Hot-Wire Anemometer for the Boundary Layer Data System

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ABSTRACT

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Hot-wire anemometry has been routinely employed for laboratory measurements of turbulence for decades. This thesis presents a hot-wire anemometer suitable for use with the Boundary Layer Data System (BLDS). BLDS provides a unique platform for in-flight measurements because of its small, self-contained, robust design and flexible architecture. Addition of a hot-wire anemometer would provide BLDS with a sensor that could directly measure flow velocity fluctuations caused by turbulence. Hot-wires are commonly operated in constant-temperature mode for high frequency response, but require a carefully tuned bridge. The constant-voltage anemometer (CVA) uses a simple op-amp circuit to improve frequency response over constant-current operation. Due to its balance between ease of operation and performance, a CVA system built for this project was tested with a 3.8 micron diameter, platinum-coated tungsten probe. The CVA was calibrated in a steady jet and a power-law curve fit accurately represented the calibration data. The CVA successfully measured velocity fluctuations in a turbulent jet, as well as in laminar and tripped turbulent boundary layers over a flat plate in a 110 MPH wind tunnel. CVA frequency response was investigated using a thermal/electrical model, controlled oscillation in a steady flow, and with a square wave test; these three methods showed agreement. The CVA is selected for integration with BLDS.

Keywords: Hot-wire, CVA, BLDS, boundary layer, flow transition, frequency response

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NOMENCLATURE

A	=	Power law offset coefficient
a_w	=	$OHR - 1$
B	=	Power law multiplying factor
c_w	=	Specific heat capacity for tungsten wire, 140 J/kg-K in prediction model
D	=	Hot-wire probe wire diameter, μm
E	=	Mean voltage output from HWA, V
E_w	=	Elastic modulus of tungsten wire, 400 GPa
e	=	Instantaneous HWA output voltage, V
f	=	Frequency, Hz
g	=	Gravitational constant, 9.81 m/s ² , 32.2 ft/s ²
G	=	Feedback amplifier gain for CTA
H	=	Amplitude ratio
h	=	Heat transfer convection coefficient, W/m ² -K
I_T	=	Total feedback current in CVA circuit analysis
I_w	=	Hot-wire probe current, mA
k	=	Exponent for inverted calibration curve power law
$k_{f,\infty}$	=	Thermal conductivity of air at film or fluid temperature, W/m-K
L	=	Hot-wire probe wire length, 1.27 mm for 1210-T1.5
L_C	=	Adjustable inductor for cable inductance compensation, H
M	=	Time constant for HWA system (CCA, CTA, or CVA), ms
M_n	=	Mach number
N	=	Number of data points for mean square error calculation
Nu	=	Nusselt number
n	=	Power law exponent
OHR	=	Traditional overheat ratio, ratio of hot to cold probe resistance
q	=	Convection heat loss from the hot-wire probe, W
R_0	=	Probe resistance at 20 °C reference temperature, Ω
R_1	=	HWA fixed circuit resistor, 5000 Ω in CVA prediction model
R_2	=	HWA fixed circuit resistor, 50 Ω in CVA prediction model

R_3	=	Adjustable resistor for balancing bridge circuit, Ω
$R_{a,b}$	=	Resistance of hot-wire probe at conditions a and b
R_{box}	=	Resistance of internal circuitry of CVA system, Ω
R_{cable}	=	Resistance of probe BNC cable, Ω
R_{DVM}	=	Probe resistance at ambient temperature measured by DVM, Ω
Re	=	Reynolds number
R_F	=	CVA circuit resistor, 1000 Ω in prediction model
R_{int}	=	Resistance inside hot-wire probe, before actual wire sensor, Ω
R_L	=	Resistance of probe cable in anemometer circuit, Ω
R_s	=	Adjustable resistor to vary source current, Ω
$R_{support}$	=	Resistance of probe support stem, Ω
R_T	=	Total feedback resistance in CVA circuit analysis, Ω
R_w	=	Resistance (hot) of hot-wire probe at operating temperature, Ω
R_∞	=	Resistance (cold) of hot-wire probe at ambient temperature, Ω
P	=	Offset coefficient for inverted calibration curve power law
Q	=	Multiplier coefficient for inverted calibration curve power law
S_{T_∞}	=	Freestream temperature sensitivity coefficient
S_u	=	Velocity sensitivity coefficient
$T_{a,b}$	=	Temperatures at different conditions a and b , $^\circ\text{C}$
T_f	=	Film temperature, $^\circ\text{C}$
T_∞	=	Ambient fluid temperature, $^\circ\text{C}$
U	=	Freestream velocity, m/s
u	=	Instantaneous velocity, m/s
u_i	=	Uncertainty of a measured parameter
u_f	=	Total uncertainty
V_l	=	Voltage source powering HWA circuit, V
V_o	=	Op-amp output from prototype CVA system, $V_o = V_s/2$
V_s	=	Op-amp output voltage for simplified circuit in literature, V
V_w	=	Probe wire voltage set-point, V

x_i	=	Measured experimental quantity
α_0	=	Temperature coefficient of resistance at 20 °C reference, 0.0042 °C ⁻¹
β	=	Calibration curve adjustment multiplier
δ	=	Boundary layer thickness, mm or in.
δ^*	=	Boundary layer displacement thickness, mm or in.
$\mu_{f\infty}$	=	Air viscosity at film or fluid temperature, N-s/m ²
ω	=	Frequency, rad/sec
$\rho_{f\infty}$	=	Air density at film temperature or fluid temperature, kg/m ³
ρ_w	=	Density assumed for platinum plated tungsten wire, 20,000 kg/m ³
θ_∞	=	Fluctuating ambient fluid temperature, °C

1. INTRODUCTION

Lowering aerodynamic drag on aircraft improves fuel efficiency. This is very desirable in both the commercial and defense sectors of the aerospace industry; for large passenger jets, fuel savings means lower per-seat passenger costs, and for surveillance aircraft, the duration of long-range missions is extended. The total drag force on aircraft generally consists of pressure drag and skin friction drag caused by a viscous fluid boundary layer.

Turbulent flow is a transient type of fluid motion with a random nature that is more prone to occur at higher Reynolds numbers than steady-state laminar flow. Studies by Thibert et al. [1] suggest that up to around 45% of the total drag force on a typical transport aircraft is of the skin friction type and Joslin [2] mentions that the skin friction for laminar flow can amount to as much as 90% less than in turbulent flow at the same Reynolds number. Now consider a conventional aircraft with about $\frac{1}{2}$ of the total drag acting on its wing, with $\frac{1}{2}$ (rounded from 45%) of that wing drag due to skin friction. This means that roughly 25% of the total drag force is skin friction drag on the wing. A laminar flow over half of the wing would therefore result in a realistic 80% (vs. 90% specified) drag reduction in that laminar wing region, for a total drag reduction for the entire airplane of 10%. These rounded estimates show there is a huge potential for better performance and cost savings by maintaining laminar flow. As a result, laminar flow has been a topic of research since the 1930s [2].

Airflow is often laminar near the front of an aircraft or test section, and then transitions to turbulent flow farther down the body. The three processes through which transition occurs are natural transition caused by gradually developing infinitesimal

disturbances referred to as Tollmien-Schlichting waves, bypass transition due to rough walls or a noisy freestream, and separated-flow transition, in which a laminar boundary layer separates and reattaches as turbulent [3]. Defects in body surfaces interrupt laminar flow and trip turbulence sooner than if the surface were smooth. As a result, modern manufacturing techniques use high tolerances to minimize surface imperfections and bonded-sandwich construction methods for fewer protruding fasteners. Wing geometry is strategically designed for favorable pressure gradients in order to delay transition, a technique referred to as Natural Laminar Flow Control (NLFC), and its counterpart, Laminar Flow Control (LFC), uses active suction techniques [2]. The drag could drastically change depending on the laminar-to-turbulent transition location [4], making the prediction and measurement of this location critical to the development of aircraft designed to achieve large regions of laminar flow.

Velocity fluctuations exceed 10% of the local velocity in a turbulent boundary layer and are a direct indicator of laminar-to-turbulent transition. Hot-wire anemometry (HWA) is a very effective method for measuring velocity fluctuations, and this practice involves submersing a wire sensor typically made of tungsten or platinum with a diameter on the order of microns into a fluid flow for velocity measurements. A photograph of a commercial hot-wire probe is shown in Figure 1.1 next to a diagram in Figure 1.2. As the probe is exposed to velocity fluctuations, variable convection heat transfer occurs, causing a change in probe temperature and therefore resistance. The voltage output for an HWA system is either proportional to the hot-wire probe resistance or the probe current, depending on which parameter is variable in the HWA circuit. The goal of this project was to select a suitable hot-wire for the Boundary Layer Data System.

Figure 1.1. Commercial hot-wire probe (TSI 1210-T1.5)

Figure 1.2. Diagram of hot-wire probe

In order to measure the fluid boundary layers and transition locations on real aircraft in flight, the defense contractor, Northrop Grumman Corp., has sponsored the development of the Boundary Layer Data System (BLDS) and the Preston Tube Data System (PTDS) [5]. The BLDS and PTDS are shown in Figures 1.3 and 1.4, respectively.

Figure 1.3. Boundary Layer Data System (BLDS)

Figure 1.4. Preston Tube Data System (PTDS)

The BLDS and PTDS are flight compatible, autonomous flow measurement systems that measure temperature, absolute static pressure, boundary layer mean velocity profile, and mean skin-friction. Autonomous describes the fact that there is no interaction with these devices during flight. Before takeoff they are adhered to the aircraft body and after landing, flight data is off-loaded. The BLDS measures a boundary layer profile with a Pitot tube or other type of pressure probe driven vertically by a motorized stage. The PTDS uses 3 pressure probes: a static surface probe, a freestream total pressure probe, and a Preston tube, which is really a total pressure probe placed flush against the body surface. Figure 1.5 displays a Preston tube.

Figure 1.5. Preston Tube

Preston tube pressure data is used to calculate the mean skin friction. The skin friction coefficients are compared to well known values in laminar, turbulent, and transitional flow, in order to distinguish the flow type. This method has proven successful, but requires significant data post-processing. It is very desirable to have a more direct way to determine whether the flow is laminar or turbulent, and this could be achieved with a sensor capable of directly measuring fluctuations associated with turbulence.

Even with the computing power of modern times, predicting the transition location using computational methods (CFD) remains challenging. Direct Numerical

Simulation (DNS), or direct solution of the time varying Navier-Stokes equations, is beyond modern computational abilities used for aircraft design [6]. Fully turbulent flows are treated by assuming a turbulence model for the Reynolds stress tensor in the time-averaged Navier Stokes equations (RANS). Unfortunately, the RANS equations cannot be used to predict transition because the averaging process removes the effects of linear disturbance growth, as explained by Pasquale et. al [6]. A common method to predict transition is the e^n amplification method. This type of analysis is based on stability theory, where the basic idea is to superimpose a disturbance on the undisturbed boundary layer state and determine whether the perturbations grow or decay [7]. Unfortunately, the e^n method becomes very complicated when applied to complex geometries, and cannot predict non-linear transition phenomena [4]. There are alternatives, including Large Eddy Simulation (LES) and a correlation-based model (LCTM) that uses intermittency and the vorticity Reynolds number [6], [8]. However, a well-validated transition model that is suitable for general-purpose CFD codes does not exist.

Predicting the transition location in wind tunnels is challenging because testing is performed on scaled-down models and the tunnel environment does not perfectly simulate that in flight. Bushnell [9], as well as Pettersson and Rizzi [4], review many concerns in scaling wind tunnel data to full-size aircraft at free-flight conditions. The predicted transition zone is affected by the geometric fidelity of the smaller model; it might not have all the details of the full-scale model [4]. Another problem is that the freestream disturbances in most wind tunnels are significantly larger than the quiet freestream conditions in flight, yielding lower transition Reynolds numbers on the ground

[9]. There are very few special low disturbance facilities and a low disturbance environment for testing laminar flow control is essential [9].

Pressure fluctuations have been investigated as a means of detecting laminar-to-turbulent transition in flight due to the fragile nature of hot-wires. Karasawa [5] tested a Kulite dynamic pressure transducer installed in the PTDS to measure the total pressure fluctuations in a flat plate boundary layer, which have a greater energy content than the static pressure fluctuations for better flow distinction. The Kulite device, shown in Figure 1.6, was evaluated for detecting laminar, turbulent, and transition regions at airspeeds up to 110 MPH on a 48 inch long elliptical nosed flat plate mounted in the 2-ft by 2-ft Cal Poly wind tunnel. Her experiments showed that the root-mean-squared (RMS) value of total pressure fluctuation reached 4% of the dynamic pressure in the transition region, 2% in fully turbulent flow, and about 1% for laminar flow. Unfortunately, freestream measurements showed a noise floor around 1%, so a low signal-to-noise ratio was achieved. While her results did demonstrate that total pressure fluctuations can be used to distinguish between laminar and turbulent flow, monitoring velocity fluctuations using a hot-wire might provide a more sensitive method to locate the transition zone.

Figure 1.6. Kulite Model XCS-062-5D pressure transducer

Hot-wires are able to measure turbulent flow intensities less than 0.1% because they operate with very low noise and high spatial resolution [10]. Unfortunately, they

suffer from thermal inertia because the hot-wire probe has a heat capacity and cannot respond instantaneously to velocity fluctuations. A finite amount of time is required for heat transfer from the wire to occur and induce a temperature change in response to a flow disturbance, and then for the wire to return to its prior temperature. This delay period is referred to as the time constant, or thermal lag. Turbulent flows have a wide bandwidth and accurately measuring velocity fluctuations usually requires compensation of the time constant for better frequency response.

The frequency response of a sensor, or in this case a hot-wire, is the measureable frequency range of velocity fluctuations or disturbances. The expected frequencies in the transitioning boundary layer were predicted using a spatial stability plot developed by Wazzan et. al [11] for the Blasius flat plate boundary layer profile. The neutral curve, shown in Figure 1.7, illustrates the upper and lower frequency limits for amplified disturbances over a range of Reynolds numbers; outside the curve, disturbances are damped. The neutral curve begins with the highest frequencies just under 6000 Hz at the critical Reynolds number based on boundary layer displacement thickness Re_{δ^*} , of 520. This is the minimum Reynolds number for initial instability, as stated by White [3]. For a freestream velocity of 50 m/s, the corresponding distance for the critical location is $x = 1.1$ in. Figure 1.7 illustrates that the amplified frequency range of disturbances decreases to between 600-2000 Hz at $x = 10$ in. and past $x = 25$ in., the frequencies are below 1000 Hz. A hot-wire for BLDS should have a frequency response that can capture most of this range in order to sufficiently measure velocity fluctuations.

Figure 1.7. Estimated frequencies of amplified disturbances in the flat plate boundary layer for 50 m/s freestream velocity and sea level air properties from the spatial stability plot [11]

Previous success and common familiarity with hot-wires make it an appealing solution for BLDS. There are three common types of HWA circuits that each operate differently to reduce the thermal time constant and improve frequency response. The constant-current anemometer (CCA) operates under constant current through the probe, a constant-temperature anemometer (CTA) maintains the probe at constant temperature, and the constant-voltage anemometer (CVA) uses a preset, fixed voltage across the probe. The next chapter explains the three types of HWA systems that were investigated, followed by a comparison of key criteria for BLDS.

2. OVERVIEW OF 3 DIFFERENT HOT-WIRE ANEMOMETERS

In order to select a suitable HWA that best met BLDS requirements, it was necessary to study the functionality of each HWA, as well as compare the benefits and drawbacks between systems. The CCA, CTA, and CVA systems are reviewed below.

CCA

Constant-current anemometry is the oldest and most basic mode of operation. See Figure 2.1 for a typical constant-current circuit. The current through the left arm of the bridge containing the hot-wire probe R_w , remains nearly constant as velocity fluctuations cause a change in the wire temperature and consequently, its resistance. A serious downside to constant-current is the risk of wire burnout. If the flow velocity is decreased during operation and prevents sufficient heat convection for a safe wire temperature, probe burnout will occur for a high enough current.

Figure 2.1. Simplified diagram of a CCA system with RC compensation [12]

If this anemometer type were chosen for BLDS, it would require the decision between a very simple circuit set-up with a single hot-wire probe resistor in series with a modern constant current source, or a more complicated Wheatstone bridge arrangement.

The traditional, old-fashioned circuit contains this bridge for a couple reasons. First, the bridge provides the means for a differential voltage output instead of an offset voltage measured across a hot-wire probe as the single resistor in series with a current source [13]. Also, the bridge is required for tuning a frequency compensation circuit, as explained by Perry [14]. Furthermore, the bridge is useful for setting the overheat ratio (*OHR*) at a specific flow speed. *OHR* is the ratio of the probe's hot operating resistance to its cold resistance removed from the circuit. When the bridge is balanced,

$$\frac{R_w + R_L}{R_1} = \frac{R_3}{R_2}. \quad (2.1)$$

For operation, R_3 is adjusted to satisfy (2.1) for the desired R_w , as described by Brunn [12]. Then R_s is adjusted so that the galvanometer installed across the bridge measuring current reads zero, indicating that the bridge is balanced [12]. During calibration and velocity measurements, the current remains constant for each new velocity, but the bridge requires tedious rebalancing since R_w will change with velocity [12]. In order to reduce power consumption, the resistors R_2 and R_3 in the right side of the bridge are generally greater than R_L and R_w on the left, so that most of the current flows through the left arms [14].

A compensation network was first built by Dryden and Kuethe in 1929 [15] and increased the frequency response enough for the first quantitative velocity fluctuation measurements [16]. The add-on circuit contains an arrangement of resistors and capacitors (RC), coupled with an amplifier, to compensate for the time constant and increase the amplitude of high frequency signals that would otherwise be attenuated [17].

CCAs with frequency compensation are tedious to operate because the time constant is a function of the velocity and *OHR*, or probe resistance, so the RC circuit

must be re-tuned in a tedious process for a major change in operating conditions [17], [18]. It would not be possible to perform this careful tuning in flight on an autonomous device such as the BLDS. Furthermore, the circuit faces stability concerns from the Wheatstone bridge. An inductor L_C in series with the resistor R_3 , shown in Figure 2.1, must be carefully adjusted for balanced inductance across the bridge [18].

CCA technology is outdated and now only used for some temperature measurements [14] and supersonic flows at very low overheat ratios, where stability problems and non-linearities can occur in the amplifiers of CTA systems [19], [20]. More on frequency response and sensitivity is discussed in Chapter 3, following the explanations for each type of HWA.

CTA

Constant-temperature anemometers grew dominant over CCAs in the mid-1960s when low drift integrated circuit operational amplifiers became available [14]. For most applications, the CTA is far superior to the CCA, having a flat frequency response beyond 20 kHz and no risk of wire burnout. A diagram of a typical CTA circuit is displayed in Figure 2.2.

The hot-wire probe temperature, and therefore OHR , is kept constant using a closed-loop circuit controlled by a feedback amplifier. As probe temperature suddenly drifts up or down from a velocity fluctuation, the bridge becomes imbalanced. The imbalance voltage $e_2 - e_1$ is detected by the amplifier, which adjusts the current to rebalance the bridge and restore probe temperature [12]. This variable current operation compensates for the thermal lag of the wire because the probe is reheated or cooled nearly instantaneously in a velocity fluctuation; frequency response is only limited by the

performance of the amplifier and feedback circuit. The system's voltage output for velocity measurements is that from the feedback amplifier, which is proportional to the variable probe current.

Figure 2.2. Typical CTA circuit [12]

CTA systems require significant tuning for stable operation and accurate turbulence measurements. The overheat ratio is set with adjustable resistor R_3 in Figure 2.2, and the variable inductor L_c is used to balance bridge inductance, which is initially offset by the inductance of the cable connected to the hot-wire probe. Bridge oscillations will occur for unbalanced inductance, similar to CCA. Generally, bridge ratios (R_2/R_1) between 5-20 are used, depending on the application [12].

A square wave test is used to optimize CTA performance, as explained by Freymuth[21] and Perry [14]. This technique involves perturbing the bridge by injecting a square wave signal, which simulates heating of the probe as velocity instantaneously drops in a fluctuation, while observing the response of the anemometer's output on an oscilloscope. The response should match the waveform plotted in Figure 2.3 with about

15% undershoot [12] and an optimum damping ratio of 0.6 [14]. Achieving the ideal waveform shape requires careful adjustment of the amplifier offset voltage and the variable bridge inductor for cable inductance compensation [14], and these parameters depend on flow conditions. Properly tuning a portable CTA installed on the BLDS for operating over the wide range of flow velocity, temperature, and pressure seen in flight introduces a serious challenge.

Figure 2.3. Square wave response of properly tuned CTA [12]

CVA

The constant voltage anemometer (CVA) was patented by Sarma in 1990 [22] and its circuit diagram displayed in Figure 2.4. This anemometer uses an inverting operational amplifier circuit to maintain a constant voltage set-point at the node labeled V_w , across the hot-wire probe R_w and a small ($< 1 \Omega$), constant probe cable resistance R_L , connected between the feedback loop and ground. The constant voltage relationship between probe current and probe resistance in Ohm's law is therefore,

$$V_w = I_w(R_w + R_L), \quad (2.2)$$

or since $R_L \ll R_w$, more simply,

$$I_w \approx V_w / R_w. \quad (2.3)$$

For an increase in mean flow speed and greater heat loss via convection, the probe temperature and consequently, resistance R_w , will decrease. The op-amp will respond by outputting more current to compensate for the lower resistance in order to satisfy constant voltage operation. If the probe temperature and resistance increase from a drop in flow speed, the output current will decrease to cool the probe off, lessening the chance of a wire burnout. This action resembles CTA behavior and improves frequency response over CCA, but still below CTA. Either the op-amp output voltage or the current running through the probe can be used for measurement of flow velocities.

Figure 2.4. Simplified CVA circuit diagram

A key benefit with the CVA compared to the CCA and CTA is that the circuit does not use a bridge. This avoids the need for careful bridge and amplifier adjustments using a square wave test [22]. The only adjustment required for operation is the set-point voltage across the hot-wire probe. Unfortunately, this usually requires that the probe is submersed in a flow moving with a velocity near measurement conditions, also at a similar temperature and pressure. There is a large drop in probe temperature when air

begins to flow over the probe, because the primary mode of heat transfer shifts from natural convection to forced convection. If the voltage is increased for a safe *OHR* at no flow, the probe will likely not be sensitive enough to measure velocity fluctuations. The constant voltage parameter does not mean constant *OHR*, which is a major advantage of CTA operation.

The V_w set-point only depends on the values of fixed circuit resistors R_I and R_F , as well as the voltage source V_I , which is set to a constant value before collecting data. By performing Kirchoff's Current Law (KCL) at the node of the inverting input of the op-amp, the equation for wire voltage is derived as [19], [23]

$$V_w = \frac{R_F}{R_1} V_1. \quad (2.4)$$

The equation for V_s is derived from KCL at node V_w in an ideal op-amp analysis, resulting in the expression,

$$V_s = \left(1 + \frac{R_2}{R_F} + \frac{R_2}{R_w + R_L}\right) V_w. \quad (2.5)$$

This is the generally accepted formula for the CVA output, stated in references [20] and [23]. Monitoring the op-amp output voltage V_s as the measured output parameter may be preferable over the wire current I_w , since careful selection of R_2 and R_F can improve output resolution, as discussed by Sarma [19]. The variable V_s is used in (2.5) for agreement with literature, but it is actually twice the output of the CVA system described in later chapters, so

$$V_o = \frac{1}{2} V_s. \quad (2.6)$$

Notice that the only variable parameter during operation is R_w in (2.5), which will depend on the local mean velocity, temperature, and turbulence intensity. Frequency compensation is also possible for the CVA circuit by adding an RC compensation network, similar to that for CCA, inside the feedback loop, before the hot-wire sensor. Kegerise and Spina [20] show that with the use of a cascaded op-amp circuit, the frequency response can be increased past 100 kHz. Exact compensation is only possible for a particular combination of parameters specific to flow conditions, but software correction applied during post-processing is often used to allow for inexact compensation, as described in [24].

3. SELECTION OF HOT-WIRE ANEMOMETER FOR BLDS

Probe Heat Transfer

Henry King [25] proposed the heat transfer relationship for a heated cylinder in 1914. This is referred to as “King’s Law” and is commonly shown in the form [12], [26]

$$Nu = A' + B'Re_D^n, \quad (3.1)$$

which relates the Nusselt number $Nu = hD/k$ to the Reynolds number $Re = \rho UD/\mu$. The coefficients A' and B' are empirical constants. The value for the exponent n is 0.5 from King, and 0.45 from Collis and Williams [27]. From a simple heat balance on the wire,

$$I_w^2 R_w = \pi D L h (T_w - T_\infty), \quad (3.2)$$

and with a linear relationship between temperature and resistance,

$$T_w - T_\infty = \frac{R_w - R_\infty}{\alpha_0 R_0}. \quad (3.3)$$

The temperature coefficient of resistance α_0 ($^{\circ}\text{C}^{-1}$) at 20 $^{\circ}\text{C}$ reference temperature linearly relates temperature change to resistance change. From substitution of equations (3.1) and (3.3) into (3.2),

$$\frac{R_w I_w^2}{R_w - R_\infty} = A + B\sqrt{U}, \quad (3.4)$$

where A and B depend on probe geometry, probe material, and air properties. This is a common expression for the probe current and resistance as a function of velocity with end losses neglected.

Frequency Response

As previously explained, the problem of thermal lag exists because a finite time is required for the sensor temperature to reach its new value after a velocity fluctuation, and this duration is the thermal time constant. The amplitude ratio H is the relative amplitude

of the time varying signal output from the HWA. More specifically, H is the ratio of the attenuated output signal to the full amplitude signal without attenuation. It decreases for increasing frequency ω and time constant M , stated in Hinze [28] as

$$H(\omega) = (1 + \omega^2 M^2)^{-1/2} . \quad (3.5)$$

Each type of anemometer has a different time constant and therefore frequency response, because each circuit controls current through the probe differently.

The equation for the CCA time constant is [28]

$$M_{CCA} = \frac{\pi \rho_w c_w L D^2}{4 \alpha_0 R_0 I_w^2} \frac{(R_w - R_\infty)}{R_\infty} , \quad (3.6)$$

which is a function of the probe's cold resistance R_∞ , hot operating resistance R_w , probe current, and wire sensor properties. The CCA time constant is the longest of all 3 anemometers, acting as the baseline to which the other time constants are compared. A frequency response plot developed experimentally by Kidron [29] is shown in Figure 3.1.

Figure 3.1. Frequency response for uncompensated CCA [29]

The relative response is the amplitude ratio in units of decibels (dB), and the probe temperature, and therefore resistance, is the same for each velocity curve. Observe that the frequency response improves for increasing velocity.

The time constant for a CTA is a few orders of magnitude smaller than that for CCA, shown as [17]

$$M_{CTA} = \frac{M_{CCA}}{1 + 2G \frac{R_w - R_\infty}{R_\infty} \frac{R_w}{R_w + R_1} \frac{R_2}{R_2 + R_3}}, \quad (3.7)$$

where the amplifier gain G is around 500-1000. This smaller time constant allows a frequency response that extends flat to tens of kHz. Figure 3.2 is a frequency response plot for a standard commercial CTA. The phase lag is the phase shift between the ideal, full-amplitude signal and the attenuated signal, but phase angle information is not necessary for this project.

Figure 3.2. Frequency response for DANTEC 5510 CTA [12]

The predicted CVA frequency response plot is shown in Figure 3.3 for a commercial TSI 1210-T1.5 probe in steady velocities $U = 10, 30, \text{ and } 50 \text{ m/s}$. The amplitude ratio is calculated by substituting the time constants measured experimentally with a square wave test from Chapter 7 into equation (3.5).

Figure 3.3. Frequency response estimated from measured time constants using $3.8 \mu\text{m}$ dia., 1.27 mm long platinum-coated tungsten wire, $U = 10, 30, \text{ and } 50 \text{ m/s}$, $V_w = 0.67 \text{ V}$, at STP conditions

In comparing these frequency response plots to the estimated boundary layer instability frequencies in Figure 1.7 at a few hundred Hz and up, the uncompensated CCA response plot in Figure 3.1 by Kidron is low, with an amplitude ratio of only 0.25 at 500 Hz and 0.1 at 1000 Hz, both at 50 m/s. The CVA's frequency response is an improvement on the CCA's, with a 0.92 amplitude ratio at 500 Hz and 0.76 at 1000 Hz, also at 50 m/s. The CTA has a frequency that exceeds boundary layer stability requirements, with a flat response to between 20 and 50 kHz.

Theoretically, the CVA has a time constant 1/3 that of CCA with an *OHR* of 2, shown by

$$M_{CVA} = \frac{M_{CCA}}{1 + 2 \frac{R_w - R_\infty}{R_\infty}}. \quad (3.8)$$

Then, substitution of equation (3.6) into (3.8) yields

$$M_{CVA} = \frac{a_w}{1 + 2a_w} \frac{\pi \rho_w c_w L D^2}{4 \alpha_0 R_0 I_w^2} \quad (3.9)$$

and variable a_w is the ratio of wire's hot operating resistance to cold resistance subtracted by one.

$$a_w = \frac{R_w - R_\infty}{R_\infty} = OHR - 1 \quad (3.10)$$

Equation (3.9) can be written in terms of Reynolds number using equations (3.1)-(3.3) and (3.10) to arrive at (3.11) from Comte-Bellot and Sarma in [30], which is used by Comte-Bellot et. al in [31] for M_{CVA} calculation.

$$M_{CVA} = \frac{1 + a_w}{1 + 2a_w} \frac{\rho_w c_w D^2}{4 k_\infty} \frac{1}{A' + B' \left(\frac{\rho_\infty U D}{\mu_\infty} \right)^{1/2}} \quad (3.11)$$

Equation (3.11) reveals the influence of velocity, probe diameter, and a_w on M_{CVA} . The CVA time constant decreases with increasing velocity from $U^{1/2}$ the denominator. Operating with a higher *OHR* (or a_w) also decreases M_{CVA} from the $(1+a_w)/(1+2a_w)$ ratio. At a fixed V_w set-point, an increase in velocity will cause more heat transfer and reduce the *OHR* to increase M_{CVA} , but the $U^{1/2}$ velocity relationship dominates, resulting in an overall decrease for M_{CVA} . This is illustrated in Figure 3.3 because higher amplitude ratios exist for the curves at higher velocities and therefore lower time constants. Also observe in (3.11) that decreasing probe diameter would be a simple way to improve frequency response, because M_{CVA} is roughly proportional to $D^{3/2}$.

Sensitivity

Sensitivity is another consideration in selection of a hot-wire for BLDS because a relatively large change in voltage output for a given change in velocity is desirable to accurately measure velocity fluctuations. Comte-Bellot [17] summarizes the sensitivities for all three different HWAs as shown below. The sensitivity coefficients are related by the relationship,

$$\frac{e}{E} = S_u \frac{u}{U} + S_{\theta_\infty} \frac{\theta_\infty}{T_\infty}. \quad (3.12)$$

The velocity coefficient S_u quantifies the fluctuating voltage e about mean voltage output E , as a function of velocity fluctuation u about mean velocity U . Similarly, the temperature sensitivity coefficient S_{θ_∞} relates e/E to the fluctuating fluid temperature θ_∞ about mean ambient fluid temperature T_∞ . Since the fluctuating parameters are divided by their respective mean values in (3.12), S_u and S_{θ_∞} are dimensionless.

Figure 3.4. Sensitivity plots for CCA, CTA, and CVA, $a_w = OHR-1$ [17]

These plots in Figure 3.4 show that the sensitivity to velocity change increases with overheat ratio for CCA and CVA, but remains constant for CTA since the wire temperature is fixed. Another observation is that sensitivity to temperature change decreases with overheat ratio for CTA and CVA, while constant for CCA.

A relatively high sensitivity with respect to velocity is desired for a strong signal-to-noise ratio. Unfortunately, CVA's velocity sensitivity is lower than CCA or CTA. Equations (3.13), (3.14), and (3.15) show the theoretical velocity sensitivity relationships derived by Comte-Bellot [17]. These are derived from the ratio of the fluctuating voltage depending on whether resistance or current are variable ($e = I_w \delta R_w$ or $e = \delta I_w R_w$) to the mean voltage ($E = I_w R_w$), followed by multiplication of U/u . It is then necessary to evaluate the partial derivative with respect to flow speed, either $\partial R_w / \partial U$ or $\partial I_w / \partial U$, followed by substitution of King's law from (3.4). The sensitivity calculations in Table 3.1 use these equations:

$$S_u^{CCA} = \frac{1}{2} \frac{R_w - R_\infty}{R_\infty} \frac{B\sqrt{U}}{A + B\sqrt{U}} \quad (3.13)$$

$$S_u^{CTA} = \frac{1}{4} \frac{B\sqrt{U}}{A + B\sqrt{U}} \quad (3.14)$$

$$S_u^{CVA} = \frac{1}{2} \frac{R_w - R_\infty}{R_\infty} \frac{1}{1 + 2 \frac{R_w - R_\infty}{R_\infty}} \frac{B\sqrt{U}}{A + B\sqrt{U}} \quad (3.15)$$

Noise

In an analog measurement system such as the HWA, the major sources of noise are the resistors and the operational amplifier [32]. Noise levels greatly depend on system bandwidth, or frequency response, and the mean frequency [33]. This has made noise challenging to compare fairly across anemometers. Freymuth [33] presents a theoretical

analysis and provides a detailed explanation for each noise source in CCA and CTA. According to Sarma [23], the CVA is believed to have a larger signal-to-noise ratio than each of these other HWA systems. A major benefit for CVA is that it does not suffer from capacitance effects of the BNC probe cable [23]. Furthermore, stray induced voltages in the cable are minimized and there is no excess noise from a bridge. As a result, the CVA is less prone to electromagnetic and radio wave interference [23]. These guidelines from Freymuth [10] for CTA, but also applicable to CCA and CVA, were followed in an effort to achieve the best signal-to-noise ratio:

1. Operate at a high overheat to maximize S_u and minimize S_{θ_∞}
2. Use wire material with a high-temperature coefficient to increase HWA sensitivity to velocity fluctuations that will induce a change in probe temperature
3. Use a thin wire to minimize the thermal time constant

Finally, it is a good practice to use a low pass filter set to the maximum frequency of interest in order to eliminate high frequency noise.

Comparison Summary and Final Selection

Table 3.1 compares the key characteristics for each anemometer. The time constants were calculated from equations (3.6)-(3.8) using the thermal/electrical model described in Appendix B. The sensitivity was calculated from (3.13)-(3.15), with the dimensionless sensitivity coefficients multiplied by an assumed mean voltage, $E = 2.0 V$, and divided by the mean velocity, $U = 50 m/s$. A TSI 1210-T1.5 platinum-coated tungsten hot-wire probe with a 3.8 micron wire diameter, $OHR = 1.80$, and standard atmospheric conditions were also used in the calculations.

Table 3.1. Comparison summary between CCA, CTA, and CVA. Parameters quoted for 3.8 μm dia., platinum-coated tungsten hot-wire probe at STP conditions, $U = 50 \text{ m/s}$ and $OHR = 1.80$

	Time Constant (ms)	Sensitivity (mV/(m/s))	Half-Amp. (-3 dB) Cut-Off Frequency	Set-Up & Tuning
CCA	≈ 0.33	≈ 15	$\approx 800 \text{ Hz}$	Set Current Level, Tune Bridge
CVA	≈ 0.13	≈ 6	$\approx 2200 \text{ Hz}$	Set Voltage Level
CTA	< 0.05	≈ 9	$\approx 100 \text{ kHz}$	Set Overheat, Tune Bridge, Square Wave Test

The CVA was chosen for investigation as a suitable device for measuring velocity fluctuations on BLDS in order to distinguish between laminar and turbulent flows. The CVA time constant is about 1/3 that for CCA (0.13 ms vs. 0.33 ms), for a half-amplitude frequency of 2200 Hz. This is promising given that the frequencies of disturbances leading to transition in the boundary layer are expected to be a few-hundred Hz and up. Furthermore, operation of the CVA is very simple; the set-point voltage across the hot-wire probe is the only adjustable parameter, and such simplicity is a major benefit for autonomous operation on BLDS. The CVA does not require careful tuning of an inductor or amplifier using a square wave test procedure, and the lack of a Wheatstone bridge removes any oscillation concerns. These benefits outweigh the CVA's lower sensitivity.

Another benefit of CVA is that the circuit components are inexpensive (under \$100) and surface mountable. These components all fit onto a postage stamp sized printed circuit board, so there would not be any difficulties from physical size limitations when integrating a small-scale CVA with BLDS. The next chapter describes a CVA circuit that was built as a proof-of-concept for constant voltage operation.

4. CVA PROOF-OF-CONCEPT CIRCUIT

A simple inverting op-amp circuit was built in order to simulate the operation of a constant voltage anemometer and prove that the concept works before testing a full prototype. An inexpensive and low power LM358 operational amplifier was used because it was readily available on the Cal Poly campus. A convenient feature of the LM358 is that it only requires a single power supply for power, with one rail at +15 V and the other rail connected to ground. Consequently, this op-amp can only output a positive voltage, so a negative input voltage was applied prior to inversion. Figure 4.1 shows a photograph of the physical circuit, Figure 4.2 is the LM358's pin-out schematic, and the simplified circuit is drawn in Figure 4.3.

The output voltage, output current, and wire voltage were predicted from an ideal op-amp analysis for comparison to measured values. The ideal assumption meant that there was no current flow into the op-amp's inputs and that there was zero voltage potential across the inputs. From KCL at node V_w , the output voltage can be written as

$$V_s = \left(1 + \frac{R_2}{R_F} + \frac{R_2}{R_w}\right) V_w, \quad (4.1)$$

and from equating the source current to the current through R_F ,

$$V_w = \frac{R_F}{R_1} V_1. \quad (4.2)$$

Substitution of (4.2) into (4.1) yields

$$V_s = V_1 \left(\frac{R_F}{R_1} + \frac{R_2}{R_1} + \frac{R_F R_2}{R_1 R_w} \right). \quad (4.3)$$

Figure 4.1. CVA simulation circuit

Figure 4.2. LM358 pinout diagram

Figure 4.3. Inverting op-amp circuit used for CVA proof-of-concept

The output current was also calculated in order to select an input voltage that met the low output capability of the LM358. Ohm's law applied to the output voltage is written as

$$I_s = \frac{V_s}{R_T}, \quad (4.4)$$

where the total feedback resistance R_T is found from the expression,

$$R_T = \frac{R_F R_w}{R_F + R_w} + R_2. \quad (4.5)$$

A reasonable setting for the DC power supply acting as the source V_I , was estimated from equations (4.3)-(4.5). The LM358 data sheet [34] specifies this op-amp is capable of sourcing at least 20 mA. In order to satisfy this specification, 0.5 V for V_I was selected, resulting in a predicted output current of 21.5 to 6.4 mA over a 5 to 20 ohm potentiometer range. The circuit was tested with these parameters and maintained constant voltage very well, with less than 1 mV of variation. Table 4.1 compares the experimentally measured values to theoretical predictions for a V_w of 100 mV.

Table 4.1. Measured vs. predicted CVA circuit values for $V_I = 0.5$ V and $R_w = 5$ -20 Ω

R_w (Ω)	R_T (Ω)	Predicted V_s (mV)	Measured V_s (mV)	V_s % Diff	Predicted V_w (mV)	Measured V_w (mV)	V_w % Diff	I_s (mA)
5.0	57.0	1226	1276	4.0	100	99.78	-0.2	21.50
7.5	57.5	852	881	3.3	100	100.23	0.2	14.82
10.0	58.0	666	687	3.2	100	100.44	0.4	11.48
12.5	58.5	554	582	5.0	100	100.56	0.6	9.46
15.0	59.0	479	499	4.1	100	100.65	0.6	8.12
17.5	59.5	426	432	1.5	100	100.70	0.7	7.15
20.0	60.0	386	387	0.4	100	100.74	0.7	6.43

The maximum percent differences between theoretical and experimental voltages were 0.7% for V_w with $R_w = 20$ Ω and 4.0% at the output V_s , for $R_w = 5$ Ω . These results are reasonable since the LM358 does not operate under perfect conditions. For example,

0.0013 V was measured across the op-amp inputs; zero volts would be ideal, but this would require truly infinite input impedance. The voltage across the potentiometer, in this case V_w , is plotted over the 5-20 Ω range for R_w in Figure 4.4.

Figure 4.4. Voltage across 5-20 Ω variable potentiometer during CVA circuit simulation

This simple circuit shows that it is possible to maintain a nearly constant voltage across a resistor with one end connected to ground and the other connected to the feedback loop of an inverting operational amplifier. The actual constant voltage anemometers used in industry maintain a wire voltage of around $V_w = 0.4\text{-}0.9\text{ V}$, which would require a higher performing op-amp able to output more current than the LM358. The real CVA system tested for this project is introduced next.

5. CVA CALIBRATION

Prototype CVA System

A bench top CVA system was designed and built by Don Frame, electrical consultant for the BLDS group. A photograph of the CVA prototype system is shown in Figure 5.1. This model was built for stand-alone operation and initial testing of CVA capabilities, before potential implementation of a smaller model on BLDS.

Figure 5.1. CVA system built for Cal Poly BLDS

This system connects to a 5-10 ohm commercial hot-wire probe through a BNC cable jack, and is capable of maintaining 0.1-1.0 V across the wire. The V_w set-point is adjusted with the V_w -Set dial and monitored with a digital voltmeter (DVM) connected to the V_w -Out BNC jack. Either the V_o -Out or I_w -Out BNC output connections are used for data collection. The R_w -Out BNC jack is used to monitor the probe resistance during operation for OHR calculation. A square wave test feature is also included for visual estimation of the system time constant. Tuning with a square wave test as with CTA does not apply to CVA. There is also an adjustable gain on the output signal, but it was set to 1

for all experimental work in this project. An operating procedure is included in Appendix A.

One feature unique to this CVA system is a resistance limiter, which engages to help protect the hot-wire probe against burnouts. The resistance limit is set before operation, an ohm or so higher than the desired hot operating resistance. If probe heating occurs unexpectedly causing the resistance to exceed the preset threshold, the resistance limit will engage and partially drop the V_w set-point in an effort to cool the probe and prevent a burnout.

Output Behavior

The first step in testing the prototype CVA was calibration. A TSI 1128 pressure-velocity calibrator was used to provide a uniform, steady, jet flow of known velocity for establishing the relationship between CVA output and velocity—the calibration. The pressure-velocity calibrator with auxiliary equipment needed to perform calibration is displayed in Figures 5.2 and 5.3. An additional pressure-velocity calibrator was built in Cal Poly's machine shops through funding from Northrop Grumman Corp., and this model is described in Appendix C.

Figure 5.2. CVA calibration equipment set-up

Figure 5.3. Hot-wire probe positioned in laminar calibration flow

The velocity exiting the nozzle of the calibrator was calculated in accordance with the 1128 model manual [35]. Four equations relate the jet velocity to differential pressure, stagnation temperature, absolute pressure, and air properties. At stagnation conditions the speed of sound a_0 is calculated as

$$a_0 = [\gamma R(T_\infty + 273.15)]^{1/2} , \quad (5.1)$$

and the Mach number as

$$M_n = \left[2 \frac{\left(\frac{P + \Delta P}{P} \right)^{\gamma-1/\gamma} - 1}{\gamma - 1} \right]^{1/2} . \quad (5.2)$$

The speed of sound a is then calculated for non-stagnation conditions using

$$a = \left[\frac{a_o^2}{1 + \left(\frac{\gamma - 1}{2} M_n^2 \right)} \right]^{1/2}, \quad (5.3)$$

so the flow speed U of the laminar jet exiting the calibration nozzle is then

$$U = M_n * a. \quad (5.4)$$

Hot-wire probes were 3.8 micron diameter, platinum-coated tungsten, with a wire length of 1.27 mm and nominal 6 ohm resistance at 20 °C room temperature. (TSI 1210-T1.5) They were calibrated over an approximate $U = 5\text{-}54 \text{ m/s}$ velocity range, as 50 m/s (110 MPH) is the maximum speed for the 2-ft by 2-ft Cal Poly M.E. wind tunnel. Initially, a probe was calibrated using four different wire voltage V_w set-points, resulting in four different values of OHR at a given flow velocity: $V_w = 0.37, 0.56, 0.69, 0.83 \text{ V}$ and $OHR = 1.2, 1.4, 1.6, 1.8$ for $U = 50 \text{ m/s}$. Hot-wire probes of the same model vary slightly in resistance by $\pm 0.25 \Omega$, and consequently, the necessary V_w set-point for the same OHR may vary up to about $\pm 0.05 \text{ V}$. The op-amp output voltage V_o is plotted as a function of calibration jet velocity U in Figure 5.4. Observe that for increasing V_w set-point, the output voltage curve for each V_w grows steeper at a given velocity; a higher temperature probe is more sensitive, corresponding to a greater change in V_o for a given change in velocity. For example, Figure 5.4 shows that from $U = 20\text{-}50 \text{ m/s}$, $\Delta V_o \approx 0.25 \text{ V}$ at $V_w = 0.83 \text{ V}$, compared to $\Delta V_o \approx 0.10 \text{ V}$ at $V_w = 0.37 \text{ V}$. A similar trend occurs between V_w and OHR , as illustrated in Figure 5.5. There is a greater change in OHR at higher V_w compared to lower V_w over the same velocity range.

Figure 5.4. CVA calibration at four different V_w set-points, sensitivity increases for higher V_w and decreases for higher U

Figure 5.5. OHR increases from higher V_w and decreases with higher U

Notice that the $V_w = 0.83\text{ V}$ data set starts at about $U = 20\text{ m/s}$, instead of $U = 10\text{ m/s}$. This higher starting velocity was necessary to keep the OHR below the chosen threshold of 2.0. TSI specifies a maximum operating temperature of $300\text{ }^\circ\text{C}$ [36] for the TSI 1210-T1.5 probe, corresponding to an OHR closer to 2.2, but a lower limit 2.0 was chosen to provide a margin of safety against wire burnout.

Lead Resistances

The lead resistances in-line with the hot-wire probe are represented by R_L in Figure 2.4, and were accounted for in the calculations of OHR . These resistance contributions account for circuit elements between the CVA resistance measurement and the probe's actual sensor element, and are tabulated in Table 5.1.

Table 5.1. Resistances between CVA and actual wire sensor of hot-wire probe

Item in CVA Circuit	Resistance (Ω)
BNC Probe Cable, R_{cable}	0.11
Single Probe Support Stem, $R_{support}$	0.02
Internal Probe Resistance, R_{int}	0.16-0.17
CVA "Box" Resistance, R_{box}	0.1

The internal, or "box" resistance of the CVA system was estimated as 0.1 ohms. This value was determined by measuring the resistance of a nominal 10-ohm resistor with a DVM, and then subtracting this reading from the "cold" resistance, measured with the CVA below $V_w = 0.1\text{ V}$. It was important to minimize resistor heating as much as possible, otherwise the CVA measured value would not be the same as the DVM-measured value.

Equations (5.5) and (5.6) were used for calculating the OHR as plotted in Figure 5.5:

$$OHR = \frac{R_w}{R_\infty} = \frac{(R_w \text{ Out}(V) * 5 \Omega/V) - R_L - R_{box}}{R_{DVM} - R_{lead}} \quad (5.5)$$

and

$$R_L = R_{cable} + R_{support} + R_{int} \quad (5.6)$$

Initial measurement of the hot-wire probe's cold resistance R_{DVM} , required caution because the current supplied by the DVM could potentially burnout the probe. Even though the short-circuit current is relatively small, cold probe resistance is measured at no-flow conditions, where natural convection is the main mode of heat transfer; natural convection provides far less cooling than forced convection. The short-circuit current was measured to be 0.3 mA for a Fluke model 87, and 1 mA for a Fluke model 289. Both of these DVMs were safe for measuring the cold probe resistance. The current of 1 mA would heat the probe only by about 6 μ W assuming no R_w variation from heating, since probes have a nominal 6 ohm cold resistance and $P_w = I_w^2 R_w$.

CVA Output Behavior: Comparison to Prediction

The CVA op-amp output voltage V_o and the voltage output proportional to probe current I_w were predicted using the thermal/electrical CVA model explained in Appendix B. The predicted and measured data sets for V_o and I_w at $V_w = 0.69 \text{ V}$ ($OHR = 1.6$ at $U = 50 \text{ m/s}$), are compared in Figures 5.6 and 5.7. This OHR was selected for use with velocity fluctuation measurements because it allows for safe operation, that is, $OHR < 2$, for flow velocities down to 5 m/s. The predicted CVA V_o and I_w outputs undershoot the true behavior by about 10% for the same V_w set-point, and there are a number of contributing factors for this difference. One is that the relationship between circuit parameters in the CVA system were treated with a simplified analysis based on the diagram in Figure 2.4. Second, the Collis and Williams correlation was used to predict

the convection coefficient for the probe, but this correlation does not account for heat loss to the prongs supporting the wire. The heated wires used by Collis and Williams were also physically different; the smallest wire used in their experiments was 0.0295 mm in diameter and 87 mm long, and made from silver-plated platinum. However, the 1210-T1.5 probes used with the CVA for this project were 3.8 μm in diameter, 1.27 mm long, and made from platinum-plated tungsten wire; heat transfer behavior will vary to some degree for different physical probe properties.

Elger and Adams [37] performed a dynamic hot-wire probe calibration using a piston to produce oscillating flow, and their experimentally measured Nusselt numbers were 10% above the prediction from Collis and Williams. This meant greater convective heat transfer was measured experimentally compared to prediction. The CVA model was also low in heat transfer; operating probe resistances were about 10% higher, and therefore hotter, than those actually measured at a given flow speed and V_w set-point.

In an effort to reduce error in the prediction model, lead resistance effects were accounted for. This means that the heat transfer analysis was performed only on the wire sensor, while the calculation of V_o and I_w with the circuit parameter relationships in (2.5) and (2.6) used the total resistance between the CVA R_w -*Out* jack and the actual wire sensor.

Figure 5.6. Comparison between measured and predicted CVA op-amp output at $V_w = 0.69$ V (OHR = 1.6 at $U = 50$ m/s)

Figure 5.7. Comparison between measured and predicted CVA probe current at $V_w = 0.69$ V (OHR = 1.6 at $U = 50$ m/s)

Calibration Curves

Calibration plots of the op-amp voltage V_o or hot-wire probe current I_w vs. jet velocity U show the increased probe sensitivity for higher V_w set-points, but the inverted form is used to determine mean velocity from mean voltage output and convert the fluctuating CVA output to a fluctuating velocity. Figures 5.8 and 5.10 show the inverted calibration curves, U vs. V_o and U vs. I_w , respectively, with a power-law curve fit in the form,

$$U = P + QE^k . \quad (5.7)$$

The variable E is the generalized mean output voltage, either from the op-amp or proportional to the current through the probe. Linearized plots for V_o and I_w have shown that a power law fit is appropriate: the linear relationships between U and V_o as well as U and I_w are illustrated in Figures 5.9 and 5.11, respectively. MATLAB was used to curve fit the data, and it was noticed after repeated curve fits that the exponent n can slightly vary for the same set of data. This is likely due to the robustness of MATLAB's power-law curve fit. The power-law form proved to be a good match to the experimental calibration data, from both visual inspection, and also by the low mean square error (MSE). The MSE is the average squared difference between the true measured value and the approximated value calculated by the curve fit. The MSE values for the V_o and I_w curves are 4.3E-3 and 8.1E-3, respectively, corresponding to a percent error of 3.3% and 5.5% for the mean voltage outputs at $U = 30$ m/s.

$$MSE = \frac{1}{N} \sum (y_{fit} - y_{meas})^2 \quad (5.8)$$

Figure 5.8. Inverted calibration curve for op-amp output voltage with power-law fit

Figure 5.9. Linearized calibration curve for op-amp output voltage

Figure 5.10. Inverted calibration curve for voltage output proportional to current with power law fit

Figure 5.11. Linearized calibration curve for probe current output voltage

The first measurements of velocity fluctuations were performed using the CVA paired with a Fluke 289 true-RMS digital multimeter. Transforming the velocity fluctuation data in RMS mV to RMS velocity was done assuming linear local sensitivity. Local meant that linear sensitivity was determined at the mean output voltage on the calibration curve. This involved recording the mean and fluctuating CVA output signals in V and mV RMS, respectively, and then using the derivative of the power-law calibration curve equation, such as that in Figure 5.8, to transform mV RMS to velocity RMS. The partial derivative of the generalized power-law curve fit from equation (5.7) results in

$$\frac{dU}{dE} = kQE^{k-1} . \quad (5.9)$$

Equation (5.9) expresses the local sensitivity in units of velocity per volt for a steady flow, as a function of the general output voltage E , and the curve fit coefficients Q and k . Either the mean V_o voltage or the mean voltage proportional to I_w was measured at the same physical hot-wire probe location as the RMS measurement. This mean value was used for the output E in (5.9). Then the local sensitivity was multiplied by the corresponding mV RMS measurement e_{rms} to yield the velocity RMS measurement u_{rms} , following the expression,

$$u_{rms} = \frac{dU}{dE} e_{rms} . \quad (5.10)$$

Using a steady flow calibration to convert voltage fluctuations to velocity fluctuations is not entirely accurate for two reasons. This is because the fluctuating components of velocity u' and voltage e' in a turbulent flow do not exist in a steady flow, and the calibration curve in (5.7) does not account for any fluctuating parameters. Linear

sensitivity with respect to mean output voltage (and specific velocity) along the calibration curve is also an approximation, because the calibration curve in Figure 5.8 is a power-law relationship, not a linear one.

The influence of temperature variation between CVA calibration and laboratory testing was necessary to consider. The ambient temperature was observed to vary up to 2 °C inside the lab, potentially affecting the CVA output behavior. The CVA theoretical model from Appendix B. was used to predict less than a 0.1% change in the CVA V_o and I_w output over the 5-54 m/s velocity range for a temperature change of +/- 5 °C. Therefore, temperature correction of the calibration curves was unnecessary. For real flight testing at high altitudes, the much colder ambient temperatures will significantly effect CVA behavior, and this is discussed further in Chapter 8.

Linear Local Sensitivity Assumption

Consider the familiar hot-wire anemometry power law, also applicable to CVA,

$$e = A + Bu^n, \quad (5.11)$$

where e and u are the instantaneous output voltage and velocity. If Reynolds decomposition is performed such that $e = \bar{e} + e'$ and $u = \bar{u} + u'$, then for the linear case with $n = 1$,

$$\overline{\bar{e} + e'} = \bar{A} + \overline{B(\bar{u} + u')}, \quad (5.12)$$

and time averaging performed,

$$\bar{e} = A + B\bar{u}. \quad (5.13)$$

Equation (5.13) shows that the mean voltage in a turbulent flow is equivalent to that in a laminar flow, when $n = 1$. To solve for the relationship between the fluctuating components, square both sides of (5.11) with u and e decomposed as before.

$$\overline{(\bar{e} + e')^2} = \overline{(A + B(\bar{u} + u'))^2} \quad (5.14)$$

Algebraic manipulation and substitution of $\bar{e}$ from (5.13) results in,

$$\sqrt{\overline{e'^2}} = B\sqrt{\overline{u'^2}}. \quad (5.15)$$

Equation (5.15) resembles the derivative of the inverted steady flow calibration curve in (5.10), showing that sensitivity is in fact linear when $n = 1$, since B is a numerical constant. However, $n \neq 1$, because the calibration data has curvature and is well represented by a power-law trend. For example, the curve fit to the output data for V_o vs. U in Figure 5.6 uses $n = 0.16$. Time averaging will now be performed on (5.11) for $n \neq 1$, with use of the binomial theorem for expansion of the $(\bar{u} + u')^n$ term. Starting again with decomposition and time averaging,

$$\overline{\bar{e} + e'} = \bar{A} + \overline{B(\bar{u} + u')^n}, \quad (5.16)$$

and then a binomial expansion

$$(\bar{u} + u')^n = \bar{u}^n + n\bar{u}^{n-1}u' + \frac{n(n-1)}{2!}\bar{u}^{n-2}u'^2 + \frac{n(n-1)(n-2)}{3!}\bar{u}^{n-3}u'^3 \dots \quad (5.17)$$

The 1st order and 3rd order fluctuating components (u' and u'^3) equal zero when averaged. The 2nd order term (u'^2) remains, and the 4th order term will be neglected as it is relatively small, so that

$$\overline{(\bar{u} + u')^n} \approx \frac{n(n-1)}{2}\bar{u}^{n-1}\overline{u'^2} = \frac{n(n-1)}{2}\bar{u}^{n-1}\overline{u'^2}, \quad (5.18)$$

and

$$\bar{e} = A + B\left(\bar{u}^n + \frac{n(n-1)}{2}\bar{u}^{n-2}\overline{u'^2}\right). \quad (5.19)$$

Since $n < 1$, $\bar{e}$ is actually smaller for turbulent flow than steady flow. To investigate the relationship between $\sqrt{\overline{e'^2}}$ and $\sqrt{\overline{u'^2}}$,

$$\overline{(\bar{e} + e')^2} = \overline{(A + B(\bar{u} + u')^n)^2}, \quad (5.20)$$

and then

$$\bar{e}^2 + \overline{e'^2} = A^2 + 2AB(\bar{u} + u')^n + B(\bar{u} + u')^{2n}. \quad (5.21)$$

Applying the binomial theorem to $(\bar{u} + u')^n$ and $(\bar{u} + u')^{2n}$, with substitution of $\bar{e}$ from (5.19) finally yields

$$\sqrt{\overline{e'^2}} = Bn\bar{u}^{n-1}\sqrt{\overline{u'^2}}. \quad (5.22)$$

Equation (5.22) is an inverted form of (5.10), but shows a similar relationship between $\sqrt{\overline{u'^2}}$ and $\sqrt{\overline{e'^2}}$. The key observation from this time-averaging analysis of the relationship between unsteady velocity and output voltage is that the mean voltage output is lower in a turbulent flow, than with a steady flow, shown by the additional $\overline{u'^2}$ term in (5.19). Therefore, slight error is introduced from the application of a steady calibration curve to non-steady turbulent flow data.

For the CVA system, the approximation of equivalent mean output voltage between steady flow non-steady flows is estimated to under-predict the mean velocity $\bar{u}$, and the fluctuating velocity $\sqrt{\overline{u'^2}}$, both by about 0.5% for a flow with a turbulence intensity of 10%. Lower velocities are determined because the reduced mean output voltage measured in a non-steady flow corresponds to a lower point on the steady inverted calibration curve, for a lower $\bar{u}$ and a smaller slope dU/dE . The 0.5% estimate is based on calculating equations (5.11), (5.19), and (5.22) using the power law curve fit coefficients from Figure 5.6, applied to measured voltage data at 10% turbulence intensity.

The error introduced by the linear assumption in (5.10), and the mean output voltage approximation discussed in the previous paragraph is circumvented with use of a

digital data acquisition system (DAQ) to collect velocity fluctuation data. The DAQ records the instantaneous voltage output from the HWA. Then the steady velocity calibration curve in the form of (5.7) is used for conversion from instantaneous voltage e to instantaneous velocity u . No RMS voltage readings are recorded, and (5.10) is not needed. Dynamic calibrations for HWA systems have been used to avoid the error associated with calibration in a laminar flow. Brunn [12] credits Perry and Morrison [38] for the development of a dynamic calibration procedure, where the hot-wire probe is vibrated at low frequencies in a steady flow. This vibration subjects the probe to known velocity fluctuations over a controlled velocity range. This allows the dynamic relationship between $\sqrt{e'^2}$ and $\sqrt{u'^2}$ to be measured directly, for development of a dynamic calibration curve. Perry and Morrison predicted up to 20% error in local sensitivity from static calibration. Brunn states this is due to poorly correlated static calibration data and that a variety of more recent work since Perry and Morrison's has shown good agreement between static and dynamic calibrations.

6. MEASUREMENT OF VELOCITY FLUCTUATIONS

Turbulent Jet

The prototype CVA system was initially tested along the centerline of a turbulent jet above the 10 mm diameter nozzle of 1128 pressure-velocity calibrator, as shown by Figures 6.1 and 6.2. This was a simple way to experiment with different V_w set-points and OHR s before testing in the wind-tunnel requiring a more involved set-up. A higher V_w meant a higher OHR , providing better sensitivity, but a lower V_w provided better protection against hot-wire probe burnout from a sudden drop in air velocity; it was necessary to select a V_w with the best balance of these two performance attributes. Downstream of the potential core, the turbulent nozzle jet was expected to contain velocity fluctuations in a broadband frequency range, and it proved very useful during initial testing.

Figure 6.1. Turbulence intensities were measured 0-5 in. above 10 mm dia. nozzle

Figure 6.2. Local mean velocity in jet centerline decreases for increasing height

The velocity fluctuations on the centerline of the jet were measured between 0 and 5 in. ($x/D = 0-12.7$) from the nozzle outlet with a jet exit velocity $U = 50 \text{ m/s}$. The

fluctuations in mV RMS from the op-amp output voltage V_o are plotted in Figure 6.3, and the corresponding turbulence intensities are plotted in Figure 6.4. Turbulence intensity is the percent ratio of the RMS velocity component to the mean local velocity, and this distinction is important because the local mean velocity decreased from 50 m/s to 22 m/s over 0-5 in. above the nozzle in the jet centerline. The fluctuating V_o signal in mV was transformed to m/s using the derivative of the inverted calibration curve dU/dV_o , assuming linear local sensitivity as outlined in Chapter 5.

The turbulence intensities reached 17% in the jet centerline, 5 in. away from the 10 mm diameter nozzle outlet ($x/D = 12.7$), as shown by Figure 6.4. These intensities are comparable to the results from the Wygnanski and Fiedler's [39] turbulent jet experiment, displayed in Figure 6.5. They measured 24% turbulence intensity at the distance of $x/D = 20$ along the jet centerline, $y/x = 0$. Self-similarity for the turbulent jet does not occur until $x/D = 50$; this is when the turbulence intensity is expected to be the same for different x/D ratios. Their measured turbulence level is 7% higher than that measured by the CVA, but they were 7.6 x/D s farther away than the hot-wire probe connected to the CVA at 5 in. above the nozzle; the turbulence intensity would be expected to increase for larger x/D . Downstream of the potential core, the turbulent jet is expected to have velocity fluctuations with a broadband frequency range, so the CVA output is likely suffering from attenuation past 700 Hz or so, behaving similar to a low-pass filter. Therefore, the CVA measured intensity is probably lower than the actual turbulence intensity.

Figure 6.3. RMS voltage measured from $x/D = 0$ -12.7 above 10 mm dia. nozzle along jet centerline with 50 m/s jet exit velocity

Figure 6.4. Turbulence measured up to $x/D = 12.7$ above 10 mm dia. nozzle along jet centerline with 50 m/s jet exit velocity

Figure 6.5. Turbulence intensities measured in a circular turbulent jet [39]

Notice that even though the V_w set-point had little effect on the measured turbulence intensity, as shown by the overlap in Figure 6.4, the amplitude of the fluctuating V_o signal strongly depends on V_w . Higher V_w set-points provide a hotter probe that is more sensitive to velocity fluctuations, resulting in higher amplitude fluctuating output signals from the CVA. The lower V_w settings are less sensitive, but have larger values for dU/dE from their respective calibration curve, that compensate for the smaller fluctuating output signal. After trial and error with four different V_w settings in the turbulent nozzle jet at different heights, a V_w value of 0.69 V ($OHR = 1.6$ at $U = 50$ m/s) provided a sufficiently high output signal from the CVA, and allowed operation over the full $U = 10$ -50 m/s velocity range. At 10 m/s, the OHR was about 1.9, comfortably below the 2.0 limit. In comparison, the higher V_w set-point of 0.83 V would provide CVA output signals larger in amplitude, but with a lower velocity limit of 20 m/s to maintain $OHR < 2.0$, as shown in Figure 5.5.

The jet of the pressure-velocity calibrator was a convenient way to practice CVA operation and evaluate CVA measurements of velocity fluctuations. The next CVA test was to measure the velocity fluctuations in laminar and turbulent boundary layers.

Laminar and Turbulent Boundary Layers

The CVA was tested in the Cal Poly Mechanical Engineering Fluids Lab using the 2-ft by 2-ft wind tunnel with a maximum velocity of 110 MPH, or 50 m/s. Measurements were obtained for two cases: a fully laminar flat plate, and a tripped-turbulent flat plate flow case at $y = 1 \text{ mm}$ from the surface, with freestream velocities $U = 11\text{-}46 \text{ m/s}$. The freestream velocities were measured with a Pitot-static probe, and this differential pressure was measured with a Setra 239 digital pressure transducer. The CVA was set to $V_w = 0.69 V$ to run a TSI 1210-T1.5 probe with a 6Ω cold resistance at room temperature. A line-diagram of the wind tunnel set-up is displayed in Figure 6.6.

Figure 6.6. Laminar and turbulent flow testing configuration in 2-ft by 2-ft wind tunnel

Laminar flow conditions were achieved by installing a cardboard flap on the end of the 36 inch long flat plate and by also adding a very slight downward angle-of-attack with a few washers installed under the plate's back vertical supports. Both of these adjustments to the flat plate eliminated flow separation at the leading edge, which would have otherwise resulted in turbulent flow development after reattachment. The location of the hot-wire probe remained constant for both the laminar flow and turbulent flow cases, at $x = 20 \text{ in.}$ downstream of the leading edge, and $y = 1 \text{ mm}$ vertically above the plate surface.

The laminar flow was tripped at $x = 13.5 \text{ in.}$ to obtain fully turbulent flow downstream of the trip. For both flow conditions, the wind tunnel fan drive frequency was increased from 15-60 Hz, in increments of 5 Hz, resulting in $U = 11\text{-}46 \text{ m/s}$ measured by the reference Pitot-static probe. At each interval, the mean (V) and fluctuating components (mV RMS) for both the op-amp output voltage V_o and the voltage proportional to current I_w were recorded. The mean voltage readings were converted to velocities in m/s using the inverted calibration curves in Figure 5.8 for V_o and Figure 5.10 for I_w . The velocity fluctuation data, in mV RMS, was converted to m/s by multiplying the raw data by the derivative of the inverted calibration curve dU/dE at the measured mean output voltage, using the linear local sensitivity approximation as described in Chapter 5.

Figure 6.7 compares the turbulence intensities for laminar and turbulent flow cases. Turbulence intensity is defined as the ratio of the RMS velocity fluctuation to the mean local velocity, both measured by the CVA, and is expressed by a percentage value

in Figure 6.7. The measurements using both the V_o and I_w outputs are nearly identical, confirming that either output can be used for measuring velocity fluctuations.

Figure 6.7. Turbulence intensities measured by CVA using V_o and I_w outputs with $V_w = 0.69 V$ over $U = 11\text{--}46$ m/s, probe wire located at $x = 20$ in. and $y = 1$ mm

The turbulence intensity has a downward trend for increasing freestream velocity. At $U = 11$ m/s, the turbulence intensity is 12.5%, but the CVA only measures 6% for 46 m/s. This decrease in measured turbulence is expected for three reasons. First, the frequencies of the disturbances inside the boundary layer are predicted to grow with increasing U . Even though the CVA time constant decreases with increasing U , as previously shown in Figure 3.3 and equation (3.11), this beneficial relationship probably does not improve the frequency response enough to compensate for the higher boundary layer disturbance frequencies associated with faster freestream velocities. Second, the

turbulent boundary layer thickness decreases for increasing U , shown by the very rough correlation for the boundary layer thickness of a turbulent flow [3],

$$\frac{\delta}{x} = 0.37 Re_x^{-1/5} . \quad (6.1)$$

Hence, the relative position of the hot-wire probe within the boundary layer increases in height and grows closer to the freestream, so that the probe position is no longer within the region of peak fluctuations. The third reason for the apparent decrease in turbulence is from the fact that the skin friction scales linearly with the Reynolds stresses, and skin friction decreases with Reynolds number. For Prandtl's skin friction correlation for turbulent flow over a flat plate [3],

$$C_f \approx 0.058 Re_x^{-1/5} , \quad (6.2)$$

and then with skin friction scaling linearly to the Reynolds stress,

$$\frac{\overline{u_1'^2}}{\overline{u_0'^2}} \approx \frac{C_{f1}}{C_{f0}} \approx \left(\frac{Re_{x0}}{Re_{x1}} \right)^{1/5} , \quad (6.3)$$

so finally,

$$\frac{\sqrt{\overline{u_1'^2}}}{\sqrt{\overline{u_0'^2}}} = \left(\frac{\bar{u}_1}{\bar{u}_0} \right)^{-1/10} . \quad (6.4)$$

The predicted decrease in turbulence intensity from the drop in Reynolds stresses is drawn in the plot of Figure 6.7. This curve does not reach the lower turbulence intensities measured by the CVA, and this difference is probably a combination of the first two mentioned reasons; a decrease in boundary layer thickness for a higher probe height relative to the boundary layer, and an increase in disturbance frequencies induced by the growing freestream velocity.

Figure 6.8. Velocity fluctuations measured in the flat plate turbulent boundary layer for the streamwise u' , normal v' , lateral w' , and shear $u'v'$ directions [40]

The turbulence intensities measured by Klebanoff [40] for the streamwise u' , normal v' , and lateral w' components throughout a turbulent boundary layer are displayed in Figure 6.8. The CVA measured a 10.2% turbulence intensity at $y = 1 \text{ mm}$ from the plate surface for the 14.7 m/s freestream velocity, shown by Figure 6.7, with mean local velocity $\bar{u}$ of 10.7 m/s, so $\bar{u}/U_\infty = 0.73$. The y/δ ratio is estimated either with a hand calculation using equation (6.1) with $y = 1 \text{ mm}$, or by following the $\bar{u}/U_\infty$ line in Figure 6.8, starting on right at $\bar{u}/U_\infty = 1.0$, and moving toward the left until the $\bar{u}/U_\infty$ line crosses the horizontal value $\bar{u}/U_\infty = 0.73$. Both methods yield $y/\delta \approx 0.18$, for a streamwise turbulence intensity of $\sqrt{u'^2}/U_\infty \approx 6.6\%$, normal component of $\sqrt{v'^2}/U_\infty \approx 4\%$, and shear $\sqrt{u'v'}/U_\infty \approx 0.13\%$. There is no lateral component $\sqrt{w'^2}$ because the wire of the probe was parallel to the flat plate, and probe sensitivity to cross-flow fluctuations in the lateral direction is negligible. The total root-sum-squared of the streamwise, normal, and shear

components is 7.7%, which is 2.5% intensity lower than that measured by the CVA. Also consider the data point with a CVA measured turbulence of 6.5% for $U = 42.5 \text{ m/s}$ and $\bar{u} = 33.0 \text{ m/s}$. Then $y/\delta \approx 0.22$, corresponding to a Klebanoff reported turbulence magnitude of 7.4%, which is 0.9% above that measured by the CVA. The CVA's lower value is probably due its limited frequency response. However, these comparisons for measured turbulence at $U = 14.7 \text{ m/s}$ and 42.5 m/s are certainly reasonable considering that Klebanoff's data was obtained for higher Reynolds numbers ($Re_x \approx 10^7$); those reached for this CVA tripped flow test at $x = 13.5 \text{ in.}$ are $Re_x \approx 10^5 - 10^6$. There would be a concern if Klebanoff's results predicted turbulence far above that measured by the CVA, because this would imply that frequency response and sensitivity were severely limiting the CVA's ability to measure velocity fluctuations in the boundary layer.

There was a surprising turbulence intensity of up to 2.5% measured in the laminar boundary layer. A stethoscope was used to check for laminar flow conditions beforehand, and the flow was very quiet. The most plausible explanation for this amount of turbulence is that some uncontrolled disturbances interrupted the laminar flat plate environment. The disturbance level in the freestream was also measured with the CVA out of the boundary layer at $y = 30 \text{ mm}$ from the flat plate surface. As shown in Figure 6.9, the turbulence intensity reached a maximum of 0.09% for $U = 42.5 \text{ m/s}$. This very small intensity is attributed to a combination of noise from tiny disturbances in the wind tunnel freestream, as well as some slight noise contributed by the CVA circuitry connected to the hot-wire probe. The CVA output was not passed through a low-pass filter, which may reduce noise effects. The freestream disturbance level is much lower than that in the laminar boundary layer, indicating that the 2.5% turbulence intensity in

the laminar boundary layer was probably caused by imperfect laminar conditions, instead of noise or vibration of the hot-wire probe.

It is worth mentioning that a slight high-pass filter effect was applied to the freestream turbulence data because a DVM was used to measure the fluctuating CVA output in mV RMS. A DVM processes the RMS of a signal over a certain time duration, for accurate readings down to a specified frequency, in this case 4 Hz for the Fluke 289 model used. Lower frequencies are not accurately captured by the DVM because the RMS time interval is insufficient for measuring very low frequency disturbances with long periods. This detail mainly applies to freestream measurements; higher frequency velocity fluctuations in the boundary layer dominate well over the low frequency disturbances measurable in the freestream.

Figure 6.9. Freestream turbulence intensity measured by CVA at $y = 30$ mm and $x = 20$ in. using V_o and I_w outputs with $V_w = 0.69$ V over $U = 11$ - 46 m/s

The mean velocities measured with the CVA are plotted against the freestream velocity measured with a Pitot-static probe in Figure 6.10. This plot illustrates how the local velocities in the laminar and turbulent boundary layers change for increasing freestream velocity, and also serves as an experimental check that a small variation between the stagnation temperature during calibration and that measured during wind tunnel testing does not contribute significant error to the CVA test data. The stagnation temperature during calibration for this hot-wire probe at $V_w = 0.69 V$ was 20.3 °C, which is a maximum of 1.5 °C below the stagnation temperature during these wind tunnel tests. The drawn line has a slope of 1, showing the perfectly linear relationship if the freestream velocity measured by the hot-wire matched that measured using the Pitot-static probe. Since the CVA freestream data follows this one-to-one linear trend very well, the 1.5 °C temperature variation was not a concern. This result agrees with the theoretical model prediction, which has shown that a 5 °C difference introduces insignificant error.

Figure 6.10. Comparison between mean local velocities measured with CVA and freestream velocity measured with both the CVA and Pitot-static probe, $V_w = 0.69 V$ over $U = 11-46 m/s$

Also notice in Figure 6.10 that the laminar and turbulent local velocity data intersect around $U = 32 m/s$. This is because a laminar boundary layer decreases in thickness faster than a turbulent boundary layer, for increasing freestream velocity and Reynolds number. Compare the laminar flat plate boundary layer approximate correlation in equation (6.5) to the turbulent correlation in (6.1). The Reynolds number raised to the negative 1/2 power in (6.5) drives the boundary layer thickness δ , down faster than the Reynolds number to the -1/5 power.

$$\frac{\delta}{x} = 5.0 Re_x^{-1/2} \quad (6.5)$$

The CVA has successfully distinguished between laminar and turbulent flows at a height of $y = 1 mm$ for $U = 11-46 m/s$. For the laminar boundary layer test, a turbulence intensity of 2.5% was measured, probably due to uncontrolled disturbances, because a

freestream test at $y = 30 \text{ mm}$ revealed a maximum noise intensity of 0.09%. In the turbulent boundary layer, the turbulence intensity started at 12.5% and decreased to 6%, for $U = 11\text{-}46 \text{ m/s}$. The CVA also measured the 0-17% turbulent intensity in the centerline of a turbulent jet 0 to 5 in. ($x/D = 12.7$) above a 10 mm diameter nozzle, with a jet exit velocity of 50 m/s. The amplitudes of velocity fluctuations measured by the CVA in both the boundary layer and turbulent jet were realistic compared to published data.

7. EXPERIMENTAL INVESTIGATION OF CVA FREQUENCY RESPONSE

Controlled Vibration in Steady Flow

Figure 7.1. Experimental set-up for hot-wire probe vibration of hot-wire probe in steady flow jet

The frequency response for the CVA has been predicted from heat transfer theory and compensated systems have been tested experimentally by Kegerise and Spina [41], but there has been little experimental validation for uncompensated systems. This would confirm the predicted amplitude ratio between the true amplitude of the velocity fluctuations and that for the measured output signal. Previous experiments to measure the frequency response of hot-wire anemometers have usually used either electrical (square wave) or thermal methods. Kidron [29] exposed a hot-wire probe to electromagnetic radiation at controlled frequencies to develop the CCA frequency response plot in Figure 3.1, and Kegerise and Spina [41] used laser-based heating for CTA and CVA. Direct heating and cooling simulates probe response in a velocity fluctuation. The frequency response of the CVA was investigated by vibrating the probe on a low amplitude shake table at a controlled frequency in the laminar jet of a pressure-velocity calibrator. This

action simulates the exposure of a hot-wire probe to a velocity fluctuation with a known frequency. If no signal attenuation occurs from the CVA during vibration, then the shake table velocity measured by an accelerometer will match the vibration velocity measured by the CVA. As attenuation becomes more prominent, the actual velocity of the shake table will exceed that sensed by the probe and measured by the CVA system.

The equipment set-up in the Cal Poly vibrations lab is shown in Figure 7.1. A VG-100 shake table manufactured by Vibration Test Systems (VTS) was driven by an HP 33120A function generator paired with an amplifier to control the frequency and amplitude of vibration. The motion of the shake table base was monitored using an attached PCB 338C04 piezoelectric accelerometer connected to an oscilloscope. The shake table was operated with a sinusoidal input between 100 and 1,000 Hz, the V_w set-point was 0.69 V, and the jet velocities exiting the nozzle were 20 m/s ($OHR=1.75$) and 50 m/s ($OHR=1.6$). The power input to the shake table was increased for each interval (50-100 Hz) in order for a constant 50 g acceleration. The mean and RMS voltages for the op-amp output V_o at each frequency were measured with a Fluke 289 digital voltmeter (DVM). The velocity measurements output by the CVA in mV RMS were calculated assuming linear local sensitivity, as explained in Chapter 5. After recording the RMS velocity, the CVA op-amp output voltage also was displayed on a Tektronix TDS 2002 oscilloscope to view the signal.

The velocity, displacement, and acceleration capabilities of the VG-100 shake table are illustrated in Figure 7.2. The maximum acceleration of the shake table was dependent on the weight of the attached bracket assembly (0.71 lbs), which fixed the probe to the base of the shake table. According to the VG-100 user manual [42],

$$\ddot{x}_{max} = \frac{100 \text{ lbs (max system force)}}{0.66 \text{ lbs (armature)} + 0.71 \text{ lbs (external bracket)}} = 73 \text{ g} . \quad (7.1)$$

Even though 73 g was the maximum rated acceleration, the shake table was operated with a peak acceleration of 50 g for a safety factor of 1.4 because overheating the shake table was a concern. The shake table has a maximum velocity of 100 in./sec and a maximum peak-to-peak amplitude of 0.75 in. These limits are plotted for varying frequency in Figure 7.2, but because the shake table was not operated below 100 Hz, acceleration was the performance limiting factor.

Figure 7.2. Acceleration, velocity, and amplitude limit plot for VG-100 shake table

The RMS velocity of the shake table was calculated under the assumption of simple harmonic motion (SHM) of the shake table, as a result of the sine wave input. For SHM,

$$\ddot{x} = -A\omega^2 \sin(\omega t), \quad (7.2)$$

and

$$\dot{x} = A\omega \cos(\omega t), \quad (7.3)$$

so for velocity RMS,

$$\dot{x}_{rms} = \frac{\ddot{x}}{2\pi f\sqrt{2}}. \quad (7.4)$$

In an effort to account for freestream noise in the laminar jet from the calibrator nozzle without any potential noise effect from the shake table, the V_o signal from the CVA in mV RMS was measured using the DVM with the air jet on, before powering on the shake table. This freestream noise value was then removed from the V_o mV RMS measurements recorded during vibration. Even though this freestream noise was very small, at 0.17 mV RMS for 20 m/s and 0.19 mV RMS for 50 m/s, it was good practice to remove this bias error from the CVA velocity data during vibration, since the shake table is the only source of perturbation. The CVA V_o was displayed on the oscilloscope for visualization with the air on, before vibration, and the corresponding displays are shown in Figures 7.3 and 7.4.

Figure 7.3. CVA V_o for $U = 20$ m/s, $V_w = 0.69$ V, $OHR = 1.75$, shake table power off

Figure 7.4. CVA V_o for $U = 50$ m/s, $V_w = 0.69$ V, $OHR = 1.6$, shake table power off

Noise was corrected for by subtracting the V_o mV RMS reading squared u_{noise}^2 in the laminar nozzle with the shake table powered off from the total V_o mV RMS reading squared u_{total}^2 during vibration at each frequency. Equation (7.5) is based on the idea that the true fluctuating velocity and the fluctuating noise quantity are not correlated.

$$u_{rms}^{true} = \sqrt{u_{rms}^2 - u_{rms}^2} \quad (7.5)$$

The final value u_{rms}^{true} in mV RMS was used to determine the velocity of hot-wire probe vibration measured by the CVA and compared to the velocity measured by the accelerometer.

The RMS velocity measured from the CVA V_o and the shake table velocity show good agreement throughout the entire frequency range, as illustrated by Figures 7.5 and 7.6. Results were slightly better for 20 m/s nozzle velocity than at 50 m/s, likely due to the higher *OHR* of 1.75 at 20 m/s vs. 1.6 at 50 m/s. A hotter wire provides better sensitivity to velocity fluctuations. An amplitude ratio of at least 0.9 up to 300 Hz was verified because the velocities agree within 10% in this frequency range. Past 300 Hz, the oscillation amplitude was too small to draw any conclusions regarding the upper CVA frequency response limits; at 400 Hz, the peak-to-peak amplitude of the shake table falls below 0.2 mm. The peak-to-peak amplitudes are shown in Figure 7.7. The limited 50 g shake table lead acceleration led to a rapid decrease in amplitude for increasing frequency. At constant acceleration, the amplitude is inversely proportional to ω^2 or $2\pi f^2$, so

$$x_{p-p} = 2 * \frac{A\ddot{x}}{(2\pi f)^2} \quad (7.6)$$

Figure 7.5. Velocity RMS measured by CVA compared to shaker velocity RMS
($U = 20 \text{ m/s}$, $OHR = 1.75$)

Figure 7.6. Velocity RMS measured by CVA compared to shaker velocity RMS
($U = 50 \text{ m/s}$, $OHR = 1.6$)

Figure 7.7. Decreasing shake table amplitude calculated from 50 g acceleration divided by $(2\pi f)^2$

The oscilloscope displays of the CVA V_o signal at shake table frequencies of 100 Hz, 400 Hz, 700 Hz, and 900 Hz are shown in Figures 7.8 through 7.15. The scope was able to automatically measure the frequency of the V_o signal, up to 250 Hz at 20 m/s and 100 Hz at 50 m/s. Past these values, the frequency of the V_o signal was determined through visual inspection of the waveform period displayed on the scope. For example, at 400 Hz, the vibration frequency and the frequency of the V_o signal matched; the period length measured on the display was 2.5 ms ($1/0.0025\text{ s} = 400\text{ Hz}$). At 700 Hz, there are about 17 peaks over a time duration of 25 ms, corresponding to a frequency 680 Hz. This is close enough to 700 Hz considering visual estimation. By a vibration frequency of 900 Hz, the period of the V_o signal was barely discernable at 20 m/s. There were 22.5 peaks over 25 ms displayed on the oscilloscope, for 910 Hz. The waveform at 50 m/s was not

clear enough to count individual periods; the hot-wire probe was more sensitive to oscillation at 20 m/s with $OHR = 1.75$, compared to an $OHR = 1.6$ for 50 m/s.

The CVA output was displayed on the oscilloscope at each frequency interval, and the visible waveform frequency matched the shake table frequency up to 900 Hz for 20 m/s and 700 Hz for 50 m/s. This result is very optimistic for CVA frequency response, because the CVA was able to detect the upper frequency range, even at very low amplitudes of vibration.

The uncertainties in the RMS velocity of the shake table measured by the accelerometer and the RMS velocity sensed by the probe connected to the CVA system were computed using the sequential perturbation method. For this method, the final result as a function of measured parameters is calculated repeatedly, each time for a different uncertainty u_i added to its corresponding measured quantity x_i . Then the differences between the final calculation with and without the uncertainty on a single measured quantity are root-sum-squared, so

$$\Delta f_i = f(x_i + u_{xi}) - f(x_i), \quad (7.7)$$

and then

$$u_f = \sqrt{\Delta f_1^2 + \Delta f_2^2 + \dots + \Delta f_n^2}. \quad (7.8)$$

Error bars are included to show the uncertainty u_f in the velocity plots of Figures 7.5 and 7.6, and the estimated uncertainties for each parameter are listed in Table 7.1. The uncertainty for the shake table velocity measured by the accelerometer is greatest at lower frequencies because the velocity is inversely proportional to angular frequency. The shake table tended to drift up to 2 g from the 50 g desired value, and the uncertainty due to accelerometer resolution and frequency precision were small in comparison.

Table 7.1. Uncertainties in shake table accelerometer and CVA parameters

Shake Table	$u_i (+/-)$
Accelerometer Non-Linearity	1%
Accelerometer Precision on Scope	0.2 V P-P
Frequency Precision	0.10%
CVA Hot-Wire	$u_i (+/-)$
Mean V_o Accuracy	0.025% + 0.0001 V
RMS V_o Accuracy	0.04% + 0.045 mV
Local Sensitivity dU/dE	0.05%

The CVA's velocity measurement uncertainty consisted of the DVM's accuracy in measuring the mean V_o in volts and the fluctuating V_o signal in mV RMS, as well as very slight error in the local sensitivity dU/dE . The DVM accuracies, specified in the Fluke 289 user manual [43], were a function of the V_o reading, so uncertainty did increase at higher voltages corresponding to higher velocities. There was also more uncertainty in the 50 m/s trial than at 20 m/s, because the lower OHR of 1.6 at 50 m/s provided lower sensitivity. The offset component of DVM accuracy (together with the nominal reading) was multiplied by the slope of the inverted calibration curve dU/dE , assuming linear local sensitivity, and dU/dE is larger for a colder probe than a hotter probe. As a result, the offset uncertainty became more prominent in the final RMS velocity for a lower OHR , compared to if there were a smaller multiplier dU/dE associated with a higher OHR .

The error bars do not overlap for all test points in the comparison plots between the shake table velocity and that measured by the CVA, so there was an unknown, uncontrolled parameter in this experiment, or the uncertainty estimates were low. One concern was that electromagnetic noise from the shake table electronics would interfere with hot-wire measurements, and this may be why CVA output waveforms on the oscilloscope display show about 1 mV peak-peak noise. However, electromagnetic noise would have driven the RMS velocity measured by the CVA up, but the velocity measured

by the CVA is lower than that measured by the accelerometer. Vibration of the hot-wire probe's prong supports for the wire was also a concern. Unfortunately, the 1 mV thickness of the waveforms covered any "fuzz" that might indicate prong vibration. The natural frequency of the wire is discussed in a paper by Persoons et. al, [44] who oscillate a probe for hot-wire measurements in environments with flow reversal. The first resonance mode for a wire without tension and clamped on both ends with $\lambda = 4.730$ is

$$f_0 = \frac{1}{2\pi} \frac{\lambda^2}{4} \sqrt{\frac{E_w D^2}{\rho_w L^4}}, \quad (7.9)$$

for a natural frequency of about 10 kHz with the 1210-T1.5 wire. This is a factor of 10 greater than the maximum vibration frequency of the shake table and suggests that the natural harmonics of the wire were probably not a source of error. However, (7.9) is a crude calculation and the assumption of the wire clamped on both ends without tension may not be valid.

The frequency response of a hot-wire probe connected to a CVA system was investigated from 100-1000 Hz using controlled vibration on a shake table at constant a 50 g acceleration. The velocities measured by the CVA and the accelerometer agreed within 10% up to 300 Hz, suggesting very little signal attenuation by the CVA in this frequency range. Above 300 Hz, data was inconclusive, because the limited capabilities of the shake table prevented amplitudes that were large enough to resemble a fluctuating velocity over the probe. The results from this first investigation into experimental validation of CVA frequency response with oscillation from a shake table are very encouraging. A similar procedure with a more powerful shake table or other means of vibration to achieve larger amplitudes would likely provide meaningful results at the

upper end of the CVA's frequency response. One method of oscillation would be a slider-crank mechanism, similar to that used by Persoons et. al [44] for their hot-wire experiments measuring reverse-flow velocities.

Figure 7.8. CVA V_o for $U = 20$ m/s, $f = 100$ Hz, $V_w = 0.69$ V, and $OHR = 1.75$

Figure 7.9. CVA V_o for $U = 50$ m/s, $f = 100$ Hz, $V_w = 0.69$ V, and $OHR = 1.6$

Figure 7.10. CVA V_o for $U = 20$ m/s, $f = 400$ Hz, $V_w = 0.69$ V, and $OHR = 1.75$

Figure 7.11. CVA V_o for $U = 50$ m/s, $f = 400$ Hz, $V_w = 0.69$ V, and $OHR = 1.6$

Figure 7.12. CVA V_o for $U = 20$ m/s, $f = 700$ Hz, $V_w = 0.69$ V, and $OHR = 1.75$

Figure 7.13. CVA V_o for $U = 50$ m/s, $f = 700$ Hz, $V_w = 0.69$ V, and $OHR = 1.6$

Figure 7.14. CVA V_o for $U = 20$ m/s, $f = 900$ Hz, $V_w = 0.69$ V, and $OHR = 1.75$

Figure 7.15. CVA V_o for $U = 50$ m/s, $f = 900$ Hz, $V_w = 0.69$ V, and $OHR = 1.6$

Measurement of Time Constant with Square Wave Test

The time constant of the CVA system paired with a 3.8 micron diameter, 1.27 mm length platinum-coated tungsten hot-wire probe (TSI 1210-T1.5) was experimentally measured using the square wave test feature built into the CVA system. This square wave test suddenly drops the current running through the probe to simulate the increased cooling stage of a velocity fluctuation. This is thought to be safer for the probe than pulsing extra current into the probe, which is done for a CTA square wave test, as discussed in Chapter 2. This time constant measurement process followed the method outlined in the Tao Systems Model 4-600 CVA manual [24].

An Agilent 54622A oscilloscope was connected to the CVA op-amp voltage output V_o for visualization and measurement of the time constant. The hot-wire probe was exposed to steady calibration flow velocities $U = 10, 30, 50 \text{ m/s}$ for corresponding $OHR = 1.9, 1.7, 1.6$ with wire voltage set-point $V_w = 0.67 \text{ V}$. The time constant decreases from $U^{1/2}$ in the denominator of (3.11), so frequency response increases with flow velocity. The oscilloscope display for the $U = 50 \text{ m/s}$ case is shown in Figure 7.16.

Figure 7.16. Square wave test to measure time constant for $U = 50 \text{ m/s}$, $OHR = 1.6$, $V_w = 0.67 \text{ V}$

The time constant is defined as the time required for the CVA output to return to 63% of its original value. This is because the unsteady heat balance equation for a hot-wire takes the form of a first order differential equation with a solution in the form of an exponential. Cursors on the oscilloscope were utilized to perform the time constant measurement. First, the horizontal cursors outlined 63% of the peak amplitude. Then the left vertical cursor was positioned at the starting point of waveform excitation, and the right vertical cursor was adjusted to intersect where the bottom horizontal cursor meets the waveform at 63% of the peak voltage. The time duration between the vertical cursors is the experimental time constant.

The time constant predictions were found to be 20-30% less than those measured from the square wave test when calculated from substitution of measured wire parameters I_w , R_w , and R_∞ at each velocity into equation (3.6).

$$M_{CVA} = \frac{a_w}{1 + 2a_w} \frac{\pi \rho_w c_w L D^2}{4 \alpha_0 R_0 I_w^2} \quad (3.6)$$

This discrepancy probably occurs because the hot-wire time constants derived from theory are based on the sensor wire alone. The actual measured I_w is up to 20% higher than that predicted by the thermal/electrical model for the same OHR ($\sim 10\%$ for the same V_w). If the measured I_w is substituted into the denominator of (3.6) for M_{CVA} prediction, these calculated M_{CVA} values are reduced.

Table 7.2 compares the measured time constants using the square wave test to those calculated with the thermal/electrical model for the same OHR , instead of with the measured current, for a more equal comparison. The model predicts a 14% longer time constant for $U = 10$ m/s and a 1% shorter time constant at $U = 50$ m/s. The CVA frequency response plot previously shown in Figure 3.3 was developed using equation (3.5) for the amplitude ratio as a function of the experimentally measured time constants with varying frequency.

Table 7.2 Measured and theoretical CVA time constants computed from measured R_w and I_w for $U = 10, 30, \text{ and } 50$ m/s, $R_\infty = 5.99 \Omega$, $D = 3.8 \mu\text{m}$, $L = 1.27$ mm, platinum-coated tungsten probe

Jet Velocity, U (m/s)	OHR	Meas. I_w (mA)	Meas. R_w (ohms)	Meas. $MCVA$ (ms)	Calc. $MCVA$ (ms)	% Diff Calc. vs. Meas.	Half Amplitude Cut-Off (Hz)
10.08	1.89	57.52	11.64	0.190	0.219	14.2	1450
30.09	1.71	63.64	10.51	0.152	0.159	4.2	1810
50.08	1.62	66.77	10.01	0.136	0.135	-0.7	2030

The influence of cable lead resistance on the measured time constant as explained by Comte-Bellot et. al [31] is expected to raise the total measured time constant by about 2% over the time constant of the hot-wire probe alone, but this was assumed negligible compared to the uncertainty introduced with the cursor method outlined above.

Low-Pass Filter Application

A low-pass filter was applied to the CVA voltage output while measuring velocity fluctuations in a turbulent jet and the cut-off frequency was gradually lowered to provide an idea of the frequencies measured and output by the CVA. Ideally, the low-pass filter set to a cut-off frequency at the upper end of the CVA frequency response would cause negligible signal attenuation on the CVA output, because the majority of the CVA output signals would pass through the filter.

The low-pass filter with a variable cut-off frequency was applied to function generated sine waves at 500 and 1 kHz frequencies to provide a clear understanding of the filter's attenuation characteristics. An amplitude ratio plot for these generated signals with the cut-off frequency adjusted from 2 kHz down to 200 Hz is displayed in Figure 7.17. When the cut-off frequency of the filter matched the frequency of the generated signal, an amplitude ratio of 0.55 was measured, confirming that the cut-off frequency gives the -3 dB attenuation as expected.

The low-pass filter's effect on the CVA hot-wire measurements in a turbulent jet is illustrated in Figure 7.18. The hot-wire probe with the wire voltage set-point $V_w = 0.69$ V was located 4 in. above the 10 mm diameter calibration nozzle outlet with a nozzle exit velocity of $U = 50$ m/s and a local mean velocity of 26 m/s. Attenuation of the CVA output signal became evident at the 800 Hz cut-off frequency, indicating that the CVA is able to measure signals with some remaining amplitude above 800 Hz. The CVA output signal for the jet must contain substantial content with frequencies in the 200-1000 Hz range, suggesting that the estimate of its -3 dB cut-off obtained from the square wave test and the thermal/electrical model are credible.

Figure 7.17. Low-pass filter applied to 500 Hz and 1 kHz, 0.4 V P-P generated signals. An amplitude ratio of 0.55 occurs when filter cut-off frequency matches generated signal frequency

Figure 7.18. Ratio of filtered to unfiltered CVA voltage output for hot-wire probe positioned 4 in. above 10 mm diameter nozzle

8. CVA OPERATION AT FLIGHT ALTITUDE

Higher OHRs and Lower Heat Transfer

A CVA system implemented on BLDS would be used to measure velocity fluctuations at high altitudes—up to about 60,000 ft—where temperatures and pressures are very low, down to about -56 °C and 7 kPa, respectively. This is very different from the laboratory conditions at standard temperature and pressure (STP), typically 20 °C and 101.3 kPa. The drop in air temperature at high altitude lowers the cold resistance of the hot-wire probe for higher *OHRs* in flight, at the same probe operating temperature. As a result, sensitivity and frequency response are improved. Also, a lower probe current and therefore V_w set-point are required for the same probe operating temperature at high altitude compared to STP conditions. However, lower air density increases the time constant, reducing frequency response. Less convection heat transfer from the hot-wire probe occurs because the density at high altitude is about 10% of its STP value, from 1.2 kg/m³ to 0.12 kg/m³. These changes in ambient conditions require a calibration curve applicable to high-altitude velocity measurements.

A 6 ohm probe at sea-level and 20 °C will drop to about 4 ohms at 60,000 ft, where $T \approx -56$ °C. This is calculated by assuming resistance is linearly dependent on temperature,

$$T_b - T_a = \frac{R_b - R_a}{\alpha_0 R_0}. \quad (8.1)$$

The $\alpha_0 R_0$ denominator term is the temperature coefficient of resistance α_o multiplied by the resistance R_0 , both at the 20 °C reference temperature. This is similar to equation (3.3), but $R_\infty \neq R_0$ because the ambient fluid temperature is 76 °C colder than the

reference temperature. At a flow velocity of 50 m/s and a probe operating temperature of 210 °C, an *OHR* of 2.70 is predicted at high altitude, compared to 1.80 at STP conditions.

This chapter investigates possible approaches for calibration applicable to flight. Also, the lower frequency response and improved sensitivity estimates at flight conditions are presented.

In-Flight Calibration

One calibration solution to much lower pressure and temperatures existing at high altitudes is to actually perform hot-wire calibration *in situ*, during flight. This could be accomplished by simultaneously reading the CVA output and matching it to velocity measured with a Pitot-static probe when both are outside any boundary layer. This could be done at any altitude, so that the calibration curve would be applied to raw velocity fluctuation data measured by the CVA. While an automated calibration procedure may be programmable into BLDS electronics, it is an additional task to accomplish before initial testing of the BLDS equipped with a small-scale prototype CVA. Another option is to modify the hot-wire calibration performed on the ground for applicability to velocity measurements at high altitude.

It is possible to linearly adjust the calibration curve for CTA systems so that it passes through a single measured velocity point at test conditions, in order to correct for small temperature drifts. This is a fairly simple process because the CTA operates with a fixed resistance and therefore wire temperature. For steady CTA calibration,

$$E^2 = (A + BU^n)(T_w - T_\infty) . \quad (8.2)$$

Brunn [12] suggests that this correction should be adequate for variations up to 80 °C in flow temperature when *OHR* ~ 1.8. The quantity E^2 in (8.2) is divided by the difference

between the operating and cold temperatures $T_w - T_\infty (\Delta T)$ during original calibration, and then E^2 is multiplied by the ΔT at test conditions.

Instead of measuring temperature differences, it is also possible to adjust the CTA calibration curve to a new fluid temperature with a multiplying factor in order for the E^2 value as a function of velocity from the original calibration curve to agree with an E^2 value measured at the same velocity under different conditions. The end result is a temperature corrected calibration curve suitable velocity measurements at the new temperature.

As discussed in Chapter 5, the CVA is traditionally calibrated in the form,

$$E = A + BU^n . \quad (8.3)$$

With CVA, the *OHR* does not remain constant with respect to velocity, so temperature correction is less straightforward. A CVA calibration method referred to as *pdr* has been proposed to compensate for drifts in fluid temperature by Sarma and Comte-Bellot [45], and for changes in *OHR* by Truzzi, Sarma, and Chokani [46]. This calibration technique involves equating the heat transfer from the hot-wire probe to a power-law function of velocity, and then normalizing this equation by the difference between the hot operating resistance and the cold resistance at fluid temperature, $R_w - R_\infty$. The *pdr* relationship is therefore written as

$$pdr = \frac{P_w}{R_w - R_\infty} = A + BU^n , \quad (8.4)$$

with

$$A = 0.24 \frac{2L\pi k_f}{\alpha_0 R_0} \text{ and } B = 0.56 \frac{2L\pi k_f}{\alpha_0 R_0} \left(\frac{\rho_f D}{\mu_f} \right)^n , \quad (8.5)$$

and

$$P_w = I_w V_w = \frac{V_w^2}{R_w} = I_w^2 R_w . \quad (8.6)$$

The *pdr* method reduces temperature influence on A and B . Unfortunately for calibration purposes, A and B still have some remaining dependence on the thermal properties of both the hot-wire probe and the air [45]. This dependence is illustrated in Figure 8.1, which shows the *pdr* output for V_w set-points from 0.37 V to 0.83 V for $U = 5-54$ m/s; these curves do not perfectly overlap. References [45] and [46] use a single calibration curve developed from a curve fit through all of their *pdr* data sets, for a more flexible calibration curve applicable to different *OHRs* and fluid temperatures. Data acquisition for the *pdr* method is also in terms of the *pdr* quantity and requires measurement of R_w as shown by (8.6). Monitoring the fluctuating I_w instead of R_w with the CVA tested in this report would also be possible, although either could be computed from a measurement of V_o when V_w and all other fixed CVA circuit parameters are known. Truzzi et. al [46] found the error associated with velocity measurement using a single calibration curve for $V_w = 0.4-0.7$ V to be less than about 2 m/s, over the range $U = 10-50$ m/s. Varying V_w simulates a temperature drift on the *pdr* quantity because the V_w changes the *OHR*, and therefore $R_w - R_\infty$ in the denominator of (8.4).

The experimental *pdr* during CVA calibration of a TSI 1210-T1.5, 6 ohm cold resistance probe in the laboratory is plotted in Figure 8.1 for 4 different V_w set-points over $U = 5-54$ m/s velocity at near STP conditions of 20.4 °C and 100.6 kPa. The spread of this *pdr* data is much smaller compared to the V_o data at different V_w set-points previously shown in Figure 5.4. For $V_w = 0.37$ V-0.83 V at 39 m/s, V_o varies by 46%, compared to a 6% variation in *pdr* for the same V_w range. These results show that the *pdr* method can

collapse calibration results for various temperature differences between the hot-wire and the air flow.

Figure 8.1. Measured pdr at STP for $V_w = 0.37\text{ V}$, 0.54 V , 0.69 V , and 0.83 V , $U = 5\text{-}54\text{ m/s}$

The thermal/electrical CVA model from Appendix B was used to predict the pdr calibration curves for the same laboratory conditions as the data in Figure 8.1. The pdr curves predicted for STP conditions at $V_w = 0.6\text{ V}$, 0.7 V , and 0.8 V are displayed in Figure 8.2. The predicted pdr output is collapsed for different V_w set-points, similar to the experimental pdr . The major difference is that the pdr output decreases as the V_w set-point increases for the real calibration data (in-agreement with [45] and [46]), but for the predicted data, the pdr output increases for increasing V_w set-point. This discrepancy is probably a result of the model's simplified circuit and heat transfer analysis, but the model was still used as a rough prediction tool for changes in behavior between ground and flight conditions.

Figure 8.2. Predicted pdr output, $V_w = 0.6$ V-0.8 V, $U = 5$ -54 m/s, STP conditions

The predicted pdr output was compared between STP conditions on the ground to the flight altitude of 60,000 ft with a temperature of -56.5 °C and pressure of 7.2 kPa, at $V_w = 0.5$ and 0.6 V. The pdr technique was not effective across this large variation in both temperature and pressure, shown by Figure 8.3. However, the pdr method did successfully compensate for variations in OHR caused by different V_w set-points, shown by the close match between the $V_w = 0.5$ V and 0.6 V curves, but only for the same ambient air conditions. The failure of pdr to collapse the calibration curves for different pressures is not really surprising since B , the parameter appearing in (8.5), is directly dependent of fluid density.

Figure 8.3. Distinct pdr curves between STP conditions and 60,000 ft altitude
 $V_w = 0.5\text{-}0.6\text{ V}$, $U = 50\text{--}100\text{ m/s}$

The predicted laboratory pdr calibration curves were shifted with a linear multiplier in an attempt to match those at high altitude. The value of the multiplier β would be obtained by measuring the mean velocity simultaneously with the CVA hot-wire and a Pitot-static probe, both outside the boundary layer during flight:

$$\beta * pdr_{STP}(U) = pdr_{flight}(U) .$$

The corresponding value of β was applied to the pdr quantities for $V_w = 0.6\text{ V}$ measured from STP calibration, and this same shifting process was redone for $V_w = 0.5\text{ V}$. An 18% change exists between the pdr flight curve and shifted STP pdr curve at the upper and lower velocity bounds $U = 100\text{ m/s}$ and 50 m/s for $V_w = 0.6\text{ V}$, and 17% change exists at the same velocities for $V_w = 0.5\text{ V}$. The curves at each V_w set point are displayed in Figures 8.4 and 8.5, respectively. Percent change refers to the difference in CVA output

between the altitude and STP shifted curves at a given velocity, relative to the change in output at altitude conditions across the total velocity range. The remaining separation between pdr calibration curves after shifting would contribute unacceptable error in both the linear local sensitivity and mean velocity.

Figure 8.4. Calibration with pdr from STP conditions shifted with constant multiplier to conditions at 60,000 ft, $V_w = 0.6 \text{ V}$, $U = 75 \text{ m/s}$

Figure 8.5. Calibration with pdr from STP conditions shifted with constant multiplier to conditions at 60,000 ft, $V_w = 0.5$ V, $U = 75$ m/s

A traditional CVA calibration using the op-amp output V_o was also checked for possible adjustment to flight conditions. The V_o calibration curves between ground and flight for $V_w = 0.5$ V and 0.6 V are displayed in Figure 8.6. The calibration curves at STP using V_o were shifted using the same process as for pdr with a multiplying factor at $U = 75$ m/s; see Figure 8.7 for $V_w = 0.6$ V and Figure 8.8 for $V_w = 0.5$ V. The $V_w = 0.6$ V calibration curve shows the best agreement, with 3% difference at $U = 100$ m/s and a near match at $U = 50$ m/s. Unfortunately, the V_o calibration curves between shifted ground and flight are less similar at $V_w = 0.5$ V. There is 6% change in V_o , so the success in shifting the $V_w = 0.6$ V case is not applicable to a wide V_w range.

Figure 8.6. Op-amp V_o at STP conditions compared to 60,000 ft, $V_w = 0.6$ V and $U = 50$ -100 m/s

Figure 8.7. Comparison between op-amp V_o for STP conditions shifted with constant multiplying factor to V_o at 60,000 ft, $V_w = 0.6$ V, $U = 75$ m/s

Figure 8.8. Comparison between op-amp V_o for STP conditions shifted with constant multiplying factor to V_o at 60,000 ft, $V_w = 0.5$ V, $U = 75$ m/s

Calibration predictions from the thermal/electrical model show that calibration curves developed at STP conditions using V_o can be better adjusted to flight altitude compared to calibration with pdr , but significant error would still be introduced with shifted calibration curves using either method. A suitable CVA calibration method for in-flight velocity measurements requires further investigation.

Frequency Response and Sensitivity in Flight

The higher $OHRs$ at altitude will improve sensitivity, but frequency response is predicted to decrease. The effect on sensitivity was studied by calculating the CVA sensitivity coefficient with respect to velocity S_u , previously defined by (3.12). The value S_u is an estimate for the ratio of the instantaneous voltage output to the instantaneous velocity, and both of these quantities are relative to their mean values. Equation (3.15)

was used to compute S_u for $U = 100-200 \text{ m/s}$ at a fixed 210°C probe temperature for STP conditions ($OHR = 1.8$) and high altitudes ($OHR = 2.7$). The sensitivity coefficient is 20% higher for flight over this velocity range. This sensitivity increase from ground to high altitude will improve the signal-to-noise ratio for velocity fluctuation measurements in flight.

The thermal/electrical model was used to predict the frequency response at high altitude and STP conditions for $U = 50-200 \text{ m/s}$ over the same probe temperature, $T_w = 210^\circ\text{C}$, with constant $OHR = 1.8$ ($a_w = 0.8$). The probe temperature, and therefore R_w , was set constant in the model for an equal comparison of frequency response across different velocities and conditions. In actual CVA operation over a velocity range, a_w decreases for increasing velocity, causing the ratio $(1+a_w)/(1+2a_w)$ to slightly increase. However, the $U^{1/2}$ relationship in the denominator of causes an overall decrease in M_{CVA} , as previously mentioned.

$$M_{CVA} = \frac{(1 + a_w)}{(1 + 2a_w)} \frac{\rho_w c_w D^2}{4k_\infty} \frac{1}{\left[A' + B' \left(\frac{\rho_\infty U D}{\mu_\infty} \right)^{1/2} \right]} \quad (3.11)$$

The frequency response plot for CVA operation at high-altitude conditions is shown in Figure 8.9, and Table 8.1 compares the predicted half-amplitude cut-off frequencies between STP and high-altitude conditions.

Figure 8.9. Predicted frequency response at 60,000 ft flight altitude, $T_w = 210\text{ }^{\circ}\text{C}$, $U = 50\text{--}200\text{ m/s}$, $D = 3.8\text{ }\mu\text{m}$, $L = 1.27\text{ mm}$, platinum-coated tungsten probe, $6\text{ }\Omega$ at $20\text{ }^{\circ}\text{C}$

Table 8.1. Cutoff frequency comparison between ground and flight (60,000 ft) conditions for $U = 50\text{--}200\text{ m/s}$, $T_w = 210\text{ }^{\circ}\text{C}$, $D = 3.8\text{ }\mu\text{m}$, $L = 1.27\text{ mm}$, $6\text{ }\Omega$ at $20\text{ }^{\circ}\text{C}$, platinum-coated tungsten probe

<i>Constant $T_w = 210\text{ }^{\circ}\text{C}$, $OHR = 1.80\text{ Ground}$, $OHR = 2.65\text{ Flight}$</i>						
Velocity (m/s)	Altitude V_w (V)	STP V_w (V)	STP $Mcva$ (ms)	Altitude $Mcva$ (ms)	STP Cut-off Freq. (Hz)	Altitude Cut-off Freq. (Hz)
50	0.52	0.69	0.127	0.279	2170	989
100	0.58	0.79	0.097	0.224	2850	1230
150	0.62	0.86	0.082	0.196	3360	1410
200	0.65	0.91	0.073	0.177	3780	1550

The key observation from this consideration of in-flight performance is that frequency response is significantly lower at high altitude; the time constants are over twice as long and the cut-off frequencies are less than half of those at STP conditions, for the same probe temperature and velocity. This occurs as a result of the 90% drop in air density, which is in the denominator of (3.11). Even though a_w increases from the lower

ambient probe temperature at high altitude, which decreases the time constant from the $(1+a_w)/(1+2a_w)$ ratio, the drop in density causes the time constant to increase overall. The alternate expression for M_{CVA} in (3.6) also shows that the time constant will increase at high altitude because a lower probe current (I_w^2 in the denominator) is required at high altitude compared to STP conditions; convection cooling is less effective at the same velocity because the air density is lower at high altitude.

Notice in Table 8.1 that the time constant varies by about 20% between $U = 100$ - 200 m/s. In order for sufficient frequency response with strong enough amplitude ratios to clearly measure velocity fluctuations in flight, it may be necessary to operate the CVA on a BLDS at relatively high velocities and hot probe temperatures. Smaller wire diameters than those on the TSI 1210-T1.5 probe used for this project would also significantly improve the frequency response, as previously shown by equation (3.11).

9. CONCLUSIONS

The direct measurement of velocity fluctuations is desired for BLDS. This would enable BLDS to discern laminar and turbulent flows in flight, which is particularly important for laminar flow control applications. Hot-wire anemometry is a well-proven way to measure velocity fluctuations, so three system types were investigated for BLDS: Constant-Current, Constant-Temperature, and Constant-Voltage. The CVA was found most promising for BLDS, because it had the best balance between frequency response, operational simplicity without a delicate bridge, and ease of hardware integration with BLDS. A thermal/electrical model to predict CVA performance was developed from application of the Collis and Williams correlation to a heated cylinder in cross-flow, together with an electrical model of the CVA operating circuit.

A bench top CVA system was built by BLDS consultant Don Frame. This system was used to calibrate a 3.8 micron diameter, 6 ohm cold resistance, platinum-coated tungsten probe (TSI 1210-T1.5) in the laminar jet of a pressure-based velocity calibrator. Calibration curves in the form of a power-law accurately fit the calibration data, and were used for converting the CVA output to velocity. Velocity data can be acquired with either the op-amp output voltage V_o or the voltage proportional to probe current I_w . Under the assumption of linear local sensitivity, RMS voltage output was converted to RMS velocity. This assumption was required to apply a steady calibration curve to dynamic turbulent flow measurements. After experimentation with different voltage set-points across the hot-wire probe in the turbulent jet of the calibration nozzle, $V_w = 0.67\text{-}0.69\text{ V}$ was chosen, for $OHR = 1.6$ at $U = 50\text{ m/s}$. This V_w set-point provided a high probe

operating temperature for a good signal-to-noise ratio, while not exceeding an *OHR* of 2.0 at $U = 5 \text{ m/s}$.

The CVA was first tested with a hot-wire probe positioned in a turbulent jet. A turbulence intensity of 17% was measured 5 in. above a 10 mm diameter calibration nozzle outlet ($x/D \approx 12.7$), with a $U = 50 \text{ m/s}$ exit velocity. This result is reasonable compared to the published 24% intensity at $x/D = 20$ because self-similarity is not expected until $x/D = 50$. Next, three wind tunnel tests were performed on a sharp-edged flat plate for freestream velocities between 11-46 m/s. A maximum 2.5% turbulence intensity was measured in the laminar boundary layer, with the hot-wire probe located at $y = 1 \text{ mm}$ above the plate surface and horizontal distance $x = 20 \text{ in}$ from the leading edge. For the turbulent case, a trip was installed at $x = 13.5 \text{ in}$, resulting in a measured turbulence intensity of 12.5-6% over $U = 11\text{-}46 \text{ m/s}$. The intensity dropped with increasing freestream velocity for three reasons: growing disturbance frequencies, increasing relative probe height in the boundary layer, and decreasing skin friction. This turbulent data also agreed with published values. A freestream measurement was also taken at $y = 30 \text{ mm}$ and reached 0.09% turbulence intensity, indicating the existence of very low noise introduced by the CVA and inside the wind tunnel freestream.

The CVA frequency response was investigated by oscillating a hot-wire probe in the laminar jet of a calibration nozzle, using a low amplitude shake table operated at a peak acceleration of 50 g. Very little signal attenuation for the CVA was shown, verifying an amplitude ratio of at least 0.9 up to 300 Hz. Past 300 Hz, the shake table was unable to provide sufficient amplitudes of oscillation for quantitative dynamic velocity measurements with the CVA. The CVA time constants were experimentally measured

with a square wave test for a cut-off frequency between 1400 and 2000 Hz over $U = 10$ - 50 m/s, respectively; these values were within 14% of estimates from the thermal/electrical model for the same *OHR*.

The *pdr* calibration method developed for temperature correction was investigated as a method to apply a calibration curve at laboratory conditions to high altitude with very low pressures and temperatures down to 7.2 kPa and -56 °C, respectively. The thermal/electrical model revealed that the *pdr* method would not compensate for the low air density at high altitude. A simple multiplying factor would not adequately shift a calibration curve developed with the CVA op-amp output voltage, or using *pdr*, for use in flight at the same V_w set-point. It was also predicted that the frequency response will drop at high altitude, with cut-off frequencies less than half of those estimated at STP conditions for the same probe temperature and velocity.

The CVA is ready for implementation on BLDS because it has proven successful at distinguishing between laminar and turbulent flows through measurement of velocity fluctuations in a turbulent jet and in a flat plate boundary layer. The next hardware development step is to build a printed-circuit-board model of the CVA. The frequency response of the CVA could be further investigated with a high-amplitude shake table. The use of smaller diameter probes would help improve the frequency response by decreasing the time constant, which is roughly proportional with diameter raised to the 3/2 power. Finally, testing the CVA in low temperature and pressure conditions similar to those at high altitude would help predict CVA behavior and performance in flight.

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APPENDIX A. CVA OPERATION PROCEDURE

rev. 05/28/12

CVA Quick Reference Guide

Required Equipment

CVA System
3 BNC cables
2 Digital Volt Meters (DVM)
Hot-wire probe support
Probe support shorting plug
Hot-wire probe

Nomenclature

V_w (V) = Constant voltage value across hot-wire probe
 R_w (Ω) = Wire resistance at operating temperature (hot)
 R_{∞} (Ω) = Wire resistance at room temperature (cold)
 I_w (mA) = Current through wire
 R_{lim} (Ω) = Threshold at which wire voltage will decrease by 0.1 V

Output Conversions

System Parameter	Calculation Using CVA Output
V_w (V)	V_w (V) = V_w -Out = V_w -Set
I_w (mA)	I_w (mA) = (40 mA/V) $\times$ I_w -Out (V)
R_w (ohms)	R_w (ohms) = (5 Ω /V) $\times$ R_w -Out (V) – box (0.1 Ω) – BNC cable res. (Ω) – probe support res. (Ω) – internal probe res. (Ω)
R_{∞} (ohms)	R_{∞} (ohms) = DVM reading (Ω) – BNC cable res. (Ω) – probe support res. (Ω) – internal DVM res. (Ω) – internal probe res. (Ω)
R_{lim} (ohms)	R_{lim} (ohms) = (2.5 Ω /revs) $\times$ R -Limit (# revs)

Operation Procedure

1. Ensure the **Ext., Off, and Int.** selector switch is set to the middle **Off** position.
2. Flip **Power** switch up to turn CVA system on. **Power** LED should light green and the **R-Limit** indicator should light red.
3. Set wire voltage (V_w) to 0.1 V using **V_w-Set** knob dial.
Note: Measuring **V_w-Out** on a DVM without **Ext.** mode active will not show the true V_w ; use numbers on knob dial.
4. Connect the probe support to one end of a BNC cable and the opposite BNC cable end to a DVM that outputs a low short circuit current (≤ 1 mA). Install probe support shorting plug. Measure and record combined resistance of the BNC cable, probe support, and internal DVM resistance. Turn off DVM.
5. Remove shorting plug from probe support and carefully install hot-wire probe. Record probe serial #.
6. Now measure combined hot-wire probe resistance with DVM and subtract off total resistance from step #4, as well as manufacturer specified internal probe resistance. The remaining value in ohms is the cold wire resistance (R_{∞}).
7. Remove BNC cable end connected to DVM and connect to the **Sensor** output on CVA system.
8. Set the resistance limit (R_{lim}) threshold to a couple ohms above R_{∞} found in step #6 using the **R-Limit** knob dial. There are 2.5 ohms per revolution of the **R-Limit** knob dial.

Warning: If probe resistance exceeds the **R-Limit** setting in ohms (R_{lim}), as indicated by red **R-Limit** LED illumination, V_w will drop 0.1 volts for burnout protection. V_w will raise back up 0.1 V when probe resistance is cooled below R_{lim} , indicated by the **R-Limit** LED colored green.

9. Connect one DVM to the **V_w-Out** BNC jack and another DVM to the **R_w-Out** BNC jack.
10. Move the selector switch left to **Ext.** from the center **Off** position. The **R-Limit** LED should turn green as a result of step #8 and **V_w-Out** should read 0.1 V on DVM.
11. Measure the cold probe resistance (R_{∞}) with the CVA by gradually lowering the **V_w-Set** knob dial over the 0.04 V – 0.05 V range while monitoring **R_w-Out** using the connected DVM with 5 Ω /V. Record the lowest observed resistance and verify that it is similar to the combined resistance (< 0.5 Ω difference) from step #6.
12. Increase V_w back to 0.1 V and move selector switch to the **Off** position.
13. Position the hot-wire sensor within a nozzle diameter of the Cal-Jet nozzle outlet or set-up probe in wind-tunnel.
14. Set **R-Limit** to a couple ohms above the expected probe's hot operating resistance (R_w) using

$$R\ Limit(\#revs) = \frac{OHR \times R_{\infty} (\Omega)}{2.5 \Omega/rev} \text{ and } OHR = \frac{R_w}{R_{\infty}}.$$

15. Turn **AIRFLOW ON** and adjust to velocity at first measurement point.
16. Move the selector switch left to **Ext.** from the center **Off** position. **V_w-Out** should read 0.1 V on DVM.
17. Increase V_w to desired set-point within 0-1 V range while monitoring **R_w-Out** with connected DVM.

Calculate necessary value for **R_w-Out** from OHR with respect to flow speed using

$$R_w\ Out(V) = \frac{OHR \times R_{\infty} (\Omega) + \text{res. offsets incl. } 0.1 \Omega \text{ for CVA box}}{5 \left(\frac{\Omega}{V} \right)}.$$

Never exceed an OHR > 2.0. Lock **V_w-Set** knob dial before measurements.

18. Once V_w is properly set, use one of the DVMs for measuring the op-amp output voltage, **V_o-Out**, or the wire current output, **I_w-Out**, for mean and RMS data. Use the other DVM for monitoring the mean **R_w-Out** or **V_w-Out**.
Note: Set DVM to Volt range for mean data and mV range for RMS data, in order to avoid accuracy problems.
19. The **R-Limit** LED should remain solid green during operation. If **R-Limit** LED illuminates solid red, immediately flip selector switch to **Off** from **Ext.** in an effort to protect the probe. Recheck R_{lim} , R_{∞} , and the V_w set-point.
20. If the **R-Limit** LED blinks red (possibly during large turbulent fluctuations), check that the mean value of **R_w-Out** will not cause OHR > 2.0. If safe, try increasing R_{lim} by turning the **R-Limit** knob dial ½ a revolution, equal to 1.25 ohms, in order to stabilize the green **R-Limit** LED.
21. **Once measurements are complete, DO NOT reduce airflow below first measurement point.**
22. Move selector switch to **Off** from the **Ext.** position. The **R-Limit** LED should turn red.
23. Flip **Power** switch down to power off CVA. All LED lights should become dark.
24. Decrease V_w back down to 0.1 V using **V_w-Set** knob dial.
25. Turn off airflow and DVMs.
26. Carefully remove hot-wire probe from probe support and return probe to original case.

APPENDIX B. CVA CALIBRATION PREDICTION

This appendix explains the key concepts and steps in the thermal/electrical model used to predict CVA behavior during steady velocity calibration. The output parameters for CVA are either the op-amp output voltage V_s or current I_w through the hot-wire probe. The power dissipated by the probe wire is a function of the flow velocity U , wire resistance R_w , and probe current I_w . A simplified CVA circuit diagram is shown in Figure B.1.

Figure B.1. Simplified CVA circuit diagram

Figure B.2. Heated cylinder in cross-flow

For thermal equilibrium on the wire in cross flow under forced, steady state, cross-flow convection and neglecting end losses as drawn in Figure B.2 [12]:

$$I_w^2 R_w = \pi D L h (T_w - T_\infty) . \quad (\text{B.1})$$

The convection coefficient h can be approximated using the more experimentally applicable version of King's Law developed by Collis and Williams [27]:

$$Nu \left(\frac{T_f}{T_\infty} \right)^{-0.17} = 0.24 + 0.56 Re_D^{0.45} . \quad (\text{B.2})$$

The T_f/T_∞ ratio is the film temperature divided by the fluid temperature. Equation (B.2) is valid between Reynolds numbers of 0.02 to 44, with the Nusselt number equal to

$$Nu = \frac{hD}{k_f} , \quad (\text{B.3})$$

and the Reynolds number as

$$Re_D = \frac{\rho_f U D}{\mu_f} , \quad (\text{B.4})$$

with fluid properties calculated at film temperature,

$$T_f = \frac{T_w + T_\infty}{2} . \quad (\text{B.5})$$

By rearranging (B.2) the convection coefficient is then

$$h = \frac{k_f}{D} \left(0.24 + 0.56 \left(\frac{\rho_f U D}{\mu_f} \right)^{0.45} \right) \left(\frac{T_f}{T_\infty} \right)^{0.17} \quad (\text{B.6})$$

and the wire resistance is assumed to vary linearly with temperature expressed as [28]

$$T_w - T_\infty = \frac{R_w - R_\infty}{\alpha_0 R_0} . \quad (\text{B.7})$$

The temperature coefficient of resistance α_0 ($^{\circ}\text{C}^{-1}$) multiplied by probe resistance at 20 $^{\circ}\text{C}$ room temperature R_0 , relates a change in temperature to a change in resistance. Ohm's law is also needed, expressed as

$$V_w = I_w(R_w + R_L) . \quad (\text{B.8})$$

The simultaneous solution of these 8 equations (B.1)-(B.8) at specific known values for U , T_∞ , R_∞ , and V_w will determine these 5 unknown variables: T_w , T_f , R_w , I_w , and h , along with the required fluid properties. The cold probe resistance R_∞ is measured at ambient fluid temperature T_∞ , the V_w set-point is selected by the user, and the velocity U is an input because this thermal/electrical model predicts CVA behavior during calibration with known velocities. The CVA op-amp output voltage is then

$$V_o = \left(1 + \frac{R_2}{R_F} + \frac{R_2}{R_w + R_L}\right) \frac{V_w}{2} .$$

An iterative solver with built-in thermal properties named Engineering Equation Solver or EES, was used to solve the simultaneous equations. The program's thermal property tables were useful because the fluid properties in this heat transfer analysis depend on T_f , but T_f is an unknown parameter. A print out of the thermal/electrical model's equations is included at the end of this appendix. Hot-wire specifications are based on a TSI 1210-T1.5 probe [36], which is a platinum-coated tungsten wire 3.8 microns in diameter and 1.27 mm in length.

*CVA Thermal/Electrical Model 05/31/12**Collis & Williams applies to fluid properties at film temperature*

$$\rho_f = \rho \left[\text{'Air'}, T = T_f, P = P_{\text{atm}} \right] \text{ kg/m}^3$$

$$\mu_f = \text{Visc} \left[\text{'Air'}, T = T_f \right] \text{ N-s/m}^2$$

$$v_f = \frac{\mu_f}{\rho_f} \text{ m}^2/\text{s}$$

$$c_{p_f} = \text{Cp} \left[\text{'Air'}, T = T_f \right] \text{ J/kg-K}$$

$$Pr = \text{Pr} \left[\text{'Air'}, T = T_f \right]$$

$$k_{\text{film}} = k \left[\text{'Air'}, T = T_f \right] \text{ W/m-K}$$

Determine ambient fluid properties

$$\rho_{\infty} = \rho \left[\text{'Air'}, T = T_{\infty}, P = P_{\text{atm}} \right] \text{ kg/m}^3$$

$$\mu_{\infty} = \text{Visc} \left[\text{'Air'}, T = T_{\infty} \right] \text{ N-s/m}^2$$

$$k_{\infty} = k \left[\text{'Air'}, T = T_{\infty} \right] \text{ W/m-K}$$

$$Re_{\infty} = \frac{\rho_{\infty} \cdot U \cdot D}{\mu_{\infty}}$$

$$P_{\text{atm}} = 101.325 \text{ kPa}$$

$$T_{\infty} = 20 + 273.15 \text{ K, Convert to absolute temperature in Kelvin}$$

Probe specifications

$$A_s = \pi \cdot D \cdot L \text{ Surface area for cross-flow convection}$$

$$L = 0.00127 \text{ 1.27 mm length wire - TSI 1210 T-1.5}$$

$$D = 0.0000038 \text{ 3.8 micron dia. wire - TSI 1210 T-1.5}$$

$$b_{20} = 0.0042 \text{ deg. C}^{-1}, \text{ Thermal resistivity at 20 deg. C}$$

$$c_w = 140 \text{ J/kg-K Wire specific heat}$$

$$\rho_w = 20000 \text{ kg, Wire density rounded up from 19300 since platinum-coated}$$

$$m_w = \frac{\pi}{4} \cdot D^2 \cdot L \cdot \rho_w \text{ kg, Mass of wire for specific heat multiplier}$$

$$T_f = \frac{T_w + T_{\infty}}{2} \text{ K}$$

$$T_{w,\text{cel}} = T_w - 273.15 \text{ Temperature output in Celsius for display purposes}$$

$$U = 50 \text{ m/s, Flow velocity}$$

Thermal Equilibrium

$$V_w = I_w \cdot R_w \quad V$$

$$P_w = I_w \cdot V_w \quad W$$

$$P_w = h \cdot A_s \cdot [T_w - T_{\infty}] \quad \text{Cylinder in cross flow convection}$$

Circuit Properties

$$R_2 = 50 \quad \text{ohms}$$

$$R_F = 1000 \quad \text{ohms}$$

$$R_p = 0.17 \quad \text{ohms, Internal probe resistance}$$

$$R_c = 0.11 + 0.02 \quad \text{ohms, Cable and probe support resistance}$$

$$V_{w,\text{total}} = 0.6 \quad V, \text{ Total set-point across cable loop containing probe}$$

Resistance Relations

$$R_w = R_{\infty} \cdot [1 + b_{20} \cdot (T_w - T_{\infty})] \quad \text{Probe resistance assumed linearly temp. dependent}$$

$$R_{\infty} = 6 \quad \text{ohms, Measured cold resistance at 20 deg. C}$$

$$\text{OHR} = \frac{R_w}{R_{\infty}}$$

$$a_w = \frac{R_w - R_{\infty}}{R_{\infty}}$$

Nusselt Number from Collis Williams Correlation

$$\text{Nus} = [A + B \cdot \text{Re}_{\text{film}}^n] \cdot \left[\frac{T_f}{T_{\infty}} \right]^{0.17}$$

$$A = 0.24$$

$$B = 0.56$$

$$n = 0.45$$

$$\text{Nus} = \frac{h \cdot D}{k_{\text{film}}}$$

$$\text{Re}_{\text{film}} = \frac{\rho_f \cdot U \cdot D}{\mu_f} \quad \text{Collis-William coefficients valid for } 0.02 < \text{Re} < 44$$

Output Parameters

$$V_o = \left[1 + \frac{R_2}{R_F} + \frac{R_2}{R_{w,\text{total}}} \right] \cdot \frac{V_{w,\text{total}}}{2} \quad V$$

$$R_{w,\text{total}} = R_w + R_c + R_p \quad \text{ohms}$$

$$V_{w,\text{total}} = I_w \cdot R_{w,\text{total}} \quad V$$

$$I_{w,\text{mA}} = I_w \cdot 1000 \quad \text{mA}$$

$$I_{w,\text{volts}} = \frac{I_{w,\text{mA}}}{40} \quad V, \text{ Actual CVA outputs Voltage with 40 mA/Volt}$$

$$\text{PDR} = \frac{P_w}{R_w - R_{\infty}} \quad W/\text{ohm}, \text{ Temperature corrected calibration quantity}$$

Calculate Time Constant and Frequency Response

$$M_{\text{cca}} = \frac{c_w \cdot m_w \cdot [R_w - R_{\infty}]}{b_{20} \cdot I_w^2 \cdot R_{\infty}^2} \quad \text{sec}$$

$$M_{\text{cva}} = \left[\frac{M_{\text{cca}}}{1 + 2 \cdot a_w} \right] \cdot 1000 \quad \text{sec}$$

$$M_{\text{cva,ms}} = M_{\text{cva}} \cdot 1000 \quad \text{ms}$$

$$H_{\text{ratio}} = \left[1 + (2 \cdot \pi \cdot f_{\text{cva}})^2 \cdot M_{\text{cva}}^2 \right] \left[\frac{-1}{2} \right]$$

$$H_{\text{ratio}} = 0.5 \quad \text{Calculate frequency at half-amplitude}$$

APPENDIX C. DUAL PROBE PRESSURE-VELOCITY CALIBRATOR

The Cal Poly BLDS group was sponsored by Northrop Grumman to build a new pressure-velocity calibrator, primarily for use with hot-wire research. A photo of the final calibrator design is shown in Figure C.1. This type of calibrator exposes a velocity measurement probe to steady laminar velocities up to about 100 m/s. At each velocity, the voltage output from the probe is recorded and plotted in calibration curve.

Figure C.1. Dual probe pressure-velocity calibrator

The relatively simple upright design of BLDS's older TSI 1128 model was sturdy, intuitive to operate, and simple to assemble. Due to these appealing factors, the basic layout of the 1128 model greatly influenced the new custom-built calibrator. Some improved features are that the airline connection has been moved to the front, the vertical support assembly for the clamping fixtures transverses horizontally, and the clamping fixture accommodates 2 hot-wire probe supports for simultaneous calibration of two probes. The different diameter nozzle outlets are interchangeable between both calibrator models.

Two probe support clamping fixtures were designed: a single probe support model, as well as a dual clamping model. These were CNCd at Cal Poly's Mustang 60 and are shown in Figure C.2, and a screenshot of the SolidWorks assembly for the dual clamping fixture is in Figure C.3. Rotation of each thumbscrew drives its clamping block onto the stem of the probe support. The clamping blocks were manufactured on an RP machine out of plastic resin. They have an angled groove designed to accommodate probe supports ranging in diameter from 0.1 to 0.25 in. The thumbscrews have right handed threads for turning into the aluminum clamping arm, and left handed threads to drive each clamping block against the adjacent probe support. The functionality of a turnbuckle greatly influenced this design.

A particularly convenient feature of these fixtures is that the inner clamping region is accessible through a 0.26 inch wide gap in the external arm piece. This allows the user to safely and conveniently load the hot-wire probe into a probe support before alignment over the nozzle. With the 1128 model, the user was required to either install the hot-wire sensor into the probe support at an awkward orientation in close proximity to

the nozzle, or dangerously slide the probe support containing the hot-wire sensor through a tight space before clamping.

Figure C.2. Single and dual probe clamping fixtures

Figure C.3. Assembly view of dual probe clamping fixture

Flow conditioning requirements were investigated for the new calibrator. It was important for the calibrator to have a uniform, fully laminar flow at the nozzle outlet in order for accurate calibrations. The velocity profile of the 14 mm nozzle was measured by traversing a Pitot-tube horizontally in half-mm increments across the nozzle outlet for cases with and without 1.5 inch thick honeycomb installed in the pressurized settling

chamber. This honeycomb material was in addition to the already existing 4 layers of 33% open area mesh screen spaced about 0.75 in. apart. The plot in Figure C.4 shows that total pressure varies by only about 10 thousands of an inch of water. The offset between the data sets with and without honeycomb are due to a small offset in differential pressure inside the nominal 6 inch diameter pipe chamber. The differential pressure of 6 in. H₂O between the settling chamber and ambient room corresponds to a nozzle exit velocity of about 50 m/s. The conclusion drawn from this test is that the Pitot pressure, and therefore velocity, is uniform without honeycomb, so it was left out of the new calibrator. The new settling chamber assembly is illustrated in Figure C.5.

Figure C.4. Measured total pressure using Pitot tube 1 mm above 14 mm dia. nozzle outlet with chamber pressure at 6 in H₂O

Figure C.5. Settling chamber assembly

A parts list is included in Table C.1, followed by the dimensioned part drawings.

Table C.1. List of materials for pressure-velocity calibrator

6061 Aluminum Stock			
Quantity	Dimensions - H (in) x W (in) x L (in)	Part Desc.	McMaster Item #
2	3/4 x 10 x 12	Clamp Arms	8975K122
2	3/8 x 10 x 12	Flange Pieces	8975K131
1	3/8 x 3/4 x 12	Stand Base	8975K463
1	1/4 x 3/4 x 24	Stand Bar	8975K27
1	1/4 x 18 x 18	Top Cover Plate	89155K27
1	1/8 x 12 x 12	Back Cover Panel	89015K18
Fastener Hardware			
Quantity	Desc.	Material	McMaster Part #
3	1/4-28 Knurled Thumb Screw, 1 in. L	Stainless Steel	91746A475
4	10-32 Knurled Thumb Screw, 0.5 in. L	Stainless Steel	94567A420
4	1/4-20 Threaded Rod, 1 in. L	Stainless Steel	8984K22
12	10-24 Socket Cap Screw, 1/2 in. L	Black Oxide Alloy Steel	91251A242
4	1/4-20 Socket Cap Screw, 5/8 in. L	Black Oxide Alloy Steel	91251A539
4	1/4-20 Hex Nut	Stainless Steel	94819A043
1	2 in. OD x 1 in. ID x 0.1 in. Thick Washer	Zinc Plated Steel	90126A038
Swagelok Pipe Fittings			
Quantity	Desc.	Size	Swagelok Part #
1	Brass Integral Bonnet Needle Valve	0.73 Cv, 1/2 in. Tube Fitting	B-1RS8
1	Bulkhead Female Connector	1 /2 in. Tube OD x 1/2 in. Female NPT	B-810-71-8
1	Male 90 Deg. Elbow	1/2 in. Tube OD x 3/8 in. Male NPT	B-810-2-6
1	Pipe Fitting Adapter	3/4-16 Male SAE x 1/2 in. Female NPT	SS-8-SAE-7-8
T-Slot Hardware			
TS10-10 1 in x 1 in Extrusion			
Quantity	Length (in)	T-Slot Item #	
4	16	650000	
3	8	650000	
4	2	650000	
T-Slot Fasteners			
Quantity	Desc.	T-Slot Item #	
44	10S Economy T-Nut 1/4-20	651163	
48	10S 1/4-20 Button Head Socket Cap Screw	651019	
20	1/8 in. 2-Hole Inside Quarter Bracket	653047	
Miscellaneous			
Quantity	Desc.	McMaster Item #	
2 Ft	6 in. Schd. 80 PVC	48855K42	
1 Ft	1/2 in. OD x 0.625 in. Wall Comp. Air Tubing	52355K95	
2 Sq. Ft	0.0045 in. Wire Dia. Mesh, 53.3% Open Area	9230T19	
8 Ft	1/8 in. x 3/8 in. Adhesive Backed Silicone Foam	8694K125	
1 Roll	1/2 in. x 0.0032 in. Thread Sealant Teflon Tape	52355K95	
4 Ft	1/8 in. Dia. Soft O-Ring Cord Stock	9864K27	
1 Bottle	Loctite Quick Set 404	7569A22	
1 Part	1/2 in. Male NPT x 1/2 in. Quick Connect Stem	1077T23	
1 Part	1/8 in. Male NPT x 3/16 in. ID Male Hose	5346K11	
-	RP Material for Clamping Blocks	-

BLDS SUMMER 2011

DRAWN BY: WILL NEUMEISTER	INIT:	CKD BY:	INIT:
TOLERANCE: +/- 0.005	UNITS: INCHES	MATERIAL: 6061 ALUMINUM	
NEXT ASSY:	SCALE: 1:2	TITLE: TOP FLANGE	
DWG #:	DATE: 09/21/11	GROUP:

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CAL POLY

DRAWN BY: WILL NEUMEISTER	INIT:	CKD BY:	INIT:
TOLERANCE: +/- 0.005	UNITS: INCHES	MATERIAL: 6061 ALUMINUM	
NEXT ASSY:	SCALE: 1:2	TITLE: BASE FLANGE	
DWG #:	DATE: 09/21/11	GROUP:

5 4 3 2 1

BLDS SUMMER 2011

DRAWN BY: WILL NEUMEISTER	INIT:	CKD BY:	INIT:
TOLERANCE: +/- 0.010	UNITS: INCHES	MATERIAL: 6061 ALUMINUM	
NEXT ASSY:	SCALE: 1:1	TITLE: STAND BASE BLOCK	
DWG #:	DATE: 08/19/11	GROUP:

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CAL POLY

DRAWN BY: WILL NEUMEISTER	INIT:	CKD BY:	INIT:
TOLERANCE: +/- 0.010	UNITS: INCHES	MATERIAL: 6061 ALUMINUM	
NEXT ASSY:	SCALE: 1:3	TITLE: TOP COVER PANEL	
DWG #:	DATE: 08/19/11	GROUP:

Also cut 1 ea. 5 in. length section and 3 ea. 1 in. length sections

BLDS SUMMER 2011		DRAWN BY: WILL NEUMEISTER		INIT:	CKD BY:	INIT:
Solid Mechanics Engineering CAL POLY		TOLERANCE:	UNITS: INCHES		MATERIAL: SCH. 80 PVC PIPE	
		NEXT ASSY:	SCALE: 1:2		TITLE: PVC PIPE SECTIONS	
		DWG #:	DATE: 10/01/11		GROUP:

All fillets 0.125 radius unless specified.

BLDS SUMMER 2011				DRAWN BY: WILL NEUMEISTER	INIT:	CKD BY:	INIT:
SolidWorks Technical Edition. Hengger Engineering. CAL POLY				TOLERANCE: +/- 0.005	UNITS: INCHES	MATERIAL: 6061 ALUMINUM	
				NEXT ASSY:	SCALE: 2:3	TITLE: DUAL CLAMP ARM	
				DWG #:	DATE: 08/19/11	GROUP:

BLDS SUMMER 2011

DRAWN BY: WILL NEUMEISTER	INIT:	CKD BY:	INIT:
TOLERANCE: +/- 0.010	UNITS: INCHES	MATERIAL: ALUMINUM	
NEXT ASSY:	SCALE: 4:1	TITLE: FINE DOUBLE SCREW	
DWG #:	DATE: 08/19/11	GROUP:	

10-32 UNF-2B,
0.25 thread depth

2 x 0.15 fillets

BLDS SUMMER 2011

DRAWN BY: WILL NEUMEISTER	INIT:	CKD BY:	INIT:
TOLERANCE:	UNITS: INCHES	MATERIAL: RP	
NEXT ASSY:	SCALE: 3:1	TITLE: FRONT CLAMP BLOCK	
DWG #:	DATE: 09/26/11	GROUP:

BLDS SUMMER 2011

DRAWN BY: WILL NEUMEISTER	INIT:	CKD BY:	INIT:
TOLERANCE:	UNITS: INCHES	MATERIAL: RP	
NEXT ASSY:	SCALE: 3:1	TITLE: BACK CLAMP BLOCK	
DWG #:	DATE: 09/26/11	GROUP:	