:

\[ R_\tau = \frac{u(t)u(t + \tau)}{u'^2(t)} \] (B.2)

This autocorrelation function is then integrated from the origin to the first zero crossing resulting in an integral time scale. Using Taylor’s frozen turbulence assumption, one can multiply the time scale by the freestream velocity and produce a length scale which serves as the integral length scale, \( \Lambda \).

The Taylor microscale, \( \lambda \), is derived using the slope and curvature of the autocorrelation function, \( f \), at the origin:

\[ 0 = 1 + \lambda \frac{\partial f}{\partial r}(0) + \frac{\lambda^2}{2} \frac{\partial^2 f}{\partial r^2}(0) \] (B.3)

The slope at the origin is assumed zero since the sampling frequency was more than adequate to provide a smooth origin, and the second order partial derivative is approximated with a second order accurate central difference. The Kolmogorov length scale, \( \eta \), is derived using the rms fluctuating velocity, integral scale, isotropic dissipation rate, \( \epsilon \) [energy/s], and the kinematic viscosity of air, \( \nu \) [m²/s]:

\[ \epsilon = \frac{u'_{rms}^3}{\Lambda} \] (B.4)

\[ \eta = \frac{\nu^{1/4}}{\epsilon^{1/4}} \] (B.5)

The turbulent scale developments are presented in Figure B.3, with an integral scale approximately between 1.38 and 1.65 inches (3.5e-2 and 4.2e-2 m), a Taylor microscale between 0.15 and 0.32 inches (3.8e-3 and 8.1e-3 m), and a Kolmogorov length scale between 0.03 and 0.02 inches (7.6e-4 and 5.1e-4 m) just 5 inches (0.13m) upstream of the leading edge of the test blade. For a scale resolution within 1% accuracy, Roach suggests a minimum sampling frequency, \( f_m \), according to Equation B.6:

\[ \frac{f_m \lambda}{U_{\infty}} > 5.0 \] (B.6)

where \( \lambda \) is the scale of interest. To resolve the lowest measured Kolmogorov scale in this work, this requires a minimum sampling frequency of approximately 23kHz, well below the current sampling
frequency of 50kHz. Roach also suggests that the integral scale is independent of Reynolds number while the microscales are dependent on Reynolds number, trends also observed in the current data of Figure B.3. Roach’s correlations for the streamwise integral and Taylor scales are presented in Equations B.7 and B.8 below:

$$\Lambda_x/d = 0.20 \left(\frac{x}{d}\right)^{1/2} \quad (B.7)$$

$$\left(\frac{\lambda_x}{d}\right)^2 = \frac{17.0}{Re_d} \frac{x}{d} \quad (B.8)$$

where $\Lambda_x$ is the streamwise integral scale, $\lambda_x$ is the Taylor microscale, and $Re_d$ is the Reynolds number based on bar diameter. These correlations produce integral and Taylor scales of 1.85 and 0.64 inches (4.7e-2 and 1.6e-2 m), respectively, within 12-34% and 100-327% of the measured values, respectively. It is noted in Roach’s paper that the integral scale correlation is more accurate than the Taylor scale correlation since the integral scales are mainly dependent on the grid bar diameter while the Taylor scales are more dependent on the instrumentation setup and the complex energy cascade of turbulence, which is difficult to match over distinct experimental configurations.
Figure B.3: LSWT turbulence scale development.
C

Background on Pressure and Skin Friction Measurement

By understanding the past capability and limitations of surface force measurements, one can better understand and appreciate the development of new technologies. Probably the first recorded skin friction measurement apparatus was that described by William Froude in 1872, which measured the skin friction experienced by planks dragged across water, as mentioned in recent skin friction reviews [Hakkinen 2004; Plesniak and Peterson 2004]. There are basically two types of skin friction sensors: direct and indirect. Direct sensors are advantageous because they do not rely on theoretical or empirical correlations, but they can be very delicate and susceptible to system mechanical noise. Indirect skin friction sensors infer the skin friction from other quantities and are typically more robust. Indirect sensors are advantageous because the measured quantity is typically easy to obtain, although the correlation dependence can impose a limit on the applicability range. Surface pressure measurement is classically accomplished by pressure taps or transducers, but these only provide information at discrete locations. More advanced techniques such as pressure sensitive paint (PSP) can provide non-intrusive, two-dimensional surface pressure data with significantly less model preparation [Liu and Sullivan 2005].

Direct Skin Friction

Direct skin friction sensors are typically based on a floating element design as seen in Figure C.1, an oil film technique, or shear-sensitive liquid crystals. Floating element sensors and associated error correction schemes received great advancement at the California Institute of Technology in the 1950s, and these relate the translation of the floating element under an applied shear stress to the skin.
friction. The accuracy of these direct sensors was mainly limited by their sensitivity to streamwise pressure gradients which create gap forces between the floating element, linkage mechanisms, and housing cavity. Adverse pressure gradients, such as those encountered in the LPT section, were particularly troublesome. The accuracy in 1969 was quoted within 10-15% of the actual skin friction value. Some potential drawbacks when using floating element sensors can be sensor spatial and force resolution trade-offs, misalignment errors, pressure gradient effects, and sensitivity to acceleration, vibration, and thermal expansion effects.

Oil film techniques use the proportionality of shear stress with the thinning rate of an oil film according to Equation C.1:

\[ \tau = \eta \cdot \frac{d}{dt} \left( \frac{dx}{dh} \right) \]  

where \( \tau \) is the skin friction, \( \eta \) is the oil’s viscosity, \( x \) is the streamwise thinning and \( h \) is the thickness of the oil. There are two types of oil film skin friction techniques: interferometric and non-interferometric [Tyler et al. 2004]. Interferometric oil films work because the oil thickness is less than the coherence length of the light source, and the rays reflected from the thinning oil surface interfere with the rays reflected from the model surface, producing alternating regions of light and dark bands known as “interference fringes” [Plesniak and Peterson 2004]. Liu and Sullivan in 1998 used the level of luminescence of oil seeded with fluorescent molecules to determine the oil thickness. This method eliminates interferometry but requires additional calibration. All oil film
skin friction techniques require additional measurement of surface temperature and knowledge of the temperature-oil viscosity relationship [Plesniak and Peterson 2004].

A third common direct sensor are shear-sensitive liquid crystals, which had significant development in the 1960s [Plesniak and Peterson 2004]. This technique relies on the change in optical properties of liquid crystals applied to a test surface as they undergo a change in phase. This phase change can be temperature, heat transfer, or shear stress dependent. The optical property changes can be temperature, color, or intensity based. Drawbacks using liquid crystals can be the complicated calibration and optical access issues, temperature-sensitivity of liquid crystals, the need to apply a new coat after each test, crystal roughness effects on the flow, and limited availability and cost of the liquid crystals.

Indirect Skin Friction

Classical indirect skin friction sensors include the Stanton tube developed in 1920 and the Preston tube developed in 1954 [Plesniak and Peterson 2004]. These techniques use Pitot tube-like probe heads which are located very close to the surface. These methods assume a region of flow similarity close to the wall, which is only generally acceptable for turbulent flow conditions. The Clauser correlation also developed in 1954 is a classical indirect skin friction method based on the law of the wall analysis wherein the skin friction is related to the measured velocity profile in a turbulent boundary layer. Surface and sublayer fences designed to remain in the sublayer have been used since the 1960s. These record shear stress perpendicular to the fence and can be used in strong pressure gradients and compressible flows. This discrete point attainment of surface shear stress supplied the needed empirical information for low-Re computational turbulence modeling routines using sublayer wall functions for closure. Recent techniques using micro-electromechanical systems (MEMS) are based on fence deflections and can have responses up to 1 kHz, as reported in 2004.

Heat transfer analogies which relate the heat transfer obtained with hot-wire or hot-film devices to the skin friction can be used in any fluid where the conductivity of the fluid is greater than the conductivity of the wall (which does not include air) [Plesniak and Peterson 2004]. The underlying assumption with these techniques is that the thermal boundary layer lies entirely in the inner region of the velocity profile, which is why the sensors are placed in the viscous sublayer. Advanced probes use multiple-wire configurations to provide direction-independent, time-dependent wall shear stress. Another indirect skin friction sensor is an optical sensor which detects the Doppler shift of light scattered from particles passing through divergent fringes in the viscous sublayer. This technique, using what are termed Laser-based or “Fan Fringe” sensors, suffers from the interaction between
low data acquisition rates and low seed densities near the wall. As a result, the method has poor
resolution of the sublayer in high $Re$ flows due to turbulent boundary layers.

**Surface Pressure Measurement**

The first global aerodynamic surface pressure measurement was accomplished with pressure sensitive
paints (PSPs) in the 1980s [Liu and Sullivan 2005]. PSP offers accurate, non-intrusive pressure
measurement with increased spatial resolution when compared to conventional taps and pressure
transducers. PSP systems use optical techniques to detect the pressure-sensitive luminescence of
chromophores suspended in an oxygen-permeable binder. As the intensity is a function of the oxygen
concentration in the binder, the surface partial pressure of oxygen is related to the luminescent
emission. The surface pressure can be obtained from Henry’s Law knowing the concentration of
oxygen in the main flow. It is generally accepted that PSP techniques for air flow environments
must have a velocity greater than $\sim 15 \text{ m/s}$ for accurate quantitative results.

The work of Liu and Sullivan in 1998 may pre-figure S3F development in 1998 when they pub-
lished their work using luminescent oil films to measure skin friction. S3F uses the same luminescence
technique to obtain film thickness measurements, but additionally provides about 25 times the pres-
sure sensitivity of PSP and comparable signal-to-noise ratio with less data averaging, allowing more
measurements for a given area in lower dynamic ranges unsuitable for PSP [Fonov et al. 2005].
S3F also combines a particle tracking algorithm for tracer particles applied on the surface of the
luminescent elastic polymer film. The tracer particle translations and film thicknesses (luminescent
intensities) are fed into an inverse finite element code which produces the surface normal pressure and
tangential surface stress contours that caused the deformations. More information on the S3F tech-
nique is presented in the Experimental Arrangement chapter. Recently, this technique was applied
to plasma flows in a Mach 5 tunnel at Wright-Patterson Air Force Base in Dayton, OH, alongside
temperature- and pressure-sensitive paints [Crafton et al. 2005]. Since these techniques are optical
and require no electrical equipment on the test surface, they are ideal for plasma environments where
electrical equipment on the surface would interfere with the plasma kinetics. Information about the
science behind or acquisition of PSP, TSP, PIV, or S3F systems can be found on the Innovative
S3F is an elastic polymer film impregnated with luminescent molecules and doped with tracer particles on its surface [Fonov et al. 2005]. The origin of the S3F technique began in the early 1990s as a direct method to measure surface shear force [Tarasov and Orlov 1990]. Upon application of a force, the film’s deformations in all three dimensions are recorded simultaneously using a single CCD camera. The luminescent molecules are excited at one wavelength and emit at another wavelength, and the intensity of the emission wavelength is proportional to the thickness of the film. Both a “flow-off” and “flow-on” image are required in order to track the surface deformations indicated by the movement of the tracer particles between the two conditions. A ratio between the two conditions also provides a means to cancel out sources of error such as unequal illumination and uneven luminophore dispersion. This ratioing effectively makes the S3F a differential pressure gauge with tunable dynamic range calibration by modifying the film’s modulus of elasticity and Poisson ratio. The normal pressure and tangential surface stresses which caused the three-dimensional deformations are reconstructed from an inverse finite element model, whose inputs are the film thickness-sensitive luminescent intensity and the tracer translations between flow-off and flow-on conditions.

Some insight into the operation of the S3F technique can be gained by considering the simplified response of the film to normal and tangential loads. The response to a purely normal load is shown in Figure D.1(a). As mentioned above, the film will deform under the normal load but will not compress or yield. The local thickness of the film will be modified by the presence of the load near the point of application, and will return to its original shape upon its removal. Maximum surface displacement is a function of the material properties and the applied normal load. Materials are typically formulated in order to ensure a deflection less than 5% of the total film thickness under maximum anticipated loading, and can be produced to provide less than 1% deflection. The issue of concern is to ensure the film displacement does not introduce flow changes due to the surface deflection. The stressed film thickness is a function of the applied normal force, the original thickness
of the film, and its shear modulus, \( h = f(F_N, h, \mu) \). The film responds to gradients in pressure and not to changes in static pressure. This can be a significant advantage for several reasons. First, the sensor is a differential rather than an absolute gauge and thus can be tuned for applications that require larger or smaller sensitivity. Furthermore, the result is a shear sensor that is insensitive to static pressure changes.

The response of the film to a purely tangential force is depicted in Figure D.1(b). Here, the surface of the film will undergo a tangential displacement due to the load but again will not yield or compress. The response of the film may be visualized by considering a series of markers on the surface of the film. The markers will be displaced as the film shears and this displacement is a function of the film properties. Again, upon removal of the load the film will return to its original shape. The actual response of the film is more complex as the responses are mildly coupled; a pure tangential load will generate a slight change in film thickness and a pure normal load will generate a slight tangential displacement. These simplified examples however demonstrate the basic operation of the S3F.

There are several ways for films to be applied to a surface including spraying with an airbrush, allowing the film to polymerize in a cavity on the model surface, and forming the film in a cavity on a flexible layer which can be glued onto a model surface. Forming films in cavities provides good control of the film thickness and physical properties and control of these parameters is necessary for quantitative measurements of pressure and shear stress. Film formation consists of pouring the polymer components into a flat cavity with a smooth or polished bottom. The film thickness can be estimated by direct measurements using either optical absorption or a capacitive thickness gauge. The film calibration procedure involves applying a specified load to the film surface and measuring the corresponding normal and tangential deformation of the film.

A final property of interest is the film’s frequency response and their potential as a high-frequency probe for both shear stress and pressure. The range of the linear frequency response of such an
elastomer is limited by the natural frequency of the shear oscillation, and can be estimated by Equation D.1:

\[ f_o = \frac{1}{2\pi} \sqrt{\frac{\mu}{\rho h^2}} \]  

(D.1)

where \( \mu \) is the shear modulus of the film, \( \rho \) is the film density, and \( h \) is the film thickness. Previous composition variations with \( \mu \in (10 \text{ - } 1000) \text{Pa} \) and \( h \in (0.1 \text{ - } 1) \text{mm} \) have produced films with frequency responses from 0.3 to 10kHz. A detailed description of the technique along with proof of concept tests are now presented.

**Measurement Concept**  
Let’s consider a cavity \( V \) having length \( L \) and depth \( h \) filled with elastic material with known shear modulus \( \mu \) and Poisson ratio \( \nu \). The upper contact surface is submerged in the flow creating a pressure distribution \( p(x) \) and friction force \( f(x) \). Surface loads provide elastic deformations and we can measure normal \( u_y \) and tangential \( u_x, u_z \) displacement fields of the surface points. In the absence of volume forces, the homogeneous elastic material and small deformations equations of equilibrium can be represented as Lame equations [Braess 2002]:

\[(\lambda + \mu)\theta_k + \mu \Delta u_k = 0 \]  

(D.2)

which are based on the Cauchy estimation for the deformation tensor:

\[ \epsilon_{k,l} = \frac{1}{2}(u_{k,l} + u_{l,k}) \]  

(D.3)

Here, the volume deformation \( \theta = \epsilon_{11} + \epsilon_{22} + \epsilon_{33} \), and \( \lambda = (2\mu\nu)/(1 - 2\nu) \). The three equations D.2 are accompanied by boundary conditions. On the contact surface \( S \), displacements \( u_k \) are described as measured functions of coordinates:

\[ u_i = u_{oi}(x_i) \text{ on } S \]  

(D.4)

and \( u_i = 0 \) on the other cavity boundaries. Thus, the described equation system is elliptic in the cavity for all values of Poisson ratio excluding 0.5 and 1 and therefore has a single solution. For the cavity occupying a half-space this is known as the Boussinesq problem and the equation of equilibrium can be integrated.

S3F is a material with a very low compressibility like rubber. S3F is essentially a rarefied polymeric matrix filled with an incompressible fluid. It requires a great deal of energy to produce a small change in density. It simplifies measurements of normal displacement component (see below) but provides an additional problem in the mathematical model - a large difference in the magnitude of Lame constants:

\[ \lambda >> \mu \]
For the sake of simplicity, let’s limit our consideration by a plane strain case, then $u_z$ is zero on the total contact surface. Lame equations D.2 can be rewritten in variational form as [Danaila et al. 2003]:

$$\int_V [2\mu \epsilon_{ij}(u)\epsilon_{ij}(v) + \lambda \epsilon_{ii}(u)\epsilon_{jj}(v)] dx = \int_{S_{ii}} g v dx, \quad v \in V$$  \hspace{1cm} (D.5)

where $g=(f, p)$ are the loads acting on contact surface $S$ and $v = (v_x, v_y)$ is the probe vector. Substituting $\lambda \epsilon_{ij} = p$, we are led to the following problem: Find $(u, p)$ such that

$$\int_V [2\mu \epsilon_{ij}(u)\epsilon_{ij}(v) + p \epsilon_{ii}(v)] dx = \int_{S_{ii}} g v dx, \quad v \in V$$  \hspace{1cm} (D.6)

It is shown in [Danaila et al. 2003] that problem D.6 has a finite element solution that converges uniformly in $\lambda$. The system D.6 provides the possibility to create a system of functions of influence or Green’s functions as FEA solutions for surface loads $g$ which model delta-type functions. In this case, the surface displacements are a convolution of the matrix of influence $G$ and loads $g$ applied on the contact surface $S$:

$$u(x) = \int_S G(x - x') g(x') dx'$$  \hspace{1cm} (D.7)

where

$$G = \begin{pmatrix} r_{xx} & r_{xy} \\ r_{yx} & r_{yy} \end{pmatrix}$$

is the matrix of Green functions or matrix of influence. The FEA approximation of functions of influence is presented in Figure D.2, where constant loads were applied on the interval [0.1h, 0.1h] of an elastic test strip having $\mu = 100$ and $\nu = 0.4999$, with thickness $h=1mm$ and length [-10h, 10h]. Red lines present the surface displacement reaction on the shear load in the shear (1) and normal (2) directions, while the blue lines present the reaction on the normal load in the normal (3) and tangential (4) directions. The normal displacement due to the action of a normal force can be approximated by the function:

$$\tilde{r}_{yy}(x) = \frac{1}{\mu} [(a_0 + a_1 x^2 \exp(-|x|/k_1) + a_2 x \exp(-|x|/k_2)]$$  \hspace{1cm} (D.8)

where parameters $(k_1, k_2, a_0)$ are obtained by fitting the FEA data. The shear (tangential) displacement due to the action of an applied normal force can be approximated by the function:

$$\tilde{r}_{yx}(x) = \frac{1}{\mu} (a_{3x} x \exp(-|x|/k_1))$$  \hspace{1cm} (D.9)

Similarly for the case of an applied shear load, the approximations for normal and shear displacements are given by:

$$\tilde{r}_{xy}(x) = \frac{1}{\mu} (a_{4x} x \exp(-|x|/k_1))$$  \hspace{1cm} (D.10)
Figure D.2: S3F response functions on loads for 1Pa force applied on the interval [-0.1, 0.1].

\[
\tilde{r}_{xx}(x) = \frac{1}{\mu}a_5exp(a_6 + a_7exp(-|x|/k_5) + a_8exp(-|x|/k_6))
\] (D.11)

Rewriting (D.7) in discrete form the reaction to an arbitrary load \( L_{ij} = (L_{xi}, L_{yi}) \) applied at surface location \([x_0, x_N]\) can be presented as:

\[
R_{xj} = \Delta x \sum_{k=0}^{N} L_{xk}\tilde{r}_{xx}(x_j - x_k) + L_{yk}\tilde{r}_{yx}(x_j - x_k)
\] (D.12)

\[
R_{yj} = \Delta x \sum_{k=0}^{N} L_{xk}\tilde{r}_{xy}(x_j - x_k) + L_{yk}\tilde{r}_{yy}(x_j - x_k)
\] (D.13)

This system of linear equations (D.12 and D.13) with unknown \( L_k \) has the diagonally dominant matrix:

\[
\tilde{G}_{jk} = \begin{pmatrix}
\tilde{r}_{xxjk} & \tilde{r}_{xyjk} \\
\tilde{r}_{yxjk} & \tilde{r}_{yyjk}
\end{pmatrix}
\] (D.14)

which can be inverted and used to solve for the applied loads. Figure D.4 presents amplitude-frequency characteristics of the response functions, which were estimated using their approximation of FEA results. The workable region of spatial frequencies is located in the interval \([0.05..0.5]\) where the AFC of normal and cross-talk component reveals differential properties; the AFC of the shear component is integrative and at least 10 to 100 times larger which explains the possibility to resolve a comparatively small friction force in the presence of high pressure gradients.
Figure D.3: 3D presentation of $G$ matrix.

Figure D.4: Amplitude-Frequency Characteristics of normal component (blue), shear component (red), and crosstalk (green) components of response function, $\omega = \Omega/\omega_0$; the spatial frequency referenced to $\omega_0 = 1/h$. Planar FEA model for S3F with $\mu=100$Pa and $\nu=0.99$. 
E

Uncertainty Analysis

“Who can comprehend errors?”
– Psalm 19, verse 13

There are two basic types of errors in measurement: random and systematic [Dieck 2002]. Random errors affect results to cause scatter in test data; systematic errors remain relatively constant over an experiment and cause bias in the results. Both types of errors cause deviation from the true value and are always present in every experiment measuring any type of quantity. Uncertainty analysis seeks to classify these errors and determine their overall effects on the test result in order to gain an appreciation of “how good” the measurement is. This appendix presents a rigorous theoretical uncertainty analysis for the thermal anemometry and pressure measurement techniques employed in this dissertation, as well as a repeatability-based analysis based on test data acquired over a small time interval respective of the time scale used for the results presented in Chapter 4. As the surface stress film is a developing technology and the reduction methodology is under development, no uncertainty analysis is presented for S3F. The total uncertainty definition used in this work is the $U_{95}$ uncertainty model which produces uncertainty intervals at 95% confidence. All terminology presented in this Appendix can be found in [Dieck 2002].

Definitions and Methodology

Several definitions are required to understand uncertainty terminology. The standard deviation of a measurement, $S_x$, is defined as:

$$S_x = \left[ \frac{\sum (X_i - \overline{X})^2}{N - 1} \right]^{1/2}$$

(E.1)
where $X_i$ is the value of the $i$th $X$ in the sample, $\bar{X}$ is the sample average, and $N$ is the sample size. The random uncertainty of a sample, $S_{\bar{X}}$, uses the standard deviation as follows:

$$S_{\bar{X}} = \frac{S_x}{\sqrt{N}} \tag{E.2}$$

The systematic uncertainty of a sample, $B_{\bar{X}}$, is taken from manufacturer specifications of accuracy for a given instrument. There are also errors in measurement due to uncertainties in the instrument calibration process, which are also divided into random and systematic components. The random calibration uncertainty, $S_{\bar{X},cal}$, is defined as:

$$S_{\bar{X},cal} = \left[ \frac{\sum(Y_i - Y_{i,c})^2}{N - K} \right]^{1/2} \tag{E.3}$$

where $Y_i$ is the $i$th data point in a calibration corresponding to $X_i$, $Y_{i,c}$ is the value of the curve fit corresponding to $X_i$, and $K$ is the number of calibration coefficients used in the curve fit. The systematic calibration uncertainty, $B_{\bar{X},cal}$, is determined by the process used to calibrate the instrument. In this work, a Ruska 7250lp low-pressure calibrator was used to calibrate the pressure transducer, and its systematic calibration uncertainty was obtained from the manual provided with the calibrator. The hot-film sensors were then calibrated against the pressure transducer, and the hot-film systematic calibration uncertainties were obtained by propagating the pressure transducer uncertainty through the Bernoulli equation:

$$V = \sqrt{\frac{2P}{\rho}} \tag{E.4}$$

where $P$ is the pressure at the measurement point of interest and $\rho$ is the density of air (1.168 kg/m$^3$). The influence coefficient, which determines the sensitivity of the hot-film test result to the pressure transducer’s uncertainty, is found by taking the partial derivative of the velocity with respect to pressure:

$$\frac{\partial V}{\partial P} = (2\rho P)^{-1/2} \tag{E.5}$$

The systematic calibration uncertainty for the hot-film results is then determined by:

$$B_{\bar{X},cal,HF} = \left[ \left( \frac{\partial V}{\partial P} \right)^2 \left( U_{95pr} \right)^2 \right]^{1/2} \tag{E.6}$$

where $U_{95pr}$ is the total uncertainty of the pressure measurement at 95% confidence given by:

$$U_{95} = \pm t \left[ (S_x)^2 + (S_{\bar{X},cal})^2 + (B_{\bar{X}}/2)^2 + (B_{\bar{X},cal})^2 \right]^{1/2} \tag{E.7}$$

where $t$ is the proper value of the Student’s $t$-distribution. The individual instrument uncertainties must then be propagated into the loss definitions presented as Equations 3.1 and 3.2 and root-sum-squared similar to Equation E.6 above. The resulting influence coefficients (partial derivatives)
are:

\[ \frac{\partial L_{\text{area}}}{\partial (P_{t,\text{in}} - P_{t,\text{ex}})} = \frac{1}{(P_{t,\text{in}} - P_{s,\text{in}})} \]  
\[ (E.8) \]

\[ \frac{\partial L_{\text{area}}}{\partial (P_{t,\text{in}} - P_{s,\text{in}})} = -\frac{(P_{t,\text{in}} - P_{t,\text{ex}})}{(P_{t,\text{in}} - P_{s,\text{in}})^2} \]  
\[ (E.9) \]

\[ \frac{\partial L_{\text{flux}}}{\partial u_{ex}} = \frac{\sum (P_{t,\text{in}} - P_{t,\text{ex}})/\sum u_{ex} - (\sum u_{ex} \cdot (P_{t,\text{in}} - P_{t,\text{ex}}))/((\sum u_{ex})^2)}{(P_{t,\text{in}} - P_{s,\text{in}})} \]  
\[ (E.10) \]

\[ \frac{\partial L_{\text{flux}}}{\partial (P_{t,\text{in}} - P_{s,\text{in}})} = \frac{1}{(P_{t,\text{in}} - P_{s,\text{in}})} \]  
\[ (E.11) \]

\[ \frac{\partial L_{\text{flux}}}{\partial (P_{t,\text{in}} - P_{s,\text{in}})} = -\frac{(\sum u_{ex} \cdot (P_{t,\text{in}} - P_{t,\text{ex}}))/\sum u_{ex}}{(P_{t,\text{in}} - P_{s,\text{in}})^2} \]  
\[ (E.12) \]

### Measurement Uncertainties

The uncertainty in this appendix is determined by two distinct methods: the first being a rigorous treatment of the applicable equations for the loss coefficient following the methodology presented in [Dieck 2002], and the second being a repeatability-based uncertainty determined by comparing multiple sets of test data taken on different days at the same Reynolds numbers.

### Theoretical Determination

All pressure measurement data are acquired with a Druck LPM5481 low-pressure differential transducer with range from -0.2 to 0.8 inches of H\textsubscript{2}O and a measurement face diameter of 1.5mm. Table E.1 presents the uncertainty breakdown for the Druck transducer at two dynamic pressures, 7.5e-3 and 1.3e-1 inches H\textsubscript{2}O (or 1.8 and 7.3 m/s). These flow speeds correspond to the L1M test case Reynolds numbers of 21,000 and 86,000, the lower and upper Reynolds number bounds of the acquired experimental test data for the L1M and L2F airfoils. At both dynamic pressures, the uncertainty is dominated by the random and systematic components based solely on the transducer’s response and not on the calibration process. The high levels of random uncertainty, \( S_\hat{z} \), is believed to be due to two physical phenomena occurring during the uncertainty data acquisition. First, one

<table>
<thead>
<tr>
<th>Pressure</th>
<th>( S_\hat{z} )</th>
<th>( B_\hat{z}/2 )</th>
<th>( S_{\hat{z},\text{cal}} )</th>
<th>( B_{\hat{z},\text{cal}} )</th>
<th>( U95 )</th>
</tr>
</thead>
<tbody>
<tr>
<td>7.5e-3</td>
<td>6.6e-3</td>
<td>1.0e-3</td>
<td>0.0e0</td>
<td>4.5e-13</td>
<td>1.3e-2</td>
</tr>
<tr>
<td>1.3e-1</td>
<td>3.7e-4</td>
<td>1.0e-3</td>
<td>0.0e0</td>
<td>1.3e-10</td>
<td>2.2e-3</td>
</tr>
</tbody>
</table>
Figure E.1: Distributions of pressure measurements about the means [inH₂O].

End of the differential transducer was plumbed to the inlet freestream where the random uncertainty was affected by the freestream unsteadiness generated by the upstream turbulence grid. Second, the other end of the transducer was plumbed to a position behind the cascade close to the inside wall where wake effects should be minimal but still affect the reading. It is suspected that these unsteady mechanisms (turbulence grid and wake effects) induced fluctuations read by the transducer which is wrongly cited as random uncertainty. Figure E.1 shows the distributions of measurements about the mean for both flow speeds, where the x-axis corresponds to each of the 30 measurements taken for uncertainty calculations, the y-axis corresponds to the amount over or under the mean, and the red line represents the respective mean. Each measurement is an average of data taken at 1kHz for 30,000 samples, with an average time of 2 minutes in between successive measurements. In Figure E.1(a) for the mean pressure of 7.5e-3 inH₂O, the standard deviation is equal to 0.00663 inH₂O. In Figure E.1(b) for a mean pressure of 1.3e-1 inH₂O, the standard deviation is equal to 0.00037 inH₂O, which illustrates how increasing the flow speed dramatically decreases the fluctuation about the mean value. For purposes of completeness, this theoretical treatment of uncertainty will continue to use the random uncertainty for the pressure transducer as presented in Table E.1, although it is believed that the low Re random error assessment contains systematic error which renders its use invalid. Therefore, estimates using both the high-Re and low-Re uncertainty will be used at low-Re, since it is believed that the true random error of the pressure transducer was that obtained at the higher Reynolds number.

Throughout this work, two different hot-film sensors were used to acquire velocity data. The first is a TSI 1240-20 x-wire hot-film probe and the second is a single normal 1211-20 hot-film sensor.
Table E.2 presents the uncertainty breakdown for both sensors at the same flow speeds as reported for the Druck pressure transducer. In this table, no entry was supplied for the systematic uncertainty.

Table E.2: TSI 1240-20 x-wire and 1211-20 hot-film uncertainties [m/s].

<table>
<thead>
<tr>
<th>Speed</th>
<th>$\tilde{S}$</th>
<th>$\tilde{B}/2$</th>
<th>$\tilde{S}_{\tilde{x},cal}$</th>
<th>$\tilde{B}_{\tilde{x},cal}$</th>
<th>$U_{95}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.8</td>
<td>2.3e-2</td>
<td>N/A</td>
<td>7.4e-3</td>
<td>1.5e-0</td>
<td>3.0e0</td>
</tr>
<tr>
<td>1.8$^*$</td>
<td>2.3e-2</td>
<td>N/A</td>
<td>7.4e-3</td>
<td>2.7e-1</td>
<td>5.4e-1</td>
</tr>
<tr>
<td>7.3</td>
<td>9.5e-3</td>
<td>N/A</td>
<td>7.4e-3</td>
<td>6.4e-2</td>
<td>1.3e-1</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Speed</th>
<th>$\tilde{S}$</th>
<th>$\tilde{B}/2$</th>
<th>$\tilde{S}_{\tilde{x},cal}$</th>
<th>$\tilde{B}_{\tilde{x},cal}$</th>
<th>$U_{95}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.8</td>
<td>3.5e-3</td>
<td>N/A</td>
<td>6.2e-3</td>
<td>1.5e0</td>
<td>3.0e0</td>
</tr>
<tr>
<td>1.8$^*$</td>
<td>3.5e-3</td>
<td>N/A</td>
<td>6.2e-3</td>
<td>2.7e-1</td>
<td>5.4e-1</td>
</tr>
<tr>
<td>7.3</td>
<td>8.9e-3</td>
<td>N/A</td>
<td>6.2e-3</td>
<td>6.4e-2</td>
<td>1.3e-1</td>
</tr>
</tbody>
</table>

$^*$ Calculations using pressure uncertainty of 2.2e-3 inH$_2$O.

$B_{\tilde{x}}$, since there is no straight-forward way to assess the systematic uncertainty present in the complex hot-wire data reduction scheme. In these situations, the uncertainty reference [Dieck 2002] suggests making an (absolutely dangerous!) educated guess as to the magnitude of the unknown systematic uncertainty if one is comfortable and experienced with such measurements. As this author is not comfortable doing so, the entry is left blank as a “best chance” determination. This table shows how the uncertainties for both the 1240-20 x-wire and the 1211-20 hot-film are dominated by the propagated uncertainty of the pressure transducer. As expected, comparison of the uncertainties for the flow speed of 1.8 m/s using the propagation of low-Re and high-Re pressure uncertainties shows how using the lower random uncertainty for the pressure transducer results in lower uncertainty in the flow speed.

Table E.3 shows the influence coefficients defined by Equations E.8 through E.12 for both Reynolds numbers analyzed. This table shows how the uncertainty in both area-averaged and flux-averaged loss is more heavily influenced by the pressure uncertainty, while the velocity uncertainty does not contribute a significant effect. As expected, the influence of the pressure uncertainty decreases with Reynolds number, as the magnitudes of the influence coefficients at $Re = 86k$ drop significantly compared to those at $Re = 21k$. 
Table E.3: Influence coefficients for loss definition uncertainties.

<table>
<thead>
<tr>
<th>Reynolds #</th>
<th>( \frac{\partial L_{\text{area}}}{\partial (P_{t,\text{in}}-P_{s,\text{in}})} )</th>
<th>( \frac{\partial L_{\text{area}}}{\partial (P_{t,\text{in}}-P_{t,\text{ex}})} )</th>
<th>( \frac{\partial L_{\text{flux}}}{\partial u_{\text{ex}}} )</th>
<th>( \frac{\partial L_{\text{flux}}}{\partial (P_{t,\text{in}}-P_{s,\text{in}})} )</th>
<th>( \frac{\partial L_{\text{flux}}}{\partial (P_{t,\text{in}}-P_{t,\text{ex}})} )</th>
</tr>
</thead>
<tbody>
<tr>
<td>21k</td>
<td>57.47</td>
<td>-49.55</td>
<td>0.00</td>
<td>57.47</td>
<td>-49.55</td>
</tr>
<tr>
<td>86k</td>
<td>3.36</td>
<td>-2.93</td>
<td>0.00</td>
<td>3.36</td>
<td>-2.93</td>
</tr>
</tbody>
</table>

* Units equal to \([\text{inH}_{2}\text{O}]^{-1}\).
† Units equal to \([\text{m/s}]^{-1}\).

Table E.4: Theoretical uncertainty in loss coefficients.

<table>
<thead>
<tr>
<th>Reynolds #</th>
<th>( L_{\text{area}} )</th>
<th>( L_{\text{flux}} )</th>
</tr>
</thead>
<tbody>
<tr>
<td>21k</td>
<td>0.3786</td>
<td>0.3786</td>
</tr>
<tr>
<td>21k*</td>
<td>0.2361</td>
<td>0.2361</td>
</tr>
<tr>
<td>86k</td>
<td>0.0072</td>
<td>0.0072</td>
</tr>
</tbody>
</table>

* Calculations using pressure uncertainty of 2.2e-3 inH_{2}O.

The resultant effect of the instrument uncertainties on the experimental loss coefficients are presented in Table E.4 for both Reynolds numbers. Here, both values of random uncertainty for the pressure transducer are included. This table shows an uncertainty in loss of 0.3786 or 0.2361 at \( Re = 21k \) and 0.0072 at \( Re = 86k \), again showing that the uncertainty decreases with Reynolds number. However, the large value of uncertainty at \( Re = 21k \) must be an extremely conservative estimate using either random uncertainty for the pressure transducer, since a true uncertainty of this magnitude would preclude the attainment of the well-correlated Reynolds lapse trends seen in Figure 4.1. In fact, the correlation between data points, which were taken over multiple days for each airfoil, as well as the agreement of trends seen with the L2F CFD predictions and experimental findings suggest the accuracy of the experimental results must be better than the theoretical uncertainty obtained through the rigorous treatment of propagated uncertainties. Therefore, another method of determining the accuracy of the experimental results based on multiple data sets of the same Reynolds number is presented in the next section to provide an uncertainty based upon the repeatability of loss coefficient determination.
Repeatability-Based Determination

For an estimation of experimental uncertainty based on repeatability, multiple data sets of the same test condition using the L2F airfoil were compared to determine standard deviations of loss coefficients at three Reynolds numbers: 3 sets at $21k$, 2 sets at $34k$, and 3 sets at $39k$. These sets include those presented earlier in Figure 4.1, and give a total of 8 sets upon which an average standard deviation can be produced which represents a repeatability-based uncertainty. Table E.5 shows the calculated area- and flux-averaged loss coefficients and standard deviations of those loss coefficients for each of the three Reynolds numbers. The first entry for each Reynolds number were plotted in the Reynolds lapse plot of Chapter 4.

Table E.5: Repeatability-based standard deviation in L2F loss coefficients at low $Re$.

| Reynolds # | $L_{area}$ | | $L_{flux}$ | | |
| --- | --- | --- | --- | --- | --- | --- | --- | --- | --- |
|            | 1     | 2     | 3     | stdev  | 1     | 2     | 3     | stdev  |
| $21k$      | 0.1788| 0.1889| 0.1695| 0.0097 | 0.1665| 0.1813| 0.1572| 0.0122 |
| $34k$      | 0.1575| 0.1594| –     | 0.0013 | 0.1508| 0.1529| –     | 0.0015 |
| $39k$      | 0.1444| 0.1478| 0.1469| 0.0018 | 0.1383| 0.1422| 0.1408| 0.0020 |

As the standard deviations provided above in Table E.5 represent actual test data, they are believed to be more realistic and representative of the true uncertainty in the loss coefficients. Therefore, the above standard deviations for each of the low Reynolds number cases are accepted for the true uncertainty of the loss coefficients at those Reynolds numbers. The experimental L2F loss coefficients of Figure 4.1 with error bars representing the standard deviations at low Reynolds number found in Table E.5 are plotted along with the theoretical uncertainty for $Re = 86k$ in Figure E.2. This figure shows how the trends described in Chapter 4 remain valid after repeatability-based uncertainty is applied. It is also believed that the amount of uncertainty at $Re = 86k$ represented by the error bars in this figure is greater than what could be expected if multiple data sets were taken for this Reynolds number and a repeatability-based uncertainty was performed.
Figure E.2: L2F Reynolds lapse with error bars at low and high $Re$. 
References


Vita

Mark McQuilling was born in Louisville, Kentucky in 1978. He graduated from Seneca High School in 1997 with Honors from the Advanced Liberal Arts Program. He received his B.S. and M.S. in Mechanical Engineering from the University of Kentucky in 2002 and 2004. He has presented multiple talks and papers on his research at conferences on both the national and international level. He was awarded a Dayton Area Graduate Studies Institute 3-year PhD Fellowship in 2004 and was therefore able to conduct his research at nearby Wright-Patterson Air Force Base in the Turbines Branch of the Propulsion Directorate of the Air Force Research Laboratory. He received his PhD in Engineering from Wright State University in Dayton, OH in 2007. He now lives with his wife, Kylie, daughter, Madison, and puppy, Sadie, near St. Louis, MO where he is beginning his faculty career as Assistant Professor in the Aerospace and Mechanical Engineering Department of Parks College of Engineering, Aviation, and Technology at Saint Louis University.