Plasma Actuators: From Lab to Flight

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Contents
1 Introduction

1.1 The DBD Plasma Aktuator

First introduced in 1998, the dielectric barrier discharge (DBD) plasma actuator has attained considerable interest in the fluid mechanics community, especially in the area of flow control. The plasma actuator is a device composed of two very thin and long (tape like) electrodes attached to an aerodynamic surface. Their widths are of the order of a few millimeters. They are separated by a thin insulation film covering the lower, grounded electrode. The upper electrode remains exposed to the flow. Viewed from above, both electrodes are arranged side by side, separated vertically by the insulation film. The whole device can be built thinner than one millimeter (wall normal thickness) and can be recessed flush with the surface.

If a sufficiently high AC voltage is applied between the electrodes, the periodic electric field created above the dielectric surface causes ionization of surrounding air molecules. The necessary ionization voltage depends on the dielectric material and is typically of the order of $10kV_{pp}$ for actuators similar to the one used in the investigations discussed in this manuscript. The charged molecules are accelerated in the electric field of the weakly ionized plasma and, by collision with neutral air molecules, transfer momentum into the fluid. In the case of quiescent air above a solid surface, this induces a flow tangential to the wall. As the actuator represents a zero-net mass-flux device, the tangential acceleration is accompanied by a smaller wall-normal velocity component, i.e. a downward movement towards the exposed actuator electrode. If the actuator is applied in a boundary-layer flow, it increases the wall-tangential momentum enabling a change of the boundary-layer properties.

Dielectric barrier discharge (DBD) plasma actuators have been proposed for a wide range of flow control situations, although they have yet to reach the true application stage. An overview of possible applications is provided by Moreau [111] or Corke et al. (e.g. [32, 33]). Despite the large number of publications on this topic, there still remain some very basic questions regarding performance, effectiveness and efficiency of these actuators. The investigations and their results discussed in Chapters 2 and 3 represent a
considerable effort to quantify and to control the performance, effectiveness and efficiency of plasma actuators. These developments were a prerequisite for the success of the flow-control experiments discussed in Chapters 4 and 5.

1.2 Transition Control using Plasma Actuators: Preliminary Work

The boundary-layer control investigations discussed in Chapters 4 and 5 are based on a few very simple proof-of-concept experiments that were conducted and published during the years 2005 to 2008. Plasma actuators were very popular at that time and numerous experiments were conducted by many groups. The control of boundary-layer separation on airfoils at low Reynolds numbers attracted the most attention. Only a few fundamental experiments on the control of laminar boundary layers of a flat plate were conducted.

Although Velkoff and Ketcham \cite{160} had used corona discharges earlier to obtain a delay of transition at free-stream speeds around 50m/s, this approach was not further developed. The main reason may be the necessary installation of corona wires above a flat plate, representing a fragile and obstructing instrument for the control of laminar boundary layers. Moreau et al. \cite{112} used flush-mounted corona wires to alter the boundary-layer profile shape on a flat plate to obtain a significant drag reduction at flow velocities up to 25m/s. Jacob and Ramakumar \cite{80} investigated the impact of a single DBD actuator on a laminar boundary-layer profile at low speeds and measured a decrease of the displacement thickness and the momentum loss thickness.

The author first demonstrated that DBD actuators can delay artificially promoted transition on a flat plate inside a wind tunnel in \cite{71}. It was shown that this can either be achieved by actively cancelling single frequency disturbances (Tollmien Schlichting waves) or by using the added momentum for a stabilization of the flow \cite{72}.

The inspiration for the boundary-layer stabilization efforts \cite{71} came from the experimental and numerical investigations conducted for Lorentz force actuators applied in weakly conducting fluids (MHD flows). In Weier \cite{166} it has been shown that a correctly adjusted wall parallel Lorentz force alters the velocity profile to a significantly more stable velocity distribution. In
Albrecht et al. [4] the authors performed direct numerical simulations to investigate the effect of the Lorentz force on the transition, demonstrating successful transition delay.

The inspiration for the active wave cancellation came from the Technical University Berlin (e.g. [15], [150], [149]), although the history of active wave cancellation extends much farther back to 1965 [165]. In experiments performed by the author ([72] and [73]) the applicability of plasma actuators as actuators for active-wave cancellation was demonstrated.

These studies were conducted for the PhD thesis of the author [70] and are not further discussed in this manuscript.

These early transition-delay experiments using plasma actuators were conducted at low velocities and demonstrated the basic feasibility of using plasma actuators for this purpose. However, the large amplitudes of the TS waves, the necessity of an artificial excitation of waves to provide controlled conditions, and the low free-stream velocities were the main limitations for an application of both flow-control procedures under more realistic conditions. The present work describes the developments and innovations that were made in order to increase the technological level of plasma actuators for drag reduction purposes on aircraft.
1 Introduction
2 Performance Quantification

For many years, when working with plasma actuators, the daily laboratory experience was characterized by a considerable amount of variation in the performance of the applied plasma actuator and its effectiveness, resulting in varying success of the experiments. Numerous influential parameters were observed. However, a systematic analysis of their influence was difficult simply due to missing standards, criteria and procedures for the characterization of the performance of the plasma actuator at hand. Another major difficulty resulted from the great variety of ways to build and operate plasma actuators. Besides the geometric arrangement of the electrodes a great variety of materials is used as a dielectric: glass, ceramics, PMMA, polyimid, Mylar, PTFE, PCB and others with thicknesses ranging from a few hundred micrometers to centimeters. The operation parameters range from a few hundred Hertz to several tens of kilohertz and the voltages range from about one kilovolt to several tens of kilovolts. The operational parameters and the geometric parameters are coupled. A strong influence on the preferences of each laboratory results from the availability of power supplies and their certain capabilities, especially concerning the producible frequency.

It was mainly this variety of parameters that impeded the development of benchmark experiments and a common metric to reproduce experiments and to compare the performance or even the efficiency of different actuators from different labs.

The procedures and concepts presented in Chapter 2 became widely used and accepted standards for the performance characterization of plasma actuators, not only enabling the comparison of different actuators but also the systematic analysis of environmental influences, as discussed in Chapter 3.

Section 2.1 presents a comprehensive adaption and advancement of a previously known procedure for the analysis of gas discharges. The simultaneous measurement and processing of the driving voltage and of the charges that move to and from the actuator’s electrodes yields valuable insight into the electrical power consumption and the time-resolved properties of the
2 Performance Quantification

actuator during operation. The main focus is put on the time dependent measurement of the capacitance of the actuator. The analysis of the capacitance and its time dependence offers entirely new monitoring capabilities for the actuator’s performance and health.

While the analysis presented in Section 2.1 is purely of electrical nature, this new procedure is combined with mechanical measurements of the actuator’s force production in Section 2.2. Clear and reliable dependencies of the power consumption, actuator’s capacitance and the produced force magnitude are analyzed and discussed. Additionally, the analysis of the light emission of the discharge is introduced as a simple and reliable quantification of plasma actuator performance.

Knowing the force magnitude produced by a plasma actuator is not enough for a precise specification of an actuator for a certain flow-control application. In particular the application of plasma actuators for transition control turns out to be sensitive to variations of the force density distribution. The penetration depth of the body force in wall normal direction is of the same order as the boundary-layer thickness. Therefore, different force density distributions and different sizes of the body force volume have significant influence on the momentum transfer from the charges to the flow. Section 2.3 presents different methods to determine the force produced by a plasma actuator from optical velocity-field measurements. Integral methods, based on the momentum balance equation applied to control volumes, and differential methods, based on the solution of the Navier-Stokes equation or on the vorticity transport equation, are compared with each other and the differences among the force density distributions obtained with the differential methods are discussed.
2.1 Capacitance and Power Consumption

In this section emphasis will be placed on a proper capacitance determination of the operative plasma actuator, in order to allow advanced impedance matching of the entire electrical setup. The significance of impedance-matched systems for optimal energetic efficiency was identified by Chen [29] and emphasized by Singh & Roy [147]. To date, at least in terms of optimization and quantification, little attention has been directed to the electrical quantities of the actuator operation, such as operating voltage $V$, frequency $f$, consumed electrical power $P_A$ and corresponding capacitance $C$ of the plasma actuator. Roth and Dai [139] introduced a power-flow diagram, which considers reactive power, dielectric heating and plasma maintenance, to yield a phenomenological summary of power losses of the plasma actuator. However, for optimization purposes this description must be extended to quantify also the power fluxes.

The resonance behavior of any high voltage (HV) generation system affects the transferred power and the resulting efficiency of its HV generation in (at least) two manners: For a constant voltage the power increases with increasing frequency. This dependency is well described in various publications (e.g. Forte et al. [58], Roth & Dai [139]). With increasing operating voltage the power also increases, which directly affects the discharge intensity. Therefore, it is expedient to clarify the interaction between power consumption and corresponding discharge characteristics, where the power consumption is calculated using the area of a Lissajous figure (cyclogram) describing the discharge energy.

This approach leads to a measure of the temporal capacitance behavior of the DBD plasma actuator involving a re-derivation of Manley’s equations [106]. The analysis of such Lissajous figures recorded during the operation of a plasma actuator, is presented as a versatile and robust tool for determining not only the actual power consumption of any plasma actuator but also its time-resolved capacitance. The latter is not only useful for the previously mentioned impedance matching for efficiency optimization purposes but also for a precise characterization of the actuator and the discharge intensity.

2.1.1 Experimental Setup

The experimental setup used in this investigation is sketched in Figure 2.1. Two different HV generation systems and three different plasma actuator lengths $L$ were used, as specified in Table 2.1. This insures that the con-


2.1.2 Diagnostics of the Electric Power Consumption

2.1.2.1 Calculation of the Power Consumption

The consumed electrical power $P_A$ as a function of the high voltage $V$ is an established way to describe the electrical characteristics of the plasma actuator, as the examples in Figure 2.2 demonstrate. However, the universality of these results is limited, since they are obtained with different operational settings such as, for instance, different HV transformers, varying actuator

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6Label-code example: An experimental setup comprising the Voltcraft power supply, the GBS transformer, an actuator of length $L = 0.45\text{m}$ and a direct wiring of the function generator to the transformer is labeled 1ICb followed by a particular parameter setting, e.g. $10kV$ or $14kHz$. 

---

Figure 2.1: Experimental plasma-actuator setup comprising function generator (FG), power supply (PS), high voltage (HV) transformer and plasma actuator; components listed in Table 2.1.
Table 2.1: Setups of the chosen HV-generators. The codes of column 2 are used to abbreviate experimental details in the legends below below:

<table>
<thead>
<tr>
<th>Device</th>
<th>Code</th>
<th>Specifications</th>
</tr>
</thead>
<tbody>
<tr>
<td>Function generator (FG)</td>
<td></td>
<td>ELV MFG 9001M</td>
</tr>
<tr>
<td>Power supply (PS)</td>
<td>1</td>
<td>Voltcraft PS 3610</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>Kenwood KRF-V6010</td>
</tr>
<tr>
<td>High-Voltage (HV) transformer</td>
<td>I</td>
<td>GBS Elektronik Minipuls 2</td>
</tr>
<tr>
<td></td>
<td>II</td>
<td>Bosch automotive ignition coil</td>
</tr>
<tr>
<td>Plasma Actuator length</td>
<td>A</td>
<td>( L = 0.15) m</td>
</tr>
<tr>
<td></td>
<td>B</td>
<td>( L = 0.30) m</td>
</tr>
<tr>
<td></td>
<td>C</td>
<td>( L = 0.45) m</td>
</tr>
<tr>
<td>wiring</td>
<td>a</td>
<td>FG→PS</td>
</tr>
<tr>
<td></td>
<td>b</td>
<td>FG→HV</td>
</tr>
</tbody>
</table>

lengths and materials, electrical settings such as operating voltages and frequencies as well as variations of the state variables.

Grundmann et al. [73] and Borcia et al. [26] explain two common ways of calculating the consumed electrical power of an actuator by determining the operating voltage and either the current \( I \) or the charge \( Q \) by means of a probe resistor or capacitor, respectively. Furthermore, Grundmann et al. [73] conclude that it is more convenient to choose the capacitor concept, since the capacitor integrates the current passing through the actuator in time and thus captures all occurring micro-discharge pulses.

Figure 2.2: Graphical review of published voltage-power relations.
with an appropriate probe capacitor (see Appendix), which leads to a better signal-to-noise ratio compared to the resistor measurements. The measured probe voltage $V_p$ across the probe capacitor $C_p$ is proportional to the charge crossing the electrodes - independent of the bandwidth of single discharge events - and the capacitance is simply a proportionality factor (cp. Figure 2.1). The measured operating voltage and charge values are plotted against each other in $Q$-$V$ cyclograms, which are better known as Lissajous figures. Figure 2.3 shows such figures for two different dielectric barrier discharge types: the volume discharge (VD) including a gas gap, and the surface discharge (SD) as it occurs with DBD plasma actuators. Despite the fact that the signal-to-noise ratio of cyclogram-based measurements is much better than that of a direct measurement of the discharge current, further post-processing of the raw data, including filtering, becomes necessary to extract robust information from the cyclogram. Therefore, we have chosen a least squares filter based on the description of Savitzky & Golay [142] to prepare the raw data for further post-processing, which is shown in Figure 2.3(b).

The energy $E_k$ which is consumed per discharge cycle $k$ is defined by the area enclosed by the cyclogram and can be calculated by

$$E_k = \oint_k C_p V_p(t) dV = \oint_k Q(t) dV.$$  \hspace{1cm} (2.1)

Subsequent multiplication of $E_k$ with the plasma frequency $f$ and averaging over $K$ discharge cycles leads to the consumed actuator power

$$P_A = E_f = \frac{f}{K} \sum_{k=1}^{K} E_k.$$ \hspace{1cm} (2.2)

Depending on the adjustment of the oscilloscope (500kp/Ch, 1GS/s) the measured time traces result in $K \approx 800$ cycles á 625 data points, depending on the chosen plasma frequency ($4kHz < f < 18kHz$). The corresponding standard deviation of the calculated power lie below 3% in all cases ($\sigma_{P_A} < 3\%$).

Regarding the shape of a Lissajous figure, volume discharges (VD) lead to a parallelogram, since the discharge starts and stops with constant geometrical plasma shape, as sketched in Figure 2.3(a). This cyclogram was already discussed by Manley [106] in 1943 and is still an important diagnostic tool for DBD research, as recently demonstrated e.g. by [53, 63, 163, 114].
2.1 Capacitance and Power Consumption

Figure 2.3: Typical $Q-V$ cyclograms (Lissajous figures) of dielectric barrier discharges (DBD) with typical operating parameters; Characteristic quantities such as $V_{\text{max}}$, $Q_{\text{max}}$ and different capacitances - VD (a): $C, C_d$; SD (b): $C_0, C_{\text{eff}}$.

Unfortunately, volume discharges are seldom used for flow control, since at least one electrode must be located in the flow [139, 127].

Any surface discharge (SD) continuously changes the shape of its discharge volume throughout each cycle and thus changes its discharge capacitance as mentioned by Gibalov & Pietsch [63]. This effect results in an almond-shaped cyclogram, as sketched in Figure 2.3(b), which is well-known from various publications (e.g.[73, 111, 130]).

2.1.2.2 Power Law Characterization

To date, the electrical power $P_A$ consumed by plasma actuators is typically characterized as a power-law-based function of either frequency $f$ or operating voltage $V$. Although the exponents of these functions describe in principle the performance of the actuator, they are not comparable among different experiments or configurations. This underlines the need for more straight-forward and universal scaling laws.

A selection of previously published results that describe such $P_A = f(V)$ relations has been shown in Figure 2.2. Some research groups use a quadratic approximation [130, 37] (with and/or without offset) for the consumed power with increasing operating voltage $V$. Other groups report exponent values ranging between 2 and 3 [139, 84] or approximately $7/2$ [46, 47,
2 Performance Quantification

![Graph showing consumed power $P_A$ as function of actuator voltage $V$.](image)

Figure 2.4: Consumed power $P_A$ as function of actuator voltage $V$.

123, 100], depending on the geometry of the actuator and the experimental configuration. A comparison of these published values with our own results confirmed approximately an exponent value of $7/2$, which is shown in Figure 2.4 for several operational configurations.

Additionally, the relative power per unit actuator length $P_A/L$, is plotted in Figure 2.4(b), in contrast to the absolute values of Figure 2.4(a), as published by varying research groups (e.g. Grundmann & Tropea [73], Jolibois et al. [84] or Little et al. [100]). For this relative description the results obtained for different actuator lengths lie approximately on top of one another when operated at the same frequency. Despite this improvement in terms of universality there still remains significant differences for changes of the plasma frequency $f$, since $P_A$ is a function of the frequency as well. Most research groups (e.g. [130, 14, 58, 134, 1, 37]) describe this power-frequency function as a linear function ($P_A \propto f$). However, independent of the operating frequency $f$ this assumption consequently has to result in a constant consumed energy $E_k$ per cycle $k$ to satisfy equation (2.2), as explicitly described by Porter et al. [133]. Consequently, the enclosed area of the corresponding Lissajous figures achieved at a constant operating voltage would have to be constant over the entire measured frequency range. This assumption, in fact, is an oversimplification as demonstrated in [95].

In contradiction to previous publications, our results show a $P_A = f(f)$ relation according to the power of $3/2$ instead of a linear relation, as sketched in Figure 2.5. Consequently, according to equation (2.2) the consumed energy $E_k$ per cycle $k$ is indeed affected by the plasma frequency
2.1 Capacitance and Power Consumption

![Graphs of power consumption vs frequency](image)

(a) absolute

(b) relative

Figure 2.5: Consumed power $P_A$ as function of frequency $f$.

with the power of $1/2$.

However, for purposes of improving the predictability of results for varying operational conditions, the above two approaches

$$P_A \propto V^{\frac{3}{2}}$$  
(2.3)

$$P_A \propto f^{\frac{3}{2}}$$  
(2.4)

should be combined in the joint functional description

$$P_AL^{-\frac{3}{2}}f^{-\frac{3}{2}} \propto V^{\frac{7}{2}}$$  
and

$$\frac{P_A/L}{f^{\frac{3}{2}}V^{\frac{7}{2}}} = \Theta_A = \text{const.}$$  
(2.5a)

which includes the length $L$ of the actuator. A similar model has been reported by Roth et al. [140] in order to describe the power losses due to dielectric heating.

The results of equations (2.5) are plotted in Figures 2.6. It is obvious from Figure 2.6(a) that the former discrepancies vanish and all curves collapse better, which demonstrates the robustness and the improved generality of equation (2.5a) as a scaling law for describing the electrical characteristics of DBD plasma actuators. Now only one single experiment is required to calculate $\Theta_A$ for a particular configuration of the dielectric material (see Figure 2.6(b)).

However, even though this new scaling law (2.5) seems promising to describe and classify the consumed electrical power of DBD plasma actuators for varying operating conditions, this model does not include any information about the efficiency of operation.
2 Performance Quantification

Figure 2.6: Combined $P-V-f$ description for several operating conditions based on the scaling law (2.5).

2.1.3 Characterization of the Plasma-Actuator Capacitance

Chen [29] identified in 2002 that impedance matching of the electrical circuit of a plasma actuator system as a powerful means for optimizing plasma actuator setups. Subsequently, several studies concerning this topic have been undertaken, as reported e.g. by Opaits et al. [117, 118], Chen [30] or Singh & Roy [147]). The impedance of a plasma-actuator setup under operation is highly unsteady and depending on many parameters. Therefore, it is desirable to determine the capacitance of plasma actuators and furthermore to quantify the impact of the operating conditions (operating voltage and frequency) on the resulting plasma-actuator capacitance.

2.1.3.1 Capacitance Determination

Based on Manley’s equations [106] it is possible to derive several capacitances involved in the discharge process. For volume discharge (VD) this has been described e.g. by Falkenstein & Coogan [53] or by Wagner et al. [163]. However, a deeper insight into the discharge (of both, VD and SD) can be provided from Lissajous figures by calculating

$$C(t) = \frac{dQ(t)}{dV(t)},$$  \hspace{1cm} (2.6)
since the slope of the Q-V cyclogram corresponds to the capacitance $C(t)$ of the actuator at specific phase angles of the operating AC voltage $V(t)$ (see e.g. [63, 163]). For VD the capacitance $C$ can easily be calculated as a series combination of $C_g$ (gas) and $C_d$ (dielectric) following

$$\frac{1}{C} = \frac{1}{C_g} + \frac{1}{C_d} \quad (2.7a)$$

$$C = \frac{C_g C_d}{C_g + C_d} \quad (2.7b)$$

The former equation (2.7a) was introduced by Manley [106], the latter one (2.7b) was used in [101, 163]. Furthermore, Manley [106] mentioned (for VD) that the sharpness of the corners of the Lissajous figures denote the abrupt stop of the discharge at the operating voltage maximum (see Figure 2.3). This phenomenological study can be transferred to surface discharge as well, since it is in very good agreement with the present observations, where a sudden discharge-current drop can be identified in combined voltage/current plots at the locations of the operating voltage maxima (see e.g. [2, 46, 58, 134]).

Unfortunately, the serial description of equation (2.7) is invalid for surface DBD actuators since their discharge occurs in a stepwise growth [63] of the plasma channels, as described by Gibalov & Pietsch [63]. Enloe et al. [46], for instance, demonstrated the effect of transient chordwise growth of the plasma extent of DBD plasma actuators by means of a photomultiplier tube. Nersisyan & Graham [114] modified the pure serial description of equation (2.7) with an additional resistance in parallel to $C_g$ - still for VD purposes. Nevertheless, this model seemed to be useful for an SD description as well, as introduced by Van Dyken et al. [158] and Enloe et al. [46, 47] almost at the same time (2004), later by Pons et al. [130]. However, based on the model of Nersisyan & Graham, other modified plasma actuator model descriptions, which are based on capacitance assumptions, have been introduced e.g. by Singh & Roy [147] ($C_p - L_p - R_p - C_d$). An even more complex model has been suggested by Orlov et al. [123, 122, 121] and subsequently applied e.g. by Lemire et al. [99]. This so-called lumped-element circuit model already includes estimations about the spatial-temporal growth of the plasma. According to Orlov et al. [123] this model was calibrated based on the experimentally obtained luminosity data of Enloe et al. [45].
2 Performance Quantification

Figure 2.7: Capacitance of DBD plasma actuator (extract of a calculated time trace).

2.1.3.2 A New Capacitance-Quantification Strategy

Regardless of the chosen DBD setup (VD or SD) equation (2.6) yields the temporal evolution of the capacitance of the plasma actuator. Due to the parallelogram shaped cyclogram of the VD of Figure 2.3(a), the capacitance of the volume discharge \( C_{VD}(t) \) has a square-wave shape. The more complex case of surface discharge (SD) is shown in Figure 2.7. The corresponding Lissajous figure has already been introduced in Figure 2.3(b).

The lower plateaus of Figure 2.7 correspond to the so-called dark periods (Manley [106]) where no discharge occurs. These dark periods can be identified in luminosity diagrams of the discharge as well, as shown for instance by Enloe et al. [46, 47] for the temporal light emission and the spatial-temporal luminosity distribution. The values of these plateaus quantify the electrical pure passive-component (cold) capacitance of the plasma actuator, which will be referred to as \( C_0 \) in the following. It is important to note that \( C_0 \) can be alternatively measured across a disconnected and non-operative plasma actuator using a conventional multimeter, and the result agrees with the method described here.

The upper plateau corresponds to the effective capacitance \( C_{eff} \), consisting of a combination of the passive-component \( C_0 \) and the contribution of the plasma itself to the capacitance. Since \( C_{eff} \) comprises the real electrode and the above-mentioned virtual electrode size, \( C_{eff} \) provides a quantity for defining the virtual capacitance of the operative plasma actuator. Based

\[ \text{The vertical peaks occur periodically at } \pm V_{\text{max}}, \text{ since the denominator of the expression } dQ/dV = C \text{ periodically changes its sign while ranging around zero.} \]
2.1 Capacitance and Power Consumption

Figure 2.8: Capacitance histogram: Relative occurrence of capacitance values derived from (2.6) for varying operating voltages (a), frequencies (b) and for both half-cycles (c).

on this characterization, it is therefore possible to quantify the magnitude of the load connected to the HV power supply and consequently the load-resonance behavior of the entire system for varying operational settings.

2.1.4 Capacitance-Histogram Analysis

Due to the difficulty in extracting robust numerical values from the gray-shaded capacitance ranges in Figure 2.7, a good estimation of $C_0$ and $C_{eff}$ can be obtained by determining the local maxima in the capacitance histogram, as demonstrated in Figure 2.8 for varying operating voltages and frequencies, both resulting in varying power levels.
Such capacitance histograms yield insight into several phenomena: A comparison of the histograms of Figure 2.8 demonstrates the independence of the lower peak $C_0$ from the discharge. This result confirms the assumption of a pure passive component character of $C_0$. Consequently, it can be concluded that the value of $C_0$ represents the constant cold capacitance of the plasma actuator. Secondly, a linear growth of the effective capacitance $C_{\text{eff}}$ can be seen for both, increasing operating voltages (Figure 2.8(a)) and increasing frequencies (Figure 2.8(b)), where the impact of the operating voltage is larger than that of the frequency. This obviously occurs due to the increasing power levels with either of the parameters ($P_A \propto V^2$, $P_A \propto f^2$), as discussed in Section 2.1.2.2. The weak impact of the frequency comes as no surprise, since combined analysis of equations (2.2) and (2.4) leads to $E \propto f^{1/2}$. This results in an even weaker impact of the frequency on the slope of the corresponding cyclogram for a constant operating voltage $V$ and cold capacitance $C_0$.

Furthermore, the light gray color in the range between the two peaks corresponds to the transient growth of $C(t)$ for the growing plasma length from $C_0$ to $C_{\text{eff}}$ of a surface DBD within every discharge cycle. This growth can be recognized from the Lissajous figures as well (cp. Figure 2.3(b)), since the counter-clockwise orientated time trace of this cyclogram and its periodically increasing slopes already suggest the observed transient growth.

The involved chemistry of the two half-cycles differs and results in two different discharges, as demonstrated by Gibalov & Pietsch [63] and Enloe et al. [49, 51]. Consequently, it is important to cross-check the resulting effective capacitance values for positive and negative half-cycles, $C_{\text{eff}}^+$ and $C_{\text{eff}}^-$, respectively (see Figure 2.3(b)). An example of such a distinction of half-cycles is given in Figure 2.8(c). All above mentioned effects qualitatively look alike for the two half-cycles, even though the peak values and widths differ slightly. This small difference occurs due to the fact that the discharge of the positive half-cycle occurs in the streamer mode, which is much noisier compared to the glow-type discharge that characterizes the negative half-cycle, as described by Orlov et al. [120]. The different noise levels can also be identified from current plots $I(t)$ as published e.g. in [105, 58, 125, 135, 27]. Nevertheless, the impact of the discharge type to the resulting effective capacitance is of minor importance, since the peak values for the effective capacitance only differ slightly ($C_{\text{eff}}^+/C_{\text{eff}}^- \approx 1$).

The peak values of $C_0$ and $C_{\text{eff}}$, extracted from the histograms, as demonstrated in Figure 2.8, are plotted in Figure 2.9 for the same operational
settings as previously discussed in Section 2.1.2.2 and plotted in Figures 2.4 and 2.5. The results of all configurations consist of two branches. The lower branches highlight the constant pure-passive-component character of the plasma-actuator’s cold capacitance $C_0$ during the above-mentioned dark periods. The upper branches of Figure 2.9 quantitatively characterize the non-constant effective capacitance $C_{\text{eff}}$ during the discharge as a function of the operating voltage (Figure 2.9(a)) and frequency (Figure 2.9(b)). Therefore, both capacitances ($C_0$ and $C_{\text{eff}}$) should be taken into account for the chosen experimental setup, if advanced impedance matching is desired.

However, it can also be recognized from Figure 2.9 that the actuator length influences the upper branches slightly, since the slopes for the short actuators (crosses) are slightly above those for the long actuators (open symbols). This effect can be explained by the occurrence of 3D effects at the ends of the actuator, which results in an enlarged total discharge area compared to the length of the electrode. With increasing actuator-length this edge effect vanishes and a more ideal 2D situation is realized.

For the plasma actuator materials and dimensions chosen for the present study, the branches of length-related passive-component capacitance $C_0/L$ collapse at a constant value of 80 pF/m over the entire range of investigated operating conditions. In contradiction to this, the length-related effective capacitance $C_{\text{eff}}/L$ more than doubles its value from $\approx 200$ pF/m at 7 kV to $\approx 450$ pF/m at 14 kV, which is more than five times the cold capacitance value (see Figure 2.9(a)). From this significant difference, the necessity of carefully matching the electrical setups for individual applica-

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2.1 Capacitance and Power Consumption

![Figure 2.9](image)

(a) voltage  
(b) frequency

Figure 2.9: Plasma actuator capacitances $C_0$ and $C_{\text{eff}}$ for varying operational configurations (cp. Figures 2.4 and 2.5).
tions is emphasized, which additionally requires knowledge of the operating conditions.

2.1.5 Resume Section 2.1

The electrical performance of DBD plasma actuators in quiescent air has been quantitatively characterized, providing a means of universally comparing different actuators and different electrical systems. A new scaling law for the actuator performance has been introduced, incorporating new insights concerning the influence of actuator size, operating voltage and frequency. In agreement with previous literature the power law \( P_A \propto V^{7/2} \) is confirmed to be appropriate for characterizing the relation between operating voltage \( V \) and consumed actuator power \( P_A \). In contradiction to the previously published results, we observe a power law \( P_A \propto f^{3/2} \) to describe the power consumption / frequency interaction, which was formerly assumed to be a linear relation. Hence, it can be concluded that the consumed energy \( E_k \) per cycle \( k \) is indeed affected by the operating frequency. An a priori prediction of performance can be achieved by means of the newly introduced scaling number \( \Theta_A \), which has a constant value for varying operational settings for a particular configuration of the dielectric material. This number is extremely beneficial, since only one single experiment is required to determine the value for \( \Theta_A \).

A new post-processing strategy is suggested, which is based on a statistical analysis of the slopes of voltage-charge cyclograms (Lissajous figures). From this analysis the two characteristic capacitances \( C_0 \) and \( C_{\text{eff}} \) are extracted to quantify the constant cold capacitance and discharge-specific effective capacitance of the plasma actuator. Since \( C_{\text{eff}} \) comprises the real electrode of a plasma actuator and the discharge-specific virtual electrode-size, this quantity characterizes the magnitude of the load connected to the HV power supply and consequently affects the resonance behavior of the entire system. For the plasma actuator materials and dimensions chosen for the present study, \( C_{\text{eff}} \) exhibited values up to five times \( C_0 \). This large difference demonstrates the necessity of carefully matching the electrical setups for the individual application, including knowledge of the operating conditions.
2.1 Capacitance and Power Consumption

The investigations and results discussed in this section have been published in:

2 Performance Quantification

2.2 Performance and Force Magnitude of Plasma Actuators

A new procedure of determining the time resolved capacitance of a plasma actuator during operation was introduced in the previous section, representing a simple diagnostic tool that provides insight into the phenomenological behavior of plasma actuators. The procedure is now demonstrated by presenting example correlations between consumed electrical energy, size of the plasma region and the operating voltage. It is shown that the capacitance of a plasma actuator is considerably increased by the presence of the plasma; hence a system which has previously been impedance matched can be considerably de-tuned when varying the operating voltage of the actuator. Such information is fundamental for any attempts to increase the energy efficiency of plasma-actuator systems. A combined analysis of the capacitance, light emission, size of the plasma region, force production and power consumption is presented.

One of the most relevant topics for future applications of plasma actuators is the efficiency of the force production. The most obvious measure to improve the electrical efficiency of a plasma actuator system is the impedance matching of the power supply to the capacitance of the actuator, i.e. operating the actuator at the resonance frequency. In order to do that, it is necessary to know the capacitance of the actuator. It is shown that the capacitance is not only considerably unsteady during operation but is also strongly dependent on the operating voltage.

Simple light emission measurements provide additional valuable information in combination with the temporal capacitance measurements. The previously reported dependency of the light emission on the consumed power (Enloe et al. [46, 47]) is in good agreement with the insight obtained by the capacitance measurements. In this section it is demonstrated that the chordwise length of the plasma region can be easily determined without a careful calibration. The results from these investigations support the conclusions that are drawn from the Lissajous figures.

Force measurements based on a balance and a rocker have been conducted simultaneously with the above mentioned electrical and optical measurements. Although both light emission measurements and force measurements have been previously performed in separate experiments, the results presented here have been obtained simultaneously and are correlated with several features of the recorded Lissajous figures.
2.2 Performance and Force Magnitude of Plasma Actuators

2.2.1 Experimental Setup

The experimental setup comprises three measurement systems, as sketched in Figure 2.10. The plasma actuator is mounted on an acrylic plate, which is connected to a weight balance (KERN PCB 250-3 precision balance, accuracy 1 mg) using a $H_1/H_2 = 3 : 1$ rocker to amplify the actuator thrust. An overhead mounted CMOS camera (Phantom V12.1, 1280×800 pixels, 24 fps; Nikon 105 mm, AF Micro NIKKOR f/2.8D) has been used to record the time-averaged light emission of the discharge during the investigation of the electrical operation parameters (see also Figure 2.13). The electrical measurements were obtained using an oscilloscope (LeCroy WaveJet354, 4CHs, 500kp/Ch, 1GS/s) to record the operating voltage $V$ (Testec HVP-15HF, 1000:1) and the voltage $V_p$ across the charge-probe capacitor $C_p = 22$ nF (LeCroy PP006A, 10:1), as depicted in Figure 3.4(a). The operating voltage $V$ is generated by a high voltage transformer (GBS Elektronik, Minipuls 2), which is driven by a laboratory power supply (Voltcraft PS 3610) and a function generator (ELV MFG 9001M).

Plasma actuators of two different lengths $L$ were investigated in order to match the size of the wind tunnel facilities being used, i.e. $L_1 = 0.17$ m (cp. Duchmann et al. [41]) and $L_2 = 0.44$ m (cp. Grundmann [71, 72, 70]). Since the influence of the size of the upper electrode is negligible for pure electrical plasma actuator experiments (see Möller [110]), the dimensions $w_1 = 2.5$ mm, $w_2 = 10.0$ mm and $d = 0.4$ mm (Kapton®) were chosen for upper and lower electrodes and the dielectric thickness, respectively. This particular parameter setting turned out to be the optimal configuration for transition delay, according to the experience of Grundmann & Tropea [71] and the characteristic diagnostics by Kriegseis et al. [92, 91].

2.2.2 (Post) Processing Strategy

2.2.2.1 Power Consumption and Capacitance

The electrical power consumption and the temporally resolved capacitance are calculated by means of voltage-charge-cyclograms (Lissajous figure), which was suggested for much simpler volume discharges by Manley [106] in 1943 and is described for the surface discharges of DBD plasma actuators in the previous chapter and in Kriegseis et al. [95].

In order to ensure that the following conclusions apply for different power levels of the plasma actuator, all experiments have been conducted on different levels of electrical power consumption. Each power consumption
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Figure 2.10: Experimental setup: (a) Photograph and (b) sketch of the test rig comprising overhead camera (CAM), weight balance (WB), rocker ($H_1/H_2$); (c) detailed view of measured electrical quantities: $V$ actuator voltage, $V_p$ charge-probe voltage, $C_p$ charge-probe capacitor, chordwise plasma distribution.
value has been adjusted by combining different operating frequencies $f$ and operating voltages $V$. The consumed power $P_A$ and of the maximal value of $C_{\text{eff}}$ of these experiments are plotted in Figure 2.11 as a function of the operating voltage $V$ for various frequencies ($8 \text{ kHz} \leq f \leq 13 \text{ kHz}$). It is obvious that the required operating voltage for a particular power level $P_A$ increases with decreasing frequency $f$, as emphasized by a gray dashed iso-power level in Figure 2.11(a). This conclusion is in good agreement with Roth & Dai [139], Dong et al. [37] or Forte et al. [58], for instance. Extensive measurements of the power consumption have been conducted for different actuator lengths and operating parameters by Kriegseis et al. [95]. The results shown there confirm that the dissipated electrical power-per-meter actuator length can reliably be determined.

The effective capacitance $C_{\text{eff}}$ for the same actuators and operating conditions as used in Figure 2.11(a) are shown in Figure 2.11(b). The capacitance $C_{\text{eff}}$ increases with increasing operating voltage $V$. At first glance one could conclude that the dependency of the maximum capacitance of the plasma actuator shows a linear dependence on the operating voltage. Additionally it seems that operating the plasma actuator at different frequencies does not influence this seemingly linear dependence. If this were true, the maximum slopes in the Lissajous figure would be independent of the operating frequency. For instance, Porter et al. [133] assume that the dissipated energy per cycle $E = P_A/f$ of the operating voltage is constant for fixed operating conditions, not dependent on the operating frequency. Our experiments show a non-constant value of the dissipated energy-per-cycle for different operating frequencies, as illustrated exemplarily in Figure 2.12(b). Therefore, the Lissajous figures corresponding to different operating frequencies at the same operating voltages change their shape (enclosed area) and also change their maximum slopes (maximum capacitance), which can be clearly identified from Figure 2.12(a). Following these observations the maximum capacitance depends linearly on the operating voltage, but it is also dependent on the operating frequency.

The impedance and therefore the resonance frequency of a plasma actuator system consisting of the high-voltage generator and the actuator obviously depends on the capacitance of the plasma actuator. This capacitance has been shown to be composed of two parts: the passive component (cold) capacitance $C_0$ and the capacitance of the plasma that has a maximal value of $C_{\text{eff}} - C_0$. Obviously, the time dependent capacitance of the plasma can easily increase the total capacitance of a plasma actuator during operation by a factor of four as compared to the cold capacitance.
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Figure 2.11: Power consumption $P_A$ (a) and actuator capacitance $C_{\text{eff}}$ (b) as function of voltage $V$ for several frequencies $f$. 
2.2 Performance and Force Magnitude of Plasma Actuators

Figure 2.12: Direct comparison of energy per cycle $E$ for frequencies $f = 9$kHz and $f = 12$kHz at fixed operating voltage $V = 8.8$kV; (a) Lissajous figures (b) enclosed area specifying $E$.

This is of essential importance when thinking about impedance matching in order to increase the energy efficiency of the force production. The resonance frequency of a plasma-actuator system does not only depend on the actuator length but also considerably on the operating voltage. This is especially of great importance if the chosen operating frequency is close to the resonance frequency of the entire system. It can happen that one moves across the resonance frequency due to this effect; hence the slopes of impedance-matching measures will then change sign.

2.2.2.2 Light Emission

The close relationship between the electrical power consumed by an actuator and the light emission is described e.g. by Enloe et al. [46, 47]. Based on these correlations a cause-effect-relation between emitted light and the resulting flow characteristics has already been demonstrated by Kriegseis et al. [90]. Pavon et al. [125] demonstrated, that the light emission of the species $i$ involved in the discharge is uniformly distributed over the species-dependent wavelengths $\lambda_{\text{DBD}}^i$. Therefore, the measurement techniques for recording the emitted light do not necessarily have to resolve information according to wavelength. Consequently, photomultiplier tubes or high-resolution cameras can be used to obtain results with high temporal and/or spatial resolution, without the need to take care of detecting special wavelengths. The latter technique is used here, as illustrated in Figure 3.2(a).
Figure 2.13 shows a photograph of a typical plasma-actuator discharge, where additionally the coordinate system and measurement domain of the light emission analysis are highlighted.

The chosen exposure time of $\Delta t = 40\text{ms}$ for the experiments is two orders of magnitude longer than the discharge. Therefore, each raw image $j \in J$ corresponds to a time integration of the spatio-temporal luminosity distribution $L(x, y, t)$. Averaging the $J = 100$ images yields the spatial gray-value distribution

$$
\bar{g}(x, y) = \frac{1}{J} \sum_{j=1}^{J} g_j(x, y), \quad \text{where}
$$

$$
g_j(x, y) \equiv \int_{\Delta t_j} L(x, y, t)dt.
$$

The discharge structure is characterized by Orlov et al. [120] for the two half cycles of the AC cycle. They distinguish a glow or Townsend-type discharge for the negative half cycle and a streamer type discharge for the positive half cycle. Kriegseis et al. [95] demonstrated that this distinction does not affect the large scale properties of the discharge, i.e. the effective capacitance $C_{\text{eff}}$. The standard deviations $\sigma_\bar{g}$ of the $J$ light-emission distributions and $\sigma_{P_A}$ of the power consumption are compared. Both lie below 5%, in order to assure that (2.8a) is a convenient and valid simplification of the discharge considering its temporal evolution.

Gherardi et al. [62, 61] and Choi et al. [31] investigated the transition from glow to streamer discharge at atmospheric conditions for nitrogen and air, respectively. This insight is of great importance, since most plasma-actuator flow control applications operate at ambient conditions, i.e. within the range of the discharge transition. Enloe et al. [48] reported a strong effect of oxygen presence on the force production, but only minor effects
2.2 Performance and Force Magnitude of Plasma Actuators

on the plasma structure itself. Therefore, the filamentary structure of the discharge has to be taken into account and discussed for the presented light emission analysis.

The spanwise averaged light intensity \( G(x) \) and corresponding standard deviation \( \sigma_G \) are estimated according to

\[
G(x) = \frac{1}{N_y} \sum_{i=1}^{N_y} \bar{g}(x, y_i) \tag{2.9}
\]

and

\[
\sigma_G = \sqrt{\frac{1}{N_y - 1} \sum_{i=1}^{N_y} \left[ \bar{g}(x, y_i) - G(x) \right]^2}. \tag{2.10}
\]

The resulting gray-value distributions \( G(x) \) are shown in Figure 2.14(a) for several power levels. For the most filamentary case at \( V = 12.3 \text{kV} \) gray values \( G(x) \) and corresponding standard deviations \( \sigma_G \) are shown in Figure 2.14(b). This diagram additionally contains information of the relative peak intensity

\[
\hat{G} = \frac{G_p - G_b}{G_b} \tag{2.11}
\]

and the chordwise plasma (illumination) length

\[
\Delta x = x_{\text{max}}(G(x) > G_b) - x_{\text{min}}(G > G_b). \tag{2.12}
\]

\( G_b \) and \( G_p \) are the gray values of background and light-emission peak, respectively. For the sake of completeness it should be noted that in literature \( \Delta x \) is usually referred to as either plasma length \([95, 6, 36, 152]\) or plasma extent \([161, 19, 132]\).

It is important not to confuse the high standard deviation \( \sigma_G \) of the gray values with a high uncertainty or error. Rather, it concerns the number of filaments that occur along the spanwise coordinate \( y \) for the streamer mode \([62, 61, 31, 48]\), which can be recognized in Figure 2.13. Regardless of its origin, \( \sigma_G \) can be neglected at chordwise locations, which are relevant for \( \Delta x \) estimation, as demonstrated in Figure 2.14(b).

The resulting plasma lengths \( \Delta x \) are displayed as a function of operating voltage \( V \) in Figure 2.15(a). A proportional increase of the plasma length can be identified for increasing voltage \( V \). In order to tie in with the results of section 2.2.2.1 the effective capacitance \( C_{\text{eff}} \) is displayed as a function of the plasma length \( \Delta x \) in Figure 2.15(b).
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Figure 2.14: Gray-value distribution $G(x)$ along the chordwise coordinate $x$ (cp. Figure 3.4(a)); (a) for several operating voltages $V$ and power levels $P_A$; (b) at $V = 12.3$ kV with corresponding standard deviation $\sigma_G$ and sketch of characteristic quantities $\Delta x$, $\hat{G}$.
2.2 Performance and Force Magnitude of Plasma Actuators

Figure 2.15: Plasma length $\Delta x$ as function of voltage $V$ (a) and corresponding actuator capacitance $C_{\text{eff}}$ as function of plasma length $\Delta x$ (b) for several frequency $f$. 
Figure 2.16: Non-dimensional relative peak intensity $\hat{G}$ as function of operating voltage $V$ for several frequencies $f$.

The curves of all measured frequencies $8 \leq f \leq 13$ kHz collapse in Figure 2.15(b). This demonstrates the close correlation between $\Delta x$ and $C_{\text{eff}}$, which is independent of other (operating) parameters such as $V$ and/or $f$.Apparently, the plasma domain appears as a virtual enlargement of the exposed electrode, where the chordwise plasma length $\Delta x$ scales with the effective capacitance $C_{\text{eff}}$ of the plasma actuator and consequently defines the effective size of the load in the electric circuit.

Therefore, the determination of the chordwise extension of the plasma along the dielectric layer by means of light emission analysis is a promising measure for plasma actuator performance quantification purposes. Provided that the sensitivity of the camera is high enough, this value can be measured without any calibration, which makes the application of such light emission diagnostics a robust and simple procedure. Examining at the Figures 2.11(b), 2.15(a) and 2.15(b) it appears that robust empirical relationships of the form $C_{\text{eff}} \propto \Delta x \propto V$ can be derived, where the operating voltage $V$ additionally requires information about the plasma frequency $f$ for an unambiguous definition of the actuator-performance level.

Despite the occurrence of streamers and the corresponding high standard deviation $\sigma_G$ in the exposed electrode’s immediate vicinity, the non-dimensional peak intensity $\hat{G}$ reveals useful information about the discharge intensity. Although careful calibration would be necessary in order to achieve quantitatively comparable results for different experiments, the linearly increasing relation of operating voltage $V$ and peak intensity $\hat{G}$
is a convenient measure to verify the respective electric measurements, as demonstrated in Figure 2.16.

2.2.2.3 Integral Force and Resulting Thrust Production

Since the plasma actuator is operated as a flow control device, it is important to compare the pure electrical quantities with the fluid mechanical results. The thrust of the wall jet is a convenient measure to quantify the effectiveness of a plasma actuator, as emphasized e.g. by Abe et al. [1, 2], Enloe et al. [47] or Thomas et al. [153]. Experimentation of the temporal evolution of force production is a rather challenging topic, as demonstrated by Enloe et al. [52] and Porter et al. [134]. Since the push-push vs. push-pull interpretation of the plasma physics has not yet been clarified, ongoing research concentrates on both time-dependent and steady force measurements. A steady force $F$ can easily be measured by means of a weight balance, as sketched in Figure 2.10 and described e.g. by Hoskinson et al. [76, 78, 77] or Gregory et al. [69]. This force $F$ should not be understood as the body force $F_b$ produced by the actuator, since it is actually the body force $F_b$ minus some unknown wall friction force $F_f$ on the surface downstream of the actuator up to the end of the plate carrying the actuator, i.e.

$$F = F_b - F_f. \quad (2.13)$$

This fact is usually neglected in the literature. Enloe et al. [52] distinguished the contribution of the two half-cycles on the plasma force production by means of a torsional pendulum and demonstrated the presence of a self-induced drag counteracting this plasma force. Furthermore, there have been a few attempts to calculate the friction force from PIV measurements of the wall jet (e.g. Versailles et al. [161], Albrecht et al. [5]). Due to the very thin wall jet near the electrodes, the relatively high velocities there and the additional uncertainty about the unsteady situation in terms of laminar-turbulent transition, this method is not yet considered reliable. Nevertheless, being aware of the friction force term of (2.13), then a comparison can be made on the basis of the usable force $F$, which corresponds to the resulting amount of thrust released by the body force $F_b$.

According to the reports of Ferry & Rovey [54] the measured weight-balance signals of operative plasma actuators can be considerably affected during experimentation. This issue is taken into account by conceiving a robust processing strategy, which allows noisy and/or drifting behaviors...
to be handled: The force measurements were conducted by repeatedly operating the actuator, which allowed a multi-step analysis of the resulting weight balance signal $W(t)$. An example of a such measured time trace is sketched in Figure 2.17, where additionally all required (intermediate) quantities are emphasized. First, the averaged balance signal $\overline{W}_n$ and corresponding standard deviation $\sigma_{W_n}$ are calculated for each plateau $n \in N_W$ separately. The ranges of excluded data are identified from the balance-signal derivative $\partial W/\partial t$, as shaded gray in Figure 2.17.

Subsequently the (averaged) force $F$ produced by the plasma actuator can be estimated according to

$$F = \frac{H_2}{H_1} \frac{g}{N_W - 1} \sum_{n=1}^{N_W-1} \Delta W_n,$$

where

$$\Delta W_n = |\overline{W}_{n+1} - \overline{W}_n|.$$  \hspace{1cm} (2.14a)

In contradiction to the previously published reports of non-constant balance signals by Ferry & Rovey[54], neither $\sigma_W$ nor $\sigma_{\Delta W}$ showed significant values for the presented experiments. Therefore, the introduced (post-) processing approach is an appropriate robust measure for estimating the wall-jet thrust, which corresponds to the plasma actuator force $F$.

An example of balance based plasma actuator force results is shown in Figure 2.18. For lower voltages $V$ an increasing slope for the force $F$ is observed, which develops into a linear relation for higher voltages. This result
is in good agreement with reports of Van Dyken et al. [158] or Thomas et al. [153]. Furthermore, two different actuator lengths $L_1 = 0.17$m and $L_2 = 0.44$m were used in order to ensure that the influence of friction at the pivot bearings can be neglected, as demonstrated in Figure 2.18(b).

### 2.2.3 Results

For the simultaneously conducted experiments the operating frequency was varied systematically in the range $f = 8 - 13$kHz. However, only the long actuator $L_2 = 0.44$m was used in order to obtain the best possible SNR for the balance measurements. A subsequent calculation of universally valid relative values has been carried out, since the experimental setup works accurately enough, as demonstrated in Figure 2.18(b). The results are shown in Figure 2.19.

Here the resulting force $F/L$ is presented as a function of several discharge specific quantities. For instance, Figure 2.19(a) shows the actuator force as function of the operating voltage. In good agreement with the results of Thomas et al. [153] an increasing force is observed for increasing frequencies. This result comes as no surprise, since it is well known that both operating voltage and frequency significantly affect the resulting performance of plasma actuators[95]. Consequently, in order to quantify any DBD actuator scenario, either both variables have to be presented together or the more precise correlations as introduced and discussed in section 2.2.2.2 should be chosen.

A force-power diagram is presented in Figure 2.19(b). Such a plot has already been shown by Van Dyken et al. [158] and subsequently led to the dimensioned coefficient of the so-called force (production) efficiency $\eta = \frac{F}{P_A} = \frac{F/L}{P_A/L}$ (cp. Gregory et al. [69], Hoskinson et al. [78, 77], Ferry & Rovey[54]). Additionally, based on the above processing strategies Figures 2.19(c) and 2.19(d) show the actuator force $F/L$ as function of the plasma length $\Delta x$ and discharge capacitance $C_{eff}$.

A comparison of Figures 2.19(a) and 2.19(c) shows that the slopes are identical for the different experiments. In addition to that, the resulting plots in the $\Delta x - F/L$ diagram of Figure 2.19(c) collapse for the entire measured frequency range $f = 8 - 13$kHz. This result demonstrates the improved robustness of plasma-length based description of the actuator force, since the complex presentation of combined voltage-frequency parameter information is no longer necessary.

A similar comparison can be performed with Figures 2.19(b) and 2.19(d).
Figure 2.18: Plasma actuator force for two actuators lengths $L$ ($L_1 = 0.17m$ and $L_2 = 0.44m$) at $f = 10kHz$ and various voltages $V$; (a) absolute force $F$, (b) relative force per meter length $F/L$. 

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![Diagram of force production as function of discharge specific variables for several frequencies]

Figure 2.19: Plasma actuator force $F/L$ as function of discharge specific variables for several frequencies $f$. (a) operating voltage $V$; (b) consumed power $P_A/L$; (c) plasma length $\Delta x$; (d) effective discharge capacitance $C_{\text{eff}}/L$. 
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The slopes of these two diagrams look similar, as well. Therefore, it is possible to easily switch between the two representations depending on the purpose of the measurement. Figure 2.19(b) is favorable to document and discuss results of precisely adjusted power levels, whereas Figure 2.19(d) should be preferred for adjusting the impedance of the actuator system. Since both representations can be derived from one and the same measurement, it is strongly recommended to calculate both, the consumed power $P_A$ and the effective discharge capacitance $C_{\text{eff}}$.

2.2.4 Resume Section 2.2

Simple optical measurements of the chordwise length of the plasma region $\Delta x$ and weight balance based measurements of the resulting actuator force $F$ reveal simple relations between operating voltage $V$, power consumption $P_A$, actuator capacitances $C_0$, $C_{\text{eff}}$, light emission intensity $\hat{G}$, size of the plasma region $\Delta x$ and the produced actuator force $F$. A close correlation between the plasma size and the effective discharge capacitance is identified from the experiments. Both quantities have proven to be appropriate to describe the resulting plasma actuator force without the formerly required detailed knowledge of operating voltage and frequency. Combined analysis of the simultaneously obtained data provides a deeper insight into the interaction of these variables.

From these interactions the conclusion can be drawn, that a combined analysis of power consumption $P_A$, effective discharge capacitance $C_{\text{eff}}$ and corresponding plasma length $\Delta x$ provides a universal and robust means to characterize, quantify and predict the DBD based thrust production for flow control applications.

The investigations and results discussed in this section have been published in:

2.3 Force Density Distribution

PIV measurements in close proximity to dielectric-barrier discharge plasma actuators have been conducted to quantify the momentum transfer of the plasma to the surrounding air flow. Based on this data a comparative analysis of six existing approaches to estimate the induced body force is presented. Integral methods calculate an integral value for the actuator force based on the momentum balance equation. Insight into the spatial distribution of the body force is provided by differential methods, which are based either on the Navier-Stokes equations or on the vorticity equation. It is demonstrated that the intensity as well as the domain of the force increase with increasing operating power levels. Emphasis is also placed on the issue of self-induced drag. It is shown that 30% of the induced momentum is consumed by wall friction. All results are validated with previously obtained balance force data and luminosity analysis of identical actuators.

The main characteristic of plasma actuators is the transfer of momentum from the discharge domain to the surrounding air. However, the magnitude and the spatial distribution of this momentum transfer is not easily predicted, either from first principles or using 'calibration' experiments.

Several approaches have been published for a time-averaged [2, 153, 54, 93] and time-resolved [135, 50, 52] explicit estimation of the plasma actuator force. Besides these explicit force estimation strategies it is possible to calculate the force indirectly from velocity measurements. The latter will be referred to as implicit strategies, which can be divided further into integral and differential methods. Integral methods calculate a net value for the actuator force, based on the momentum balance equation. Deeper insight into the spatial distribution of the induced force is provided by differential methods, which are based either on the Navier-Stokes equations or on the vorticity equation.

Even though Debien et al. [35] recently uncovered valuable unsteady aspects of the actuator force production, the major objective of the present study is a comparative analysis of various existing implicit force estimation strategies [161, 43, 86, 76, 78, 14, 5, 168], which assume a quasi steady (i.e. steady-in-mean) momentum transfer. PIV measurements in close proximity to dielectric-barrier discharge plasma actuators are conducted to achieve a velocity data base under quiescent air conditions, which is then used for the force estimation purpose.
2 Performance Quantification

![Diagram of electrical plasma actuator setup](image)

Figure 2.20: Electrical plasma actuator setup comprising function generator (FG), power supply (PS), high voltage (HV) generator and plasma actuator; measured electrical quantities: $V$ actuator voltage, $V_p$ charge-probe voltage, $C_p$ charge-probe capacitance, chordwise plasma distribution.

2.3.1 Experimental Procedure

2.3.1.1 Experimental Setup

The experimental setup comprises two separate systems for the simultaneous measurements of electrical characteristics of the DBD actuator and the PIV measurements, as shown in Figures 2.20 and 2.21, respectively.

The electrical measurements were obtained using an oscilloscope (LeCroy WaveJet354, 4CHs, 500kp/Ch, 1GS/s) to record the operating voltage $V$ (Testec HVP-15HF, 1000:1) and the voltage $V_p$ across the charge-probe capacitor $C_p = 22$ nF (LeCroy PP006A, 10:1), as depicted in Figure 2.20. The operating voltage $V$ is generated by a high-voltage generator (GBS Elektronik, Minipuls 2), which is driven by a laboratory power supply (Voltcraft PS 3610) and a function generator (ELV MFG 9001M). The plasma actuator is flush-mounted on a black acrylic plate to prevent reflections. To assure best possible comparability with former experiments\[91, 95, 93\] the geometric actuator parameters were chosen as listed in Table 2.2.

To investigate the flow behavior and especially the momentum transfer to the flow, PIV measurements have been conducted in close proximity to the actuator’s discharge region (see Figure 2.21). For orientation purposes the wall-jet direction as well as the $x$-coordinate origin are included in the Figure 2.20.

A commercial high-speed PIV system comprising a Litron Nd:YLF ($\lambda = 527$ nm) dual-cavity laser and two Phantom V12 high-resolution cameras (12 bit, maximum resolution $1280 \times 800$ pixels) was utilized, which

\[6\]http://www.gbs-elektronik.de/en/services/hv-pulse-technology/special-pulse-generator-solutions
Table 2.2: Actuator setup: dimensions and measured parameters

<table>
<thead>
<tr>
<th>parameter</th>
<th>value</th>
</tr>
</thead>
<tbody>
<tr>
<td>operating voltage (peak-to-peak)</td>
<td>$V = 8kV, 9kV, 10kV, 11kV, 12kV$</td>
</tr>
<tr>
<td>operating frequency</td>
<td>$f = 11kHz$ (sine wave)</td>
</tr>
<tr>
<td>plasma actuator length</td>
<td>$L = 0.15m$</td>
</tr>
<tr>
<td>upper electrode width (copper)</td>
<td>$w_1 = 2.5mm$ (70µm thickness)</td>
</tr>
<tr>
<td>lower electrode width (copper)</td>
<td>$w_2 = 10mm$ (70µm thickness)</td>
</tr>
<tr>
<td>dielectric thickness (Kapton®)</td>
<td>$d = 0.4mm$ (5 tape layers)</td>
</tr>
</tbody>
</table>

was operated in single-frame mode at a repetition rate of 10k frames per second (fps) and a pulse duration of 150 ns. This high repetition rate with $\Delta t = 100\mu s$ required the reduction of the spatial resolution down to 800×600 pixels. A maximum number of $N = 10k$ images per run per camera was recorded, exploiting the available buffer capacity of 8 GB. The cameras were mounted facing one another perpendicular to the laser-light sheet as sketched in Figure 2.21. This arrangement allowed the simultaneous recording of two different fields of view (FOV). This arrangement provided the highest possible spatial resolution (81.3 pix/mm) in the plasma’s immediate vicinity, suitable for force calculations (FOV #1). FOV #2 enables the analysis of the spatial distribution of the resulting wall-jet downstream of the discharge domain for the identical experimental realizations with approximately half the spatial resolution (42.2 pix/mm) but twice the physical domain of FOV #1. A reversely mounted 120 mm SKR SYMMMAR lens and a 105 mm Nikon Nikkor lens were fitted to span the physical dimensions 10×7 mm² (FOV #1) and 19×14 mm² (FOV #2) respectively. The reverse setting was chosen to reduce the observed domain of FOV#1 beyond the lens’ lower magnification limit of 1:1.

The test section was enclosed in a plexiglass containment (450×325×345 mm³) with quartz-glass windows to assure best possible quality of optical accesses for laser-light sheet and cameras. Di-Ethyl-Hexyl-Sebacat (DEHS) aerosol (mean diameter 0.9 µm) is used to seed the containment. A detailed list of components and settings is given in Table 2.3.

The major objective of the PIV investigations was to obtain a force (distribution) quantification. The spatial resolution of FOV #1 fulfilled the corresponding requirements. However, the availability of a second camera (FOV #2) was found to be beneficial for other purposes. Based on the calculated DBD source terms from FOV #1 (see Section 2.3.2.2), these ad-
2 Performance Quantification

Figure 2.21: Sketch of the experimental PIV setup.

Table 2.3: Components of the implemented PIV setup: Chosen product, properties and corresponding settings.

<table>
<thead>
<tr>
<th>component</th>
<th>description</th>
<th>settings/properties</th>
</tr>
</thead>
<tbody>
<tr>
<td>test section</td>
<td>PMMA enclosure</td>
<td>dimensions (length/width/height): 450 mm/325 mm/345 mm</td>
</tr>
<tr>
<td>laser</td>
<td>Litron Lasers Model LDY303-PIV</td>
<td>laser medium: Nd:YLF 527 nm, 70 W pulse duration: 150 ns</td>
</tr>
<tr>
<td>cameras</td>
<td>2×Phantom V12</td>
<td>800×600 pixels, 10000 fps</td>
</tr>
<tr>
<td>FOV #1</td>
<td>Schneider-Kreuznach SKR SYMMAR 120/5.6-0.33X (reverse mode!)</td>
<td>focal length: f=120 mm field of view: 10×7 mm resolution: 81.3 px/mm</td>
</tr>
<tr>
<td>FOV #2</td>
<td>Nikon Nikkor 105 mm f/2.8 AF-Micro</td>
<td>focal length: f=105 mm field of view: 19×14 mm resolution: 42.2 px/mm</td>
</tr>
<tr>
<td>seeding</td>
<td>DEHS</td>
<td>mean diameter 0.9 µm</td>
</tr>
<tr>
<td>software</td>
<td>Dynamic Studio (Dantec Dynamics)</td>
<td>Versions 2.1 and 2.3</td>
</tr>
</tbody>
</table>
ditional velocity data from FOV #2, comprising a fully developed wall-jet, are available for validation of future numerical simulations, including the DBD source term as determined in the present work from FOV #1. First such numerical studies are already reported by Maden et al. [103, 104].

2.3.1.2 Data Processing

The velocity distribution was calculated from the raw data using commercial software (Dynamic Studio). The maximum accuracy of PIV algorithms is in the range of 0.1 pixel (see e.g. Nobach and Bodenschatz [116]), which results in a (theoretical) velocity accuracy of the order $10^{-2} \text{m/s}$ for the present parameter settings. To achieve the best possible accuracy, first the raw images were cut to the range of interest (ROI) and the signal-to-noise ratio (SNR) was improved by subtracting the image mean. Subsequently, the $N - 1$ flow fields $U_i(x, y)$ were calculated with a multi-grid cross-correlation algorithm (‘adaptive correlation’). Rectangular interrogation areas (IAs) with a final/initial size of $64 \times 16 / 512 \times 128$ pixels and $32 \times 16 / 256 \times 128$ pixels and 75% overlap were chosen for FOV #1 and FOV #2 respectively, to fulfil the requirements of calculating a wall jet’s velocity profile, i.e. strong wall-normal velocity gradients ($\partial u_i / \partial y \gg \partial u_i / \partial x$) and high wall-parallel velocities ($u \gg v$). Furthermore, window deformation and a Gaussian window function ($k = 2$) were applied for the velocity-distribution calculations. Outliers ($< 5\%$) were eliminated using a neighborhood validation (3×3).

The time averaged velocity distribution $u_i(x, y)$ and corresponding standard deviation $\sigma_{u_i}$ were subsequently calculated according to

$$u_i(x, y) = \frac{1}{N - 1} \sum_{n=1}^{N-1} U_i(x, y, \Delta t_n)$$  \hspace{1cm} (2.15)

and

$$\sigma_{u_i} = \sqrt{\frac{1}{N - 2} \sum_{n=1}^{N-1} \left[ U_i(x, y, \Delta t_n) - u_i(x, y) \right]^2}.$$ \hspace{1cm} (2.16)

The relatively short measurements time (1s) at 10kHz frame rate results in data acquired on the same timescale as the highly unsteady force production (see Debien et al. [35]) at the operating frequency of 11kHz. Statistical significance is facilitated by the difference in the frequencies of 1kHz. To assure statistical significance of the averaged data and corresponding quasi-steady approaches, four characteristic flowfield locations are chosen.
to estimate the convergence of the time averaged data in terms of relative standard deviations of the velocity $\sigma_u/u$. These locations are in particular (A) the zone above the discharge domain, where a suction towards the DBD occurs at low velocities, (B) the shear layer between wall jet and quiescent air directly above (C) the location of the fastest measured wall-jet velocity and (D) the fully developed wall-jet profile.

The relative standard deviation $\sigma_u/u$ for locations A-D as a function of the number of records $N$ is shown in Figure 2.22. Similar convergence diagrams of particle-based velocimetry for plasma actuator flow measurements have been published e.g. by Benard and Moreau [22] (time-resolved PIV) or Greenblatt et al. [68] (LDA). From Figure 2.22 it can be concluded that statistical significance was ensured by recording $N = 10k$ images for each case. Note the high ratio of $\sigma_u/u (>10\%)$ for convergence at location (A), where the AC character of the discharge is perceptible.

The main discharge specific quantities of the plasma actuator, i.e. power consumption $P_A$ and actuator capacitances $C_0, C_{eff}$, are determined from
2.3 Force Density Distribution

Lissajous-figure analysis\cite{95} according to

\[ P_A = f \oint Q \text{d}V = \frac{f}{K} \sum_{k=1}^{K} \oint_{k} C_p V_p \text{d}V \quad (2.17) \]

and

\[ C(t) = \frac{dQ}{dV}, \quad (2.18) \]

where Equation (2.17) represents the average of \( K \) discharge cycles and \( C_p V_p = Q \) is the charge crossing the actuator. Subsequent statistical analysis of the capacitance time traces determined according to Equation (2.18) leads to the characteristic capacitances \( C_0 \) and \( C_{\text{eff}} \) \cite{93, 95}. The results of the discharge specific measurements are shown in Figure 2.23 for the measured parameter range of the PIV experiments. To demonstrate the reproducibility of actuator construction and electrical experimentation, results of the electrical experiments of Kriegseis et al. \cite{95} for identical operating conditions are added to the diagrams. It is important to note that the discharge specific capacitance \( C_{\text{eff}} \) increases with increasing operating voltage, whereas the cold (pure passive component) capacitance \( C_0 \) remains constant for all measured operating conditions. Independent from the chosen combination of operating parameters, i.e. \( V \) and \( f \), the discharge capacitance \( C_{\text{eff}} \) is in close correlation with the plasma length \( \Delta x \) of the discharge domain, which has been demonstrated by Kriegseis et al. \cite{93}. Therefore, \( C_{\text{eff}} \) is a promising pure electrical measure to be considered for correlations of the electrical discharge intensity and the spatial extent of the momentum transfer domain.

2.3.2 Force Estimation Approaches

In order to achieve insight into existing strategies of plasma actuator force estimation all integral\cite{161, 43, 86, 76, 78, 14} and differential\cite{5, 168} methods are first briefly introduced. Control volumes (CVs) of both approaches (integral and differential) are sketched in Figure 2.24. Based on the aforementioned assumption of quasi-steady momentum transfer, for all approaches the flow is assumed to be two dimensional, steady, of constant viscosity and incompressible.
2 Performance Quantification

Figure 2.23: Electrical plasma actuator performance for the measured parameter range of the PIV experiments; (a) power consumption $P_A$, (b) Capacitances: Cold capacitance $C_0$ and effective discharge capacitance $C_{\text{eff}}$ (cp. Kriegseis et al. [95]).

Figure 2.24: Sketch of the implemented control volume and chosen boundary nomenclature as used for the force estimation: Velocity distribution is sketched with black arrows, force is shaded gray.
2.3 Force Density Distribution

2.3.2.1 Integral Methods

All implemented integral methods originate from the momentum balance equation

\[
\frac{D}{Dt} \iiint_{V(t)} \rho u_i dV = \iiint_{V(t)} \rho k_i dV + \iiint_{S(t)} t_i dS. \tag{2.19}
\]

applied in \(x\)-direction (free index \(i \equiv x\)), where the first right-hand-side term of the general volume force \(k_i\) is substituted by the unknown plasma force \(F_i := F\). In order not to confuse the reader while introducing and comparing the approaches, it is refrained from distinguishing between the body force \(F_b\) and the resulting thrust \(F\) in this section. Based on the respective assumptions and simplifications, all approaches have originally been suggested to characterize the plasma actuator performance, which will be simply denoted as \(F\) here. Further interpretations and conclusions drawn from the different results will clarify this issue retroactively in Section 2.3.3.

Assuming steady, incompressible 2D flow and assuming no contributions of the \(t_i\)-integral on any of the outer control volume surfaces, Equation (2.19) reduces to

\[
\frac{F}{L} = \rho \int_{\text{right}} u^2 \, dy - \rho \int_{\text{left}} u^2 \, dy + \rho \int_{\text{top}} uv \, dx + \int_{\text{wall}} \tau_w \, dx, \tag{2.20}
\]

with \(F\) as the only unknown. However, depending on the available measurement equipment (e.g. PIV, Laser Doppler Anemometry (LDA), Constant Temperature Anemometry (CTA), Pitot probe) further simplifications may be required such that certain right-hand-side terms of Equation (2.20) are neglected. Depending on the degree of simplification the integral methods can be subdivided into four different cases, which are briefly described in the following and are listed below in Table 2.4.

**Case 1:** Versailles et al. [161] process their PIV-data taking all terms of (2.20) into account. Based on the no-slip wall condition they calculate the wall-shear stress by means of the first data point above the wall according to

\[
\tau_w = \mu \frac{\Delta u}{\Delta y}|_{1.1A}, \tag{2.21}
\]
2 Performance Quantification

where the first row of interrogation areas (1.1A) is used for the determination. Insertion of (2.21) into (2.20) leads to

\[
\frac{F}{L} = \rho \int_{\text{right}} u^2 \, dy - \rho \int_{\text{left}} u^2 \, dy + \rho \int_{\text{top}} uv \, dx + \mu \int_{\text{wall}} \frac{\Delta u}{\Delta y} \, dx. \tag{2.22}
\]

for the force term estimation.

**Case 2:** In contrast to Case 1, Durscher and Roy [43] and Kotsonis *et al.* [86] only calculate the momentum flux crossing the control-volume boundaries of their PIV domain according to

\[
\frac{F}{L} = \rho \int_{\text{right}} u^2 \, dy - \rho \int_{\text{left}} u^2 \, dy + \rho \int_{\text{top}} uv \, dx. \tag{2.23}
\]

The force calculated from Equation (2.23) represents a net force

\[
F = F_b - F_t \tag{2.24}
\]

as emphasized by Kriegseis *et al.* [93], where actuator force and actuator thrust are explicitly distinguished.

**Case 3:** Hoskinson *et al.* [76, 78] estimate the actuator force based on Pitot-tube measurements. In addition to neglecting the wall-shear stress they further assume the flux over the left and top CV-boundaries to be negligible (quiescent air), which reduces the right side of (2.20) to the first term

\[
\frac{F}{L} = \rho \int_{\text{right}} u^2 \, dy, \tag{2.25}
\]

i.e. a momentum flux crossing the right control volume boundary.

Similar approaches are reported by Greenblatt *et al.* [68] (‘time-mean jet momentum’), Little *et al.* [100] (‘time-mean momentum addition’) or Mestiri *et al.* [107] (‘tangential force’), where the integration domain of the latter only takes the half height of the jet into account.\(^6\)

For the sake of completeness, it is important to mention at this point that Hoskinson *et al.* [76, 78] in retrospect draw the conclusion that neglecting the inflow boundary is the coarsest simplification of (2.20).

\(^6\)In particular, Mestiri *et al.* [107] perform the integration of (2.25) only between the wall \(y = 0\) and the maximum jet velocity \(y = y(u_{\text{max}})\), but they do not provide further explanations for this reduction of their domain.
Case 4: Baughn et al. [14] conclude from boundary-layer experiments that the velocity profile at the left boundary is almost unaffected by the presence of the plasma actuator force. Therefore, they only measure the velocities at the right boundary and calculate the top boundary based on the continuity equation. They furthermore demonstrate the importance of the friction term by comparing the resulting boundary-layer profile of experiments with and without plasma actuator operation. Since the present work only considers quiescent air measurements, the approach of Baughn et al. [14] is modified accordingly without changing the original idea:

\[
\frac{F}{L} = \rho \int_{\text{right}} u^2 \, dy + \rho \int_{\text{top}} uv \, dx + \mu \int_{\text{wall}} \frac{\Delta u}{\Delta y} \, dx. \tag{2.26}
\]

A summary of the chosen methods and corresponding simplifications of Equation (2.20) is given in Table 2.4, where the chosen markers as used for the discussion of the respective results are already added for clarity.

<table>
<thead>
<tr>
<th>case</th>
<th>( \frac{F}{L} = \rho \int_{\text{right}} u^2 , dy - \rho \int_{\text{left}} u^2 , dy + \rho \int_{\text{top}} uv , dx + \int_{\text{wall}} \tau_w , dx )</th>
<th>marker</th>
<th>equation</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>( \checkmark ) ( \checkmark ) ( \checkmark ) ( \checkmark )</td>
<td>-○-</td>
<td>(2.22)</td>
</tr>
<tr>
<td>2</td>
<td>( \checkmark ) ( \checkmark ) ( \checkmark ) ( \times )</td>
<td>-▲-</td>
<td>(2.23)</td>
</tr>
<tr>
<td>3</td>
<td>( \checkmark ) ( \times ) ( \times ) ( \times )</td>
<td>-▼-</td>
<td>(2.25)</td>
</tr>
<tr>
<td>4</td>
<td>( \checkmark ) ( \times ) ( \checkmark ) ( \checkmark )</td>
<td>-■-</td>
<td>(2.26)</td>
</tr>
</tbody>
</table>

The influence of the dimensions of the chosen control volume on the results of each right-hand-side term of (2.20) is demonstrated in Figure 2.25 for the velocity distribution of the strongest wall jet (\( V = 12 \) kV). The sum of all terms, which culminates to the calculated force \( F/L \), is also shown in the diagram. The \( x \)-location of the right boundary (\( x_{\text{max}} \)) is varied systematically starting at \( x_{\text{max}} = x_{\text{min}} = -1.5 \) mm up to the final position \( x_{\text{max}} = 6.8 \) mm. For the initial position \( x_{\text{max}} = x_{\text{min}} \) the terms of the top and wall boundary are zero and the left and right boundaries only differ in sign by definition. The individual contributions of the different terms of (2.20) can easily be quantified by the right ordinate in Figure 2.25, which
Figure 2.25: Contribution of each right-hand side term of (2.20) to the calculated force $F/L$ as a function of the chordwise control-volume size, i.e. chosen $x_{\text{max}}$; $x_{\text{min}} = -1.5 \text{ mm} = \text{constant}$, operating voltage $V = 12 \text{ kV}$; calculated uncertainties for the individual CV boundaries appear in the legend behind the respective components.

is normalized with the determined overall force $F/L \approx 25\text{mN/m}$. Furthermore, the relative uncertainty of the individual control volume boundaries has been added in brackets to the legend of Figure 2.25, where based on (2.16) the standard deviations of all CV boundaries have been calculated separately and subsequently taken into account for the respective terms of (2.19).

As expected, the major contribution to the calculated force is generated by the wall jet, which leaves the CV across the right boundary (–⊲–). The second dominant influence on the calculated force obviously is the effect of the estimated wall friction (–□–). The top and left boundaries have only minor influence on the results. Furthermore, the almost constant values of the sum of force contributions at both ends of the corresponding curve (–•–) confirm that the chosen final CV size contains the entire plasma actuator force.

### 2.3.2.2 Differential Methods

Insight into the spatial distribution of the induced body force requires differential force estimation methods, based either on the Navier-Stokes equations (Case 5) or on the vorticity equation (Case 6).

**Case 5:** The Navier-Stokes equations for steady and incompressible 2D
2.3 Force Density Distribution

flow of constant viscosity are given by

\[ \rho \left( u \frac{\partial u}{\partial x} + v \frac{\partial u}{\partial y} \right) = f_x - \frac{\partial p}{\partial x} + \mu \left( \frac{\partial^2 u}{\partial x^2} + \frac{\partial^2 u}{\partial y^2} \right), \quad (2.27a) \]

\[ \rho \left( u \frac{\partial v}{\partial x} + v \frac{\partial v}{\partial y} \right) = f_y - \frac{\partial p}{\partial y} + \mu \left( \frac{\partial^2 v}{\partial x^2} + \frac{\partial^2 v}{\partial y^2} \right). \quad (2.27b) \]

In Equations (2.27) the body force term \( \rho k_i \) has been replaced by the general expression \( f_i(x, y) \), which represents the plasma actuator body force distribution. Since the system (2.27) consists of only two equations for the three unknowns \( f_x, f_y \) and the pressure distribution \( p \), further assumptions have to be made. Wilke\[168\] assumes (and retroactively verifies numerically) that the force term \( f_i \) is of at least one order of magnitude larger than the pressure gradients in the entire control volume, i.e.

\[ |f_i| \gg \left| \frac{\partial p}{\partial x_i} \right|. \quad (2.28) \]

Consequently, the pressure gradients are neglected

\[ \frac{\partial p}{\partial x_i} := 0 \quad (2.29) \]

and the two remaining unknowns \( f_x \) and \( f_y \) of system (2.27) can be calculated according to

\[ f_x(x, y) = \rho \left( u \frac{\partial u}{\partial x} + v \frac{\partial u}{\partial y} \right) - \mu \left( \frac{\partial^2 u}{\partial x^2} + \frac{\partial^2 u}{\partial y^2} \right), \quad (2.30a) \]

\[ f_y(x, y) = \rho \left( u \frac{\partial v}{\partial x} + v \frac{\partial v}{\partial y} \right) - \mu \left( \frac{\partial^2 v}{\partial x^2} + \frac{\partial^2 v}{\partial y^2} \right). \quad (2.30b) \]

**Case 6:** In order to deal with the problem of unknown pressure-gradients \( \partial p/\partial x_i \) Albrecht et al. \[5\] successfully demonstrated a vorticity-equation based estimation of volume forces according to

\[ \frac{1}{\rho} \left( \frac{\partial f_x}{\partial y} - \frac{\partial f_y}{\partial x} \right) = u \frac{\partial \omega}{\partial x} + v \frac{\partial \omega}{\partial y} - \frac{\mu}{\rho} \left( \frac{\partial^2 \omega}{\partial x^2} + \frac{\partial^2 \omega}{\partial y^2} \right), \quad (2.31) \]

where the vorticity is defined as

\[ \omega = \frac{\partial v}{\partial x} - \frac{\partial u}{\partial y}. \quad (2.32) \]
Although the pressure gradients are eliminated from Equation (2.31), there still remain the two unknowns $f_x$ and $f_y$ in a single equation. Therefore, it is further assumed that the curl of the force is strongly dominated by $\partial f_x / \partial y$, i.e.

$$\frac{\partial f_x}{\partial y} \gg \frac{\partial f_y}{\partial x}. \tag{2.33}$$

Following Assumption (2.33) the chordwise gradient $\partial f_y / \partial x$ is neglected,

$$\frac{\partial f_y}{\partial x} := 0, \tag{2.34}$$

and the remaining force gradient $\partial f_x / \partial y$ can be calculated according to

$$\frac{1}{\rho} \frac{\partial f_x}{\partial y} = \frac{u}{\partial x} + \frac{v}{\partial y} - \frac{\mu}{\rho} \left( \frac{\partial^2 \omega}{\partial x^2} + \frac{\partial^2 \omega}{\partial y^2} \right). \tag{2.35}$$

Subsequent numerical integration leads to

$$f_x(x, y) = -\rho \int_{\infty}^{0} \left[ \frac{u}{\partial x} + \frac{v}{\partial y} - \frac{\mu}{\rho} \left( \frac{\partial^2 \omega}{\partial x^2} + \frac{\partial^2 \omega}{\partial y^2} \right) \right] dy. \tag{2.36}$$

The plasma actuator force is unknown at the wall ($y = 0$) and assumed to be zero at the top CV boundary. Since the cumulative trapezoidal numerical integration as used for the calculations needs an initial value, the integration limits are exchanged in (2.36) and the sign is changed following Albrecht et al. [5].

Assumption (2.33) is examined carefully and in more detail in Section 2.3.4, based on the results of Case 5, from which both force components $f_i$ are available.
2.3 Force Density Distribution

2.3.3 Comparison of the Integral Forces (Cases 1-6)

In order to compare the differential approaches with the integral approaches (Cases 1-4), the force distributions \( f_x(x, y) \) of the differential Cases 5 and 6 are integrated over the entire control volume (CV) according to

\[
\frac{F}{L} = \int \int_{A_{CV}} f_x \, dA_{CV} = \int_{y_{min}}^{y_{max}} \int_{x_{min}}^{x_{max}} f_x(x, y) \, dx \, dy.
\] (2.37)

The same uncertainty-estimation procedure as described in Section 2.3.2.1 has been applied accordingly to the force distributions of Cases 5 and 6. An extensive discussion of the spatial distribution of the force \( f_i(x, y) \) will follow in Section 2.3.4.

All calculated results of the actuator force \( F \) are plotted in Figure 2.26, where the overall uncertainties of all six cases is added in brackets behind the respective legend entries. Note that the colored lines denote the PIV-based results, whereas the gray shaded lines belong to explicitly measured thrust values by means of the weight balance experiments as published by Kriegseis et al. [93]. This combined representation allows a direct comparison of implicitly and explicitly achieved results. The discussion of the results follows the order of introduction of the cases, which is identical with the order of the legend entries in Figure 2.26.

Case 1 (\( -\bullet- \)) includes all four boundaries for the calculation of the force. Comparison with Case 2 (\( -\triangle- \), neglected wall boundary) clearly shows the importance of the wall-shear stress \( \tau_w \) for the result, since a 30% drop of the calculated force is observed. This difference occurs due to the self-induced drag as indicated in Figure 2.25 and explicitly reported by Enloe et al. [52]. Consequently, the estimated force is too weak when neglecting the friction. Case 3 (\( -\triangledown- \)) only takes the right boundary into account. Since the top and left boundaries add negative values to the results, this case results in a larger force estimation as compared to Case 2. In Case 4 (\( -\blacksquare- \)) only the negative left boundary is neglected from the balance, resulting in larger values when compared with Case 1, which considers all terms.

The curves of the two differential Cases 5 (\( -\blacktriangleleft- \)) and 6 (\( -\blacktriangledown- \)) are less smooth in comparison to the integral approaches. In particular, the determined forces of the 11 kV-experiment show unexpectedly high values for both differential approaches. The second order and third order derivatives of the velocity necessary for applying the differential methods require highly accurate experimental data and therefore cause larger uncertainties as for the integral methods. Nevertheless, at first glance, both differential
Figure 2.26: Plasma actuator force $F/L$ as a function of operating voltage $V$; implemented Cases 1-6 of the present study appear colored ($f=11$ kHz), balance based data appear gray (*explicit weight balance based measurements of Kriegseis et al. [93]; measurement uncertainty $\sigma_F < 3\%$); calculated uncertainties for different PIV-based approaches appear in the legend behind the respective Cases.
methods show good agreement with one another, which supports the validity of (confidence regarding) the two different assumptions (2.28) and (2.33). A deeper insight into these assumptions and their consequences is provided in Section 2.3.4. As expected, both differential Cases (5 and 6) fluctuate around the values of Case 1, since the latter calculates the force $F$ as an integral source term according to Figure 2.24(a) without neglecting any terms of Equation (2.20).

Furthermore, Figure 2.26 includes the comparison with the weight balance based data of Kriegseis et al. [93], which explicitly measures the resulting thrust of the actuator forced wall jet according to Equation (2.24). Therefore, it comes as no surprise that Case 2 matches these data best, since this case mimics the thrust measurements by neglecting the wall shear stress term (cp. Table 2.4).

### 2.3.4 Force Distribution Analysis

This section discusses the results obtained with force determination procedures that yield a spatial information on the force density distribution.

#### 2.3.4.1 Case 5 (Navier-Stokes Equation)

Beginning with the Navier-Stokes equation based Case 5, the contours of the force distributions $f_x(x, y)$ and $f_y(x, y)$ according to Equation (2.30) are shown in Figure 2.27 for the operating voltage of $V = 12$ kV, exemplarily. Furthermore, a 10% isoline ($\max[f_x]/10$) is plotted to denote the momentum-transfer domain. This threshold turned out to be a convenient indicator for the boundary of the force domain, since on the one hand the force magnitude is reduced by one order. On the other hand the signal-to-noise ratio for this threshold still allows a clear separation of the force from background.

The direct comparison of different orders of magnitude in the color codings of Figures 2.27(a) and 2.27(b) clearly shows that the momentum transfer from the discharge to the surrounding air is predominated through the $x$-component of the force $f_x(x, y)$. In accordance with detailed numerical simulations (see e.g. Boeuf et al. [25, 23, 24] or Unfer et al. [156, 155]), the maximum values of the force density can be observed slightly downstream of the upper electrode’s trailing edge ($x = 0$) in the range of $x = 1$mm to 2mm. Farther downstream, at larger $x$ values, the force decreases. Note the layer of negative force values farther downstream directly above the wall boundary, which at first glance appears unexpected. To clarify this
Figure 2.27: Force distributions $f_x(x, y)$ (a) and $f_y(x, y)$ (b) determined according to Case 5 (2.30); the 10% isoline ($\max[f_x]/10$) indicates the momentum transfer domain; note the different orders of magnitude; $V = 12$ kV, $f = 11$ kHz.
2.3 Force Density Distribution

Figure 2.28: Contribution of the right-hand-side terms of (2.30a) to the resulting $x$-component of the source term $f_x(x, y)$; (a,b) convective terms, (c,d) diffusive terms; color coding and 10% isoline identical with Figure 2.27; $V = 12$ kV, $f = 11$ kHz.

issue, the contributions of the convective and diffusive terms of Equation (2.30a) are presented separately in Figure 2.28(a,b) and 2.28(c,d), where the above introduced 10% isoline is again included for orientation purposes.

Obviously, the major contribution to the calculated force $f_x(x, y)$ is produced by the convective terms $\rho u \frac{\partial u}{\partial x}$, as shown in Figures 2.28(a) and 2.28(b). Due to the strong convective acceleration $\frac{\partial u}{\partial x}$ the convective term in $x$-direction constitutes the high force amplitudes in the left part of the force domain. The wall normal term additionally augments the force domain further downstream. The diffusive terms $\mu \left( \frac{\partial^2 u}{\partial x^2} \right)$ contribute only to a minor extent to the resulting force distribution. The wall normal diffusive term, $\mu \left( \frac{\partial^2 u}{\partial y^2} \right)$, denotes the ridge of the wall jet, where the negative changing rate of the slope of the wall-jet velocity profile is maximal at maximal velocities. The term of chordwise diffusion $\mu \left( \frac{\partial^2 u}{\partial x^2} \right)$ is negligible (order of $\pm 10$ N/m$^3$). Obviously, at some extent downstream the attenuated actuator force $f_x(x, y)$ is dominated by shear forces of the wall jet’s boundary layer, thus leading to a deceleration of the flow within that shear layer. Consequently, as the sign of $\frac{\partial u}{\partial x}$ changes from plus
2 Performance Quantification

Figure 2.29: Ratio of the force gradients $\frac{\partial f_y}{\partial x}$ and $\frac{\partial f_x}{\partial y}$; the 10% isoline ($\max[f_x]/10$) indicates the momentum transfer domain; note the logarithmic color coding; $V = 12$ kV, $f = 11$ kHz.

to minus, the shear introduces negative values for the ‘source term’, which has been addressed in some recent publications [55, 52].

Font et al. [55], for instance, conclude that ‘a large part of the force imparted by the actuator on the air (70-90%) is almost immediately lost to the drag of the air with the adjacent wall surface’. Enloe et al. [52] specify this performance drop, as they introduce the term ‘self-induced drag’ based on the proportionality of the surface drag to the wall-normal velocity gradient $\partial u/\partial y$ (cp. (2.21)). Following these reports, the counteraction of the drag scales with the imparted momentum to the air, since an increased wall-jet velocity introduces an increased velocity gradient $\partial u/\partial y$. However, this issue clearly demonstrates the limitations of inverse approaches to calculate the applied source term from the resulting velocity field, where a careful distinction between cause and effect is only possible based on thorough interpretation of the results.

2.3.4.2 Case 6 (Vorticity Equation)

To provide an assessment basis for the validity of Assumption (2.33) of Case 6, both force gradients $\frac{\partial f_x}{\partial y}$ and $\frac{\partial f_y}{\partial x}$ are determined from the force distributions $f_i(x, y)$ of Case 5 (see Figure 2.27). The ratio of the absolute values of both gradients according to

$$\left|\frac{\partial f_y}{\partial x}\right| / \left|\frac{\partial f_x}{\partial y}\right|$$

are shown in Figure 2.29 to quantify the quality of Assumption (2.33).

The logarithmic color coding of Figure 2.29 visualizes the order of magnitude of the ratio (2.38). Above the upper electrode in the range of negative
2.3 Force Density Distribution

Figure 2.30: Force distribution $f_x(x, y)$ determined according to Case 6 (2.36); the 10% isoline ($\max[f_x]/10$) indicates the momentum transfer domain; $V = 12$ kV, $f = 11$ kHz.

$x$ values, numerator and denominator of (2.38) are of the same order of magnitude, entailing that assumption (2.33) is invalid in this region. A comparison with Figure 2.27 leads to the conclusion that this region is mainly constituted by the ratio of small values. Moreover, the 10% isoline used in the previous Figures is included in the diagram and demarcates the domain in which the validity of the assumption (2.33) is essential. Apparently, the main part of the invalid region is excluded from the force domain, which confirms the validity of assumption (2.33) with sufficient (spatial) accuracy for the application of the vorticity equation based Case 6.

Figure 2.30 shows the contour of the force distribution $f_x(x, y)$ according to (2.36) for the operating voltage of $V = 12$ kV, similar to Figure 2.27(a). Again, a 10% isoline of the force ($\max[f_x]/10$) indicates the relevant force domain. From the direct comparison of Figures 2.30 and 2.27(a) it can be seen that the maximum values, the distribution and the overall size of the produced force are comparable, although differing slightly in shape and force intensity.

The resulting contributions of the right-hand-side terms of Equation (2.36) to the resulting source term $f_x(x, y)$ are shown in Figure 2.31. The convective (a,b) and diffusive (c,d) terms of the vorticity equation are integrated separately for the diagrams to allow best possible comparison with Figure 2.28. All four diagrams of Figure 2.31 reveal identical characteristics similar to Figure 2.28. The contributions of the convective terms predominate those of the diffusive terms. Especially the $x$-component of the convection, $\rho u (\partial \omega / \partial x)$, shows identical patterns of acceleration and deceleration above the dielectric surface.

In conclusion, the main characteristics of the expected source term dis-
Figure 2.31: Contribution of the right-hand-side terms of (2.36) to the resulting source term $f_x(x, y)$; (a,b) convective terms, (c,d) diffusive terms; color coding and 10% isoline identical with Figure 2.30; $V = 12$ kV, $f = 11$ kHz.
2.3 Force Density Distribution

Distribution $f_x(x, y)$ are determined with both differential approaches, thus being available for numerical simulations of DBD-based flow control applications [104]. Furthermore, Case 5 on the one hand additionally provides information on the wall-normal momentum transfer $f_y(x, y)$, which is lost by definition in Case 6. On the other hand the influences of the unknown pressure gradients $\partial p/\partial x_i$ remain to be clarified in Case 5. Wilke [168] retroactively verified the validity of assumption (2.29) with a numerical simulation of his results in a quiescent air environment. This verification is confirmed by the comparative analysis of Cases 5 and 6. The similarity of the force distributions determined according to Equations (2.30a) and (2.36) proves that the possible presence of forces due to pressure gradients are at least one order of magnitude smaller than the resulting volume force intensity. Therefore, it is an appropriate simplification maintaining sufficient accuracy, if the contribution of possible pressure gradients is neglected for a Navier-Stokes equation based force distribution estimation.

2.3.4.3 Correlation of Force Domain, Plasma Length and Effective Discharge Capacitance

In Sections 2.3.4.1 and 2.3.4.2 the 10% isolines of the determined force distributions have already been introduced to demarcate the momentum transfer domain within the recorded velocity distributions. The spatial (chordwise) extent of this domain is furthermore a valuable measure in terms of a characteristic momentum transfer length scale, which can easily be extracted from the body-force distributions.

The 10% isolines for all measured operating voltages $V$ are summarized in Figure 2.32. The chordwise extent of the momentum transfer domain can be compared with the chordwise plasma length $\Delta x$ achievable from light-emission analysis as described by Kriegseis et al. [93]. This-force related length scale is therefore denoted $\Delta x_F$, as sketched in Figure 2.32(a) for the 12 kV experiment, where the index $F$ is added to assure the distinction of force-determination based and light-emission based (no index) estimation of the same quantity.

Obviously, the size of the body force domain grows with increasing operating voltage $V$, whereas the location of the experiments’ maximum force intensity (marked with crosses of the respective color) varies only slightly. The values of $\Delta x_F$ are shown in Figure 2.33 for all measured operating conditions. The resulting plasma length $\Delta x$ of the discharge luminosity obtained from gray-value analysis by Kriegseis et al. [93] is also included in the diagram. The aforementioned sensitivity of the PIV-based calculations
Figure 2.32: 10% isolines of the force distribution $f_x(x, y)$ for all operating voltages $V (f=11 \text{ kHz})$; (a) Case 5, (b) Case 6.

is clearly recognizable. In particular, it can be concluded from the spatial extent of the isolines that the vorticity equation based approach (Case 6) is less favorable than the application of the Navier-Stokes equation (Case 5). The latter uses up to second derivatives of PIV data, whereas the former uses up to third derivatives and subsequent numerical integration.

Nevertheless, the similar characteristics of the plasma length $\Delta x$ and the length of the momentum transfer domain $\Delta x_F$ can be clearly identified from Figure 2.33, where both $\Delta x$ and $\Delta x_F$ grow linearly with increasing operating voltage (Figure 2.33(a)). Additionally, the parameter-independent close correlation of both length scales ($\Delta x$ and $\Delta x_F$) with the effective discharge capacitance $C_{\text{eff}}$ is demonstrated in Figure 2.33(b). Obviously, the former frequency dependency vanishes such that all curves collapse (cp. [93]) and the $\Delta x_F - C_{\text{eff}}$ curves fall on top of the $\Delta x - C_{\text{eff}}$ data. It can therefore be concluded inversely that the plasma length $\Delta x$ indeed serves as a measure for the streamwise extent of the momentum transfer domain. This is an important insight, since only one experiment is required to gather spatial information about the momentum transfer domain - either from PIV experiments or even simpler via light intensity analysis - from which the required length scale can be extracted. Based on the here shown
2.3 Force Density Distribution

Figure 2.33: Plasma length $\Delta x$ as function of operating voltage $V$ (a) and discharge capacitance $C_{\text{eff}}$ (b); dark markers: estimated momentum transfer length $\Delta x_F$ based on the 10% isolines of Figure 2.32; white markers: Measured plasma length $\Delta x$ based on light emission analysis (*: cp. Kriegseis et al. [93]).
2 Performance Quantification

plasma length-discharge capacitance correlation the extracted length scale can subsequently be utilized to transform a force-capacitance interrelation \( (F - C_{\text{eff}}) \) into a force-penetration length interrelation \( (F - \Delta x_F) \).

### 2.3.5 Resumee Section 2.3

In order to determine the force produced by the plasma actuator different integral and differential force estimation approaches are implemented and successfully applied to PIV-based data of plasma actuator forced wall jets. The post-processing strategies of several research groups are implemented to allow a comparison of the different approaches with identical raw data. The comparative analysis of various force estimation strategies, the discussion of the corresponding simplifications and their consequences for the resulting values of the actuator’s body force (distribution) allow the evaluation of recent progress on the topic of velocity-information based force term estimation of DBD plasma actuators.

It is demonstrated that the magnitude as well as the domain of the force increase with increasing operating voltages \( V \). In agreement with previous reports on self-induced drag by Enloe *et al.* [52] it is shown that 30% of the overall induced momentum is consumed by wall friction. From this insight the conclusion must be drawn that it is of particular importance to carefully distinguish the terms *actuator force* and *actuator thrust* when discussing the momentum transfer to the flow.

In addition to the discussion of the magnitude of the force, information on the spatial distribution of the plasma actuator force is provided by two differential methods. Even though the two methods contain up to third-order derivatives of PIV data, good agreement with the results of the integral approaches is achieved. Furthermore, the force distribution determined based on both Navier-Stokes equation and vorticity equation based approaches agree well with one another, where in both cases the contribution of the convective terms predominate those of the diffusive ones. The similarity of the results of both approaches additionally proves that the pressure gradient \( \partial p / \partial x_i \) can only have a negligible influence on the resulting force. Detailed knowledge about the spatial distribution of the volume force is essential when modeling the plasma actuator for numerical simulations of various flow-control scenarios. Therefore, the present work constitutes a step towards prediction of both amplitude and distribution of DBD based volume forces for CFD applications. First such numerical efforts are shown by Maden *et al.* [103, 104].

The PIV-based data is validated with previously conducted balance based
2.3 Force Density Distribution

force measurements and discharge-luminosity analysis of identical actuators and operating conditions as shown by Kriegseis et al. [93]. In particular, the (chordwise) growth of the force domain with increasing operating voltages is identified to result from a cause-effect relation between plasma presence and resulting volume force, as the correlation of the plasma length $\Delta x$ and the length of the force domain $\Delta x_F$ is successfully demonstrated with the present work.

The investigations and results discussed in this section have been published in:

2 Performance Quantification
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In the existing literature it is commonly assumed that the discharge affects the flow, but not vice versa. The reports of flow control experiments deal with free stream velocities ranging between several meters per second and supersonic conditions. In contrast, mainly quiescent air experiments have been conducted to characterize actuator performance under the influence of varying ambient conditions (e.g. temperature, pressure, humidity) or variations of actuator-specific parameters (e.g. geometry, dielectric material).

Chapter 3 addresses the above mentioned influences of environmental conditions on the performance of plasma actuators. In contrast to previous studies, the investigations discussed here focus on the newly identified influence of the flow velocity on the actuator’s performance; also in combination with changing conditions such as pressure or humidity. Such an investigation requires robust methods for the quantification of the effects. The new methods discussed in Chapter 2 are ideal for this purpose since they exploit reliable dependencies between the actual force production and more easily measurable quantities such as light emission, power consumption and capacitances. Once such performance impairing effects are identified, they need to be considered in the actual flow control application. A step towards the compensation of environmental influences is the on-line analysis of the actuator performance. Such a monitoring system is then implemented into a more complex system to automatically adjust the actuator’s performance to overcome issues with changing body forces.

Section 3.1 demonstrates the use of the quantification methods of the previous chapter for the development of an on-line monitoring system, capable of monitoring the actual power consumption as a measure for the actual force production. The system’s functionality is demonstrated with an actuator placed in a high-speed flow with a Mach number of up to 0.8 and a pronounced time dependance. Both pressure and velocity have a strong effect on the actuator’s power consumption. This demonstration is followed by a first attempt to compensate for the reduced power consumption due to the changing environmental conditions by an automated
3 Performance Control

adjustment of the operating voltage of the plasma actuator.

As a consequence of the recently discovered air-flow effect, a systematic investigation of its magnitude and velocity dependency is presented in Section 3.2. First discovered at velocities higher than 100m/s, the air-flow effect is measurable also at lower velocities of several tens of meters per second. An explanation of the physical background of this effect is offered by introducing a scaling number that relates the flow velocity to the drift velocity of the ions that are responsible for the force production of the plasma actuator.

The effect of varying pressures on DBD gas discharges and on DBD plasma actuators was previously investigated. Section 3.3 describes an investigation in which external influences by varying pressures in combination with varying free-stream velocities are quantified and the interaction of both influences is described. If applied on an airfoil it is this situation that the plasma actuator is exposed to: depending on the angle of attack, flight velocity, altitude, etc. the actuator experiences different combinations of velocity and pressure.

Finally, Section 3.4 discusses a set of experiments that take the proof-of-concept studies from Section 3.1 to a higher level by applying more advanced analysis tools for monitoring the power consumption and a carefully adjusted PID controller for the performance control. A fast and reliable performance control system is presented. Such systems have proven extremely useful for all kinds of plasma actuator flow control experiments. The previously mentioned difficulties with changing performance from day to day and even during one experiment are therefore no longer an issue.
3.1 On-line Performance Characterization and Monitoring

Flow control applications depend on the reliability of the applied flow control actuator. As mentioned before, plasma actuators do not necessarily provide constant forcing with constant operating voltage, when the environmental conditions change. Another issue emerging from varying performance of a plasma actuator is the efficiency of its operation. Any plasma actuator flow-control system, which has previously been impedance matched for a particular reference Mach number and thermodynamic state, can be considerably de-tuned by simply varying the free stream velocity and/or the altitude (temperature, pressure or density). Therefore, a detailed online knowledge of the actuator’s performance is essential to permanently maintain the optimum electrical efficiency and flow-control effectiveness during operation at changing airflow conditions.

3.1.1 Experimental Procedure

The experimental setup comprises two measurement systems, as sketched in Figure 3.1. A plasma actuator as used in [95] of \( L = 0.11 \) m length is mounted at the wall of the wind-tunnel’s test section. At the opposite window a CMOS camera (Phantom V12.1, 512×512 pixels, 24fps; Nikon 105 mm, AF Micro NIKKOR f/2.8D) is used to record the spatio-temporal light emission of the discharge during the power-consumption analysis (cp. [94]). The electrical control circuit is built up using a digital oscilloscope (Picotech PicoScope4424, 4CHs, 2500p/Ch, 10MS/s) to record the operating voltage \( V \) (Testec HVP-15HF, 1000:1) and the voltage \( V_p \) across the charge-probe capacitor \( C_p = 22 \) nF (LeCroy PP006A, 10:1). The operating voltage \( V \) is generated by a high voltage generator (GBS Elektronik, Minipuls2), which is driven by a notebook-controlled laboratory power supply (Volcraft VSP 2410) and a function generator (GW Instek, SFG-2004, fixed frequency: \( f = 12.0 \) kHz).

The experiments are conducted in a blow-down type wind tunnel in order to obtain a transient airflow during experimentation, as shown in Figure 3.21. The static pressure \( p \) ranges between \( p_{\text{min}} = 0.89 \) bar and \( p_{\text{max}} = 1.45 \) bar during the blow-down. Benard et al. [19] and Versailles et al. [161] previously reported a favorable and adverse impact on plasma actuator performance at quiescent air conditions under reduced and elevated pressure levels, respectively.
3 Performance Control

Figure 3.1: Sketch of experimental setup. (a) Wind tunnel test section and overhead camera (CAM); (b) detailed view of electrical plasma-actuator setup comprising function generator (FG), power supply (PS), high voltage (HV) transformer, notebook (NB) and plasma actuator.

![Figure 3.1](image-url)

Figure 3.2: Transient flow conditions. (a) Static and total pressure $p, p_t$; (b) Mach number $M$; characteristic times are labeled as $t_1, t_2, t_3$ (cp. Table 3.1).

![Figure 3.2](image-url)
3.1 On-line Performance Characterization and Monitoring

3.1.1 Online Characterization and Monitoring

The online-characterization of the voltage-charge cyclograms, i.e. for monitoring or controlling purposes, is based on the diagnostic approach as introduced in Section 2.1. For every time step $t^i$ the algorithm calculates the power consumption

$$ P_A^i = f E^i = f \int_{t_{i-1}}^{t_i} Q \, dV \quad \text{with} \quad Q = C_p V_p. \quad (3.1) $$

When using the diagnostic tool in closed-loop control mode, the algorithm furthermore compares $P_A^i$ with a pre-set power level $P_A^*$ and calculates the control signal $\Omega^{i+1} = \Omega^{i+1}(\Omega^i, P_A^i, P_A^*)$ for the next time step by means of a PD control algorithm, which is then sent to the power supply (see Figure 3.4(a)). The light-emission data is used to validate the online-diagnostic tool by means of the temporal plasma length evolution $\Delta x$ (cp. [94]).

3.1.2 Results

The pre-set initial conditions of $f = 12\text{kHz}$, $V = 10\text{kV}$ and $p_0 = 1.028$ bar result in an initial power consumption $P_A = P_A^* = 7.2\text{W}$ for $t < 0$. Three characteristic times are highlighted for $p(t_1) = p_{\text{max}}$, $p(t_2) = p_0$, $p(t_3) = p_{\text{min}}$ with the purpose of distinguishing pressure and air-speed effects on the discharge performance (see Figures 3.21 and 3.3).

The results from the monitoring experiment (Figure 3.3(a)) clearly reveal the impact of the transient flow conditions on the resulting actuator power $P_A$. Immediately after the wind tunnel valve is opened at $t = 0$ a power peak occurs due to an initial expansion wave passing the test section, which is...
3 Performance Control

followed by a significant performance drop ($P_A = 4.8\text{W}$) once the blowdown scenario is fully developed at $t_1$ under adverse pressure conditions at maximum airflow speed (see Table 3.1). With decreasing Mach number and pressure at $t_2$ a constantly reduced performance ($P_A = 4.9\text{W}$) is observed, solely due to the impact of high speed airflow ($M = 0.75$) at ambient pressure conditions $p_0$, which agrees with the reports of Barckmann et al. [12]. The influence of the minimum pressure at $t_3$ exceeds the adverse airflow impact ($M = 0.69$), which results in an increased performance $P_A = 8.7\text{W}$ as compared to the initial value $P^*_A = 7.2\text{W}$. Thereafter, all quantities asymptotically return to their initial values again. The plasma length $\Delta x$ of the simultaneously recorded light-emissions further confirms the correctness of the online characterization of the power consumption.
Table 3.1: Measured data at characteristic times; $^m$ monitoring, $^c$ controlling (cp. Figure 3.3).

<table>
<thead>
<tr>
<th>$t$</th>
<th>$p$ [bar]</th>
<th>$M$</th>
<th>$P_A$ [W]</th>
<th>$V$ [kV]</th>
<th>$\Delta x$ [mm]</th>
<th>$\Omega$ [V]</th>
</tr>
</thead>
<tbody>
<tr>
<td>&lt; 0</td>
<td>1.03</td>
<td>0</td>
<td>7.2$^{m,c}$</td>
<td>10.0$^{m,c}$</td>
<td>2.9$^{m,c}$</td>
<td>0.89$^c$</td>
</tr>
<tr>
<td>$t_1 = 2.4$[s]</td>
<td>1.45</td>
<td>0.84</td>
<td>4.8$^m$ 5.7$^c$</td>
<td>10.8$^m$ 10.1$^c$</td>
<td>1.9$^m$ 1.6$^c$</td>
<td>0.97$^c$</td>
</tr>
<tr>
<td>$t_2 = 7.8$[s]</td>
<td>1.03</td>
<td>0.75</td>
<td>4.9$^m$ 7.5$^c$</td>
<td>9.4$^m$ 10.2$^c$</td>
<td>2.0$^m$ 2.8$^c$</td>
<td>0.95$^c$</td>
</tr>
<tr>
<td>$t_3 = 14.5$[s]</td>
<td>0.89</td>
<td>0.69</td>
<td>8.7$^m$ 7.2$^c$</td>
<td>10.3$^m$ 9.8$^c$</td>
<td>3.3$^m$ 2.8$^c$</td>
<td>0.78$^c$</td>
</tr>
</tbody>
</table>
3 Performance Control

For identical initial and airflow conditions the results of the closed-loop control experiment are shown in Figure 3.3(b). The control algorithm fails for the very strong initial power oscillation of the passing expansion wave. Thereafter the performance drop is identified at $t_1$ and the algorithm counter-acts this drop, as shown be the slope of the control signal $\Omega$. At $t = 5.5s$ the control algorithm collapses for a single time step, which causes a power overshoot. Apart from this peak the algorithm successfully conducts a closed-loop control of the the above-discussed power variations, which again is confirmed by the results of the plasma-length $\Delta x$. Qualitatively, the slope of the control signal $\Omega$ of the controlled case directly mirrors the slope of the power $P_A$ of the uncontrolled case, which underlines the successful counter-action of the controller even in this simple proof of concept approach.

3.1.3 Resume Section 3.1

The impact of changing kinematic and thermodynamic airflow conditions on the performance of dielectric barrier discharge (DBD) plasma actuators is demonstrated. The necessity of counter-acting these performance fluctuations is met by a novel online-characterization and in-situ-control approach. Based on the measured real-time performance data, the possibility of achieving a constant plasma-actuator performance during operation under fluctuating and transient flow conditions is demonstrated in a simple proof of concept approach.

The investigations and results discussed in this section have been published in:

3.2 Environmental Influences: Airflow Effect

The airflow effects on dielectric barrier discharges are a well-known phenomenon. Many industrial applications of dielectric barrier discharge (ozone generation, surface treatment or pollution control) are configured such that gas flows through the discharge domain, as comprehensively reported by Kogelschatz [85]. The observed processes are commonly characterized by the airflow rate through the precipitation or depollution reactors in combination with electrical and/or chemical quantities (see e.g. Mizuno [109, 108]). Dramane et al. [38], for instance, report a significant influence of the flow rate through axial and planar reactors on the transferred charge $Q$ and the corresponding power consumption $P_A$. Furthermore, Jolibois et al. [83] present chemical measurements of discharge assisted species generation based on a so-called energy density, defined as the ratio of consumed power and the flow rate through the reactor.

Despite this broad experience with airflow-discharge interaction, reports about discharge based aerodynamic flow control commonly still neglect this interaction. However, the discharge intensity; hence the DBD plasma-actuator performance, is affected by numerous parameters. On the one hand, the impact of ambient conditions on the flow induced by plasma actuators has been documented extensively in many recent publications. It has been demonstrated that humidity [7, 21, 119], temperature [119, 161], ambient pressure [161, 69, 169, 18, 151, 2] and gas-species [151, 2, 56, 44], have either adverse or favorable effects on the plasma actuator’s discharge intensity. On the other hand, it is commonly assumed in the flow-control community that the actuator discharge manipulates the flow but not vice-versa.

Only few studies [125, 12, 87] have investigated this reverse aerodynamic influence of the air-flow velocity on the actuator performance. These reports concentrate on higher Mach numbers ($M > 0.4$). Therefore, it is the main objective of the present study to identify and quantify the impact of the airflow on the plasma actuator performance at lower and moderate velocities, as are relevant for take-off and approach scenarios of many aerodynamic lifting surfaces [129], but also essential for control of unmanned aerial vehicles or internal flows. In addition, comparisons to data obtained at higher Mach numbers allow universally valid conclusions for performance changes of dielectric barrier discharge plasma actuators under transient and/or fluctuating airflow conditions to be drawn.
3 Performance Control

3.2.1 Experimental Procedure

3.2.1.1 Experimental Setup

To investigate the airflow influence on the plasma-actuator performance, plasma actuators were operated in various wind tunnel experiments. The actuator performance was determined by means of simultaneous electrical and optical measurements as proposed by Kriegseis et al. [93, 95], as sketched in Figure 3.4.

Simultaneous to the light emission measurements, the relevant electrical quantities were recorded, as depicted in Figure 3.4(a). For this purpose a high-resolution oscilloscope (LeCroy WaveJet 354, 4CHs, 500kp/Ch, 1GS/s) has been used. Measured quantities are the actuator’s operating voltage $V$, recorded using a high voltage probe (TESTEC HVP-15HF, 1000:1), and the probe voltage $V_p$ across the charge-probe capacitor with $C_p = 22$ nF.

The single dielectric barrier discharge (DBD) plasma actuator under investigation is a standard configuration and consists of two copper electrodes with a streamwise extent of $w_1 = 2.5$mm (upper electrode) and $w_2 = 10$mm (lower electrode), which are separated by Kapton® tape as polyimide dielectric. The geometric dimensions of the applied actuators are listed in Table 3.2. The operating voltage for the actuator is generated by a custom-designed, high-voltage transformer (GBS Elektronik, Minipuls 2), which is driven by a laboratory power supply (Voltcraft PS 3610) and a function generator (ELV MFG 9001M).

Different cameras were used according to their availability, while for all experiments a Nikon lens (Nikon 105mm, AF Micro NIKKOR f/2.8D) was used and the cameras were mounted outside the wind tunnels. To investigate the airflow influence in the desired range of free-stream velocities $U_\infty$ and Mach numbers $M$ between quiescent air up to cruise flight conditions ($M = 0.75$), two different wind tunnels were utilized for the experimentation:

The low-speed experiments (Exp1 - Exp4) were conducted in the open-return wind tunnel facility (NWK2) at the Technische Universität Darmstadt. This wind tunnel features a test section of 450mm × 450mm and is capable of producing flow speeds up to 68m/s. The wind tunnel was operated under ambient pressure conditions ($p = p_0 = 1.03$bar). Figure 3.4(b) gives a schematic overview of the mechanical setup. The plasma actuator is placed on a flat plate with elliptical leading edge, which is mounted in the center of the tunnel. Using this technique the influence of the tunnel
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walls is eliminated and a boundary-layer of defined thickness can be set according to the downstream position of the plasma actuator. For this study the actuator was placed 400mm downstream of the leading edge to simulate conditions as encountered during active wave cancelation (AWC) experiments[73]. Furthermore, a thin tripping wire was placed at the leading edge to achieve a turbulent boundary layer on the plate. Based on hot-wire measurements the boundary-layer thickness was determined to be $\delta = 12.9, 10.4$ and $9.2\text{mm}$ for free-stream velocities of $U_\infty = 10, 30$ and $50\text{m/s}$, respectively. To minimize the mechanical influence of the actuator on the airflow, the exposed electrode has been flush mounted with the dielectric in a cavity of the plate as indicated in Figure 3.4(a).

The luminosity measurements of Exp1 - Exp4 were carried out using a CCD camera (PCO SensiCam, $1376 \times 1040$ pixels, 1 fps; Nikon 105 mm, AF Micro NIKKOR f/2.8D) recording the actuator’s light emission through an optical access in the ceiling of the tunnel.

The high-speed experiments (Exp5 - Exp6) were conducted in the blow-down wind tunnel (TVM) of the Technische Universität Darmstadt (Figure 3.4(c)). This wind tunnel is designed to operate at Mach numbers in the range of $0.4 < M < 4$ at precisely adjustable pressure levels. The dimensions of the test section are $150 \times 150\text{mm}^2$. Two different CMOS cameras were used for the experiments in the TVM. For experiment 5 a Vosskühler camera (Vosskühler1000, $1024 \times 1024$ pixels, 8 bit, 5 fps), for experiment 6 a Phantom camera (Phantom V12.1, $512\times512$ pixels, 12 bit, 24 fps) was used to record the spatio-temporal light emission of the discharge during the power consumption analysis of the operating actuator.

3.2.1.2 Parameter Space

In order to quantify the effects of flow speed on the actuator performance a parametric study has been conducted. Table 3.2 gives an overview of the covered parameter space.

For the first experiments (Exp1, Exp2) the flow speed was increased by increments of $10\text{m/s}$ (respectively $5\text{m/s}$), while operating voltage and frequency were held constant. Two different power levels were investigated. A clear detrimental impact of the flow speed on the actuator’s performance is visible, as will be discussed in detail in the next chapter.
Figure 3.4: Sketch of experimental setup; (a) detail of applied components and measured electrical quantities: power supply (PS), function generator (FG), high voltage transformer (HV), $V$ actuator voltage, $V_p$ charge-probe voltage, $C_p$ charge-probe capacitor, chordwise plasma distribution; (b) test section of the wind tunnel NWK2 (low speed), flat plate, plasma actuator and camera (CAM); (c) test section of the wind tunnel NWK2 (high speed), plasma actuator and camera (CAM).
Table 3.2: Parameter space: Covered range of operating conditions, state variables and geometric dimensions
(n.a. \(\equiv\) quantity not available/measured during experimentation).

<table>
<thead>
<tr>
<th>Exp.-Nr.</th>
<th>operating voltage (V) [kV]</th>
<th>frequency (f) [kHz]</th>
<th>free-stream velocity (U_\infty) [m/s]</th>
<th>Mach number (M)</th>
<th>relative humidity (\phi) [%]</th>
<th>actuator length (L) [mm]</th>
<th>electrode width (w_1) [mm]</th>
<th>electrode width (w_2) [mm]</th>
<th>dielectric thickness (d) [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Exp1</td>
<td>8</td>
<td>14</td>
<td>0 - 55</td>
<td>0 - 0.16</td>
<td>30</td>
<td>0.4</td>
<td>2.5</td>
<td>10</td>
<td>0.4</td>
</tr>
<tr>
<td>Exp2</td>
<td>12</td>
<td>10</td>
<td>0 - 55</td>
<td>0 - 0.16</td>
<td>30</td>
<td>0.4</td>
<td>2.5</td>
<td>10</td>
<td>0.4</td>
</tr>
<tr>
<td>Exp3</td>
<td>9.8 - 12.7</td>
<td>8.2 - 13.4</td>
<td>0</td>
<td>0</td>
<td>30</td>
<td>0.4</td>
<td>2.5</td>
<td>10</td>
<td>0.4</td>
</tr>
<tr>
<td>Exp4</td>
<td>9.8 - 12.7</td>
<td>8.2 - 13.4</td>
<td>50</td>
<td>0.145</td>
<td>30</td>
<td>0.4</td>
<td>2.5</td>
<td>10</td>
<td>0.4</td>
</tr>
<tr>
<td>Exp5 [12]</td>
<td>13</td>
<td>8</td>
<td>0, 144</td>
<td>0, 0.42</td>
<td>n.a.</td>
<td>0.11</td>
<td>2.5</td>
<td>10</td>
<td>0.8</td>
</tr>
<tr>
<td>Exp6 [87]</td>
<td>10</td>
<td>12</td>
<td>0, 257</td>
<td>0, 0.75</td>
<td>n.a.</td>
<td>0.11</td>
<td>2.5</td>
<td>10</td>
<td>0.4</td>
</tr>
</tbody>
</table>
Furthermore experiments were carried out in quiescent air (Exp3) and at a constant flow speed of 50m/s (Exp4), during which the electrical parameters of the actuator (operating voltage, operating frequency) have been varied. Finally the results are compared to high-speed experiments at $M = 0.42$ (Exp5) and $M = 0.75$ (Exp6), carried out in the tri-sonic wind tunnel TVM at ambient pressure level ($p = p_0 = 1.03$ bar) to eliminate the issue of pressure effects on the discharge behavior[124].

Note that a preliminary version of the results of Exp5 has already been published alongside the separation control experiments of Barckmann et al. [12] and Exp6 is an ambient-pressure extract of the online characterization experiments of Kriegseis et al. [87]. The thickness of the corresponding turbulent boundary in the TVM has been determined for Exp 5 using a Pitot rake[12], i.e. $\delta(M = 0.42) = 18\text{mm}$. Since Exp6 is an extract of the blow-down experiments of Kriegseis et al. [87] no such measurement was made for $M = 0.75$. However, based on standard estimations for the thickness of turbulent boundary layers[143] it can be assumed that $\delta(M = 0.75) \approx 12\text{mm}$.

### 3.2.2 (Post) Processing Strategy

All applied data processing approaches are first recapped according to Chapters 2.1 and 2.2.2.2 and are exemplarily illustrated here based on Exp5.

#### 3.2.2.1 Power Consumption

According to the quantification strategy discussed in Section 2.1 the consumed power of the plasma-actuator $P_A$ and the corresponding characteristic capacitances $C_0$ and $C_{\text{eff}}$ are determined based on Lissajous figure analysis.

Figure 3.5 shows the electrical results of Exp5 under quiescent-air conditions and at a Mach number of $M = 0.42$. At first glance, the Lissajous figure shrinks in the vertical direction but remains constant in the horizontal direction at high-speed conditions, as shown in Figure 3.5(a). Within the measurement accuracy the operating voltage $V$ (abscissa) remains unaffected by variation of the airflow conditions, whereas the amount of transformed charge $Q = C_p V_p$ (ordinate) was reduced significantly. Consequently, the enclosed area of the cyclogram is smaller at high-speed conditions, which leads to a reduced power consumption $P_A$.
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Figure 3.5: Lissajous-figures and characteristic discharge quantities under quiescent air conditions and at $M = 0.42$ (Exp5) characterizing the airflow influence on the electrical discharge performance.
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Furthermore, the parallelism of the flatter capacitance tangents to the cyclograms demonstrates the independence of the cold capacitance $C_0$ from external flow conditions. This result is not surprising, since $C_0$ is the pure passive component of the plasma actuator functioning as a passive load in an electrical circuit (see Section 2.1.3). In contrast, the effective discharge capacitance $C_{\text{eff}}$ reduces with power consumption, as qualitatively indicated by the weaker $C_{\text{eff}}$-slopes in Figure 3.5(a). The histogram-based analysis of the capacitance-time traces

\[ C(t) = \frac{dQ(t)}{dV(t)} \tag{3.2} \]

for $M = 0$ and $M = 0.42$ are shown in Figure 3.5(b). Clearly visible, the effective discharge capacitance is reduced by 30%, whereas $C_0$ remains constant.

3.2.2.2 Light Emission

The discharge light emission is measured to estimate the overall discharge extent of the operative actuator. According to Section 2.2.2.2 the gray-value analysis of the raw images leads to the relative peak intensity

\[ \hat{G} = \frac{G_p - G_b}{G_b} \tag{3.3} \]

and the (chordwise) plasma length

\[ \Delta x = x_{\text{max}}(G > G_b) - x_{\text{min}}(G > G_b), \tag{3.4} \]

where $G_b$ and $G_p$ are the gray values of background and light-emission peak, respectively. The Mach number time trace $M(t)$ of Exp5 and the resulting gray value distribution of the luminosity analysis are shown in Figure 3.6, where the gray values $G(x,t)$ are plotted as a function of chord and time. The distance between the two white lines emphasize the strongly reduced plasma length $\Delta x$ for the duration of the wind-tunnel operation. Moreover, the maximum gray value $G_p$ diminishes, which according to (3.3) weakens the peak luminosity $\hat{G}$ for high flow velocities.
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Figure 3.6: Spatio-temporal gray value distribution $G(x, t)$ of the plasma luminosity (Exp5); the wind tunnel is turned on at $t = 16\text{s}$, the high-speed condition of $M = 0.42$ is fully developed at $t = 20\text{s}$, the wind tunnel is turned off at $t = 60\text{s}$. 
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3.2.2.3 Relative Performance

To characterize the airflow influence on the different experiments by a key number, the relative performance

\[ \Pi_\phi = \frac{\phi(M)}{\phi|_{M=0}} \]  \hspace{1cm} (3.5)

and the relative performance drop

\[ \Psi_\phi = 1 - \Pi_\phi = 1 - \frac{\phi(M)}{\phi|_{M=0}} \]  \hspace{1cm} (3.6)

are defined. In definitions (3.5) and (3.6) \( \phi \) appears as a general variable, which has to be exchanged by the respective quantities, i.e operating voltage \( V \), power consumption \( P_A \), characteristic capacitances \( C_0 \) and \( C_{\text{eff}} \), plasma length \( \Delta x \), or peak luminosity \( \hat{G} \). Both numbers \( \Pi_\phi \) and \( \Psi_\phi \) quantify performance changes due to airflow influence normalized with the quiescent air reference value \( \phi|_{M=0} \). Furthermore, the normalized representation of performance demonstrates the inherent advantage of excluding the influence of state-variable variations when comparing different realizations. These favorable and/or adverse effects appear in the numerator as well as in the denominator of definition (3.5) and therefore largely cancel out.

Table 3.3: Performance specific quantities under quiescent air conditions \( M = 0 \) and at \( M = 0.42 \) (Exp5).

<table>
<thead>
<tr>
<th>( M )</th>
<th>( V [\text{kV}] )</th>
<th>( P_A [\text{W}] )</th>
<th>( C_0 [\text{pF}] )</th>
<th>( C_{\text{eff}} [\text{pF}] )</th>
<th>( \Delta x [\text{mm}] )</th>
<th>( \hat{G} )</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>13.4</td>
<td>18.2</td>
<td>9.4</td>
<td>61.2</td>
<td>5.1</td>
<td>3.1</td>
</tr>
<tr>
<td>0.42</td>
<td>12.9</td>
<td>12.9</td>
<td>9.6</td>
<td>39.8</td>
<td>3.4</td>
<td>2.0</td>
</tr>
<tr>
<td>( \Pi_\phi [%] )</td>
<td>96.3</td>
<td>70.9</td>
<td>102.1</td>
<td>65.0</td>
<td>66.7</td>
<td>64.5</td>
</tr>
<tr>
<td>( \Psi_\phi [%] )</td>
<td>3.7</td>
<td>29.1</td>
<td>-2.1</td>
<td>35.0</td>
<td>33.3</td>
<td>35.5</td>
</tr>
</tbody>
</table>

From the example results of Exp5, as listed in Table 3.3, it can be concluded that the changes of operating voltage and cold capacitance are negligible, since the deviations of \( \Psi_V \) and \( \Psi_{C_0} \) lie within the measurement accuracy. Much more salient is the performance drop of the discharge specific quantities, which exceeds 30% reduction of the electrical (\( \Psi_{P_A}, \Psi_{C_{\text{eff}}} \)) and optical (\( \Psi_{\hat{G}}, \Psi_{\Delta x} \)) quantities.
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3.2.3 Results

3.2.3.1 Power Consumption Quantification

For Exp1 and Exp2 the dependence of the resulting power consumption $P_A$ on different Mach numbers $M$ is shown in Figure 3.7. Even though the power level of Exp2 (Figure 3.7(b)) is roughly four times the power level of Exp1 (Figure 3.7(a)), the performance decreases for both experiments with increasing free-stream velocities.

A detailed comparison of the actuator performance under quiescent air conditions and at a free-stream velocity of $U_\infty = 50$ m/s is given by the combined analysis of Exp3 and Exp4 as shown in Figure 3.8. Both experiments show the expected power-law behavior $P_A \propto V^{\frac{7}{2}}$ and $P_A \propto f^{\frac{3}{2}}$, which has been combined in definition (2.5b) and is extensively discussed by Kriegseis et al. [95].

Furthermore, a performance drop is identified for the entire measured parameter range of Exp4 (---) when compared with Exp3 (——). At first glance, this proportional offset suggests that the decreasing plasma-actuator performance for increasing airflow speeds is independent of the chosen electrical operating parameters, i.e. plasma frequency $f$ or operating voltage $V$ (see Figure 3.9). It will be demonstrated below that this independence is only a first simplification of a more complex relationship.
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![Graphs showing power consumption $P_A$ as a function of operating voltage $V$ and frequency $f$.]

(a) $P_A - V$ diagram; $\sigma_{P_A}^{(3)} = 0.98\%$, $\sigma_{P_A}^{(4)} = 1.69\%$

(b) $P_A - f$ diagram; $\sigma_{P_A}^{(3)} = 1.72\%$, $\sigma_{P_A}^{(4)} = 1.58\%$

Figure 3.8: Power consumption $P_A$ as a function of operating voltage $V$ (a) and frequency $f$ (b); Exp3: $U_\infty = 0$ solid lines ( —— ); Exp4: $U_\infty = 50$ m/s dashed lines ( - - - ).

between airflow and discharge.

However, in order to characterize this proportional offset, the scaling number $\Theta_A$ according to definition (2.5b) was calculated for Exp3 and Exp4. The results are shown in Figure 3.9. Based on the Gaussian fits to the $\Theta_A$ histograms, a quantification of the offset is possible according to

$$\Pi_{\Theta_A} = \frac{\Theta_A|_{U_\infty=50\text{m/s}}}{\Theta_A|_{U_\infty=0\text{m/s}}} = \frac{5.13 \times 10^{-4}}{5.46 \times 10^{-4}} = 0.939. \quad (3.7)$$

The result of (3.7) demonstrates that the average plasma-actuator performance is already reduced by $\Psi_{\Theta_A} = 6\%$ for the Mach number $M = 0.145$, which must be considered a significant drop, $p < 0.001$). Note that the relative performance, its drop and corresponding significance are identical for $\Theta_A$ and $P_A$ according to definition (2.5b), i.e.

$$\Pi_{\Theta_A} = \Pi_{P_A} \quad \text{and} \quad \Psi_{\Theta_A} = \Psi_{P_A}. \quad (3.8a)$$

As a first approximation the performance drop estimation based on $\Psi_{\Theta_A}$ is a convenient measure to identify the airflow influence on the discharge in
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Figure 3.9: Scaling number $\Theta_A$ for free-stream velocities $U_\infty = 0$ (■ Exp3) and $U_\infty = 50$ m/s (■ Exp4).

relation to the quiescent air performance of the plasma actuator. However, the increased relative standard deviation of $\Theta_A$ for Exp4 ($\sigma_{\Theta_A}^{(4)} = 6.1\%$) as compared to the quiescent air reference of Exp3 ($\sigma_{\Theta_A}^{(3)} = 3.3\%$) requires to discuss the airflow influence on the discharge performance in more detail. In the next section, therefore, further influence of the chosen operating voltage $V$ on the performance drop $\Psi$ will be demonstrated.

3.2.3.2 Scaling Number Derivation

To gain a deeper insight into the airflow influence and the corresponding drop of quantities, the calculated relative performances $\Pi_{PA}$ are shown in Figure 3.10(a) for Exp1 and Exp2. Despite the stronger absolute power drop of Exp2 in comparison to Exp1 (cp. Figure 3.7), the decrease of the relative performance $\Pi_{PA}$ is obviously weaker for the higher applied voltages (Exp1: $V = 8$kV, Exp2: $V = 12$kV). The resulting curve of Exp1 demonstrates that a plasma-actuator performance drop of $\Psi_{PA} \approx 10\%$ can occur for Mach numbers $M \approx 0.2$. This result becomes important, when thinking about plasma assisted stall control of UAVs, for instance, since the plasma-actuator control authority will be significantly affected by such a performance drop.

Additionally, the relative performance of Exp4 for operating voltages ranging from $9.8 - 12.7$kV are included in Figure 3.10(a), where the crosses represent the voltage-wise averages of the results. The performance $\Pi_{PA}$ obviously depends on the operating voltage at a given Mach number $M = 0.145$. 

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The experiments with higher operating voltages show less sensitivity to airflow influences as indicated by the arrow in the diagram. This insight indicates limitations of the validity of the assumption that velocity $U_\infty$ and corresponding Mach number $M$ serve properly as the underlying quantities on the abscissa of performance diagrams (see Figure 3.10(a)).

The Mach number, defined as

$$M = \frac{U_\infty}{a_\infty},$$

compares the ratio of two characteristic external airflow quantities, i.e. the free-stream velocity $U_\infty$ and the sonic speed $a_\infty$.

Due to the obvious occurrence of an operating voltage dependent performance drop, it would be convenient, if $M$ could be replaced by a new reference number, which compensates this dependency. If this reference number is denoted by $K$, it would be desirable to obtain constant values of $\Pi_{P_A}(K)$ and $\Psi_{P_A}(K)$ for identical $K$-data but differing operating voltages $V$.

This can be achieved by a reference number definition, where the numerator remains $U_\infty$ as in the definition of $M$, but the denominator is replaced
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by the drift velocity

\[ v_d = \mu E \]  \hspace{1cm} (3.10)

of the momentum imparting ions. In this manner a discharge specific internal velocity measure of the momentum transfer procedure is established. The resulting scaling number for the airflow influence on the plasma-actuator discharge appears as

\[ K = \frac{U_\infty}{v_d}, \]  \hspace{1cm} (3.11)

where \( K \triangleq \frac{\text{external velocity}}{\text{internal velocity}} \).

Within the range of electric field strengths \( E \) of up to \( 10^7 \text{V/m} \), which are relevant for operative plasma actuators, the ion mobility \( \mu_i \) varies by only about \( \pm10\% \) and is almost the same for positive and negative ions. Therefore, an average of \( \mu_i \approx 2 \times 10^{-4} \text{m}^2/\text{Vs} \) of data existing in the literature\[113, 10, 34, 141, 13]\] is chosen as constant ion mobility value for the drift velocity in (3.10). For the sake of completeness, it should be noted that alternatively the EHD number \( N_{\text{EHD}} = I / (w_2 \rho \mu U_\infty^2) \) is used as a non-dimensional number, when the ratio between momentum transfer and airflow inertia is the desired measure (see e.g. Moreau et al. [112] or Soldati and Banerjee [148]).

Applying (3.10) in the denominator of (3.11) yields a reference value of the electric field strength \( E \) for the determination of the drift velocity. For this purpose,

\[ E_0 = \frac{V}{d} \]  \hspace{1cm} (3.12)

is used following the approach of phenomenological plasma-actuator models (e.g. Shyy et al. [146] or Jayaraman and Shyy[81]).

Both, the operating voltage \( V \) and the dielectric thickness \( d \) are available for any conducted experiment or numerical simulation, whereas an accurate determination of the entire electric field distribution \( E(x, y, t) \) is rather challenging for the geometry of plasma actuators. However, \( E \) scales linearly with the applied operating voltage \( V \). For this reason \( E_0 \), according to (3.12) can be used as convenient characteristic of the operating conditions, although it overestimates the field strength of the total distribution.
This overestimation appears as a constant factor for the entire range of $K$, and therefore, the characteristics of $K$ are unaffected by this simplification.

The resulting curves of relative performance $\Pi_{PA}$ and corresponding performance drop $\Psi_{PA}$, as a function of this novel scaling number $K$, are plotted in Figure 3.10(b), opposing the conventional representation of Figure 3.10(a). The former operating voltage dependent discrepancies between the experimental results of the performance drop vanish and the data fall on top of one another within the measurement uncertainty. Similar to Figure 3.10(a), the arrow indicates the voltage-wise orientation of the crosses representing the results of Exp4.

However, this arrow also denotes the characteristic behavior of the data along the resulting universal curve. The direct comparison of Exp1 ($V = 8$ kV) and Exp2 ($V = 12$ kV) clearly shows that the higher operating voltage not only leads to less performance drop, but also to lower values of the scaling number $K$, since $V$ appears linearly in the denominator of (3.11). Consequently, the operating voltage $V$ corresponds to a compression parameter along the universal $\Pi - K$ curve, as indicated by the arrow of Exp4.

To verify the universality of $K$ as a characterizing scaling number for the performance drop at even higher Mach numbers, the resulting values of $\Pi_{PA}$ of Exp5 and Exp6 are plotted in Figure 3.11 together with the data already shown in Figure 3.10. The result of Exp5 exhibits a significantly steeper performance drop as compared to Exp6, although operated at a higher voltage (Exp5: $V = 13$kV, Exp6: $V = 10$kV). The higher voltage in the case of Exp5 is compensated by a thicker dielectric (Exp5: $d = 0.8$mm, Exp6: $d = 0.4$mm), thus causing an even lower electric field strength.

Similar to the representation of the low-speed experiments in Figure 3.10(b), the discrepancies between the experimental results of the performance drop due to dependencies on operating voltage and/or dielectric thickness vanish and both curves collapse in Figure 3.11(b). The data of the low-speed experiments (Figure 3.10) are repeated in Figure 3.11 to demonstrate the identical slope of the $\Pi - K$ curve for the entire velocity range.

Two conclusions can be drawn from the results of Figure 3.11: First, it is demonstrated that the performance drop $\Psi$ can easily exceed 30% even at Mach numbers below 0.5, depending on the operating conditions of the plasma-actuator setup. This significant reduction of the plasma-actuator performance and corresponding control authority emphasizes that the calibration of any discharge based flow-control system (experimental or
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Numerical! is of limited validity, since the error of the estimated plasma-actuator performance increases significantly with increasing deviation from the calibrated reference flow speed (typically \( U_\infty = 0 \)). A calibration conducted in quiescent air inevitable involves two drawbacks: subsequent implementation in numerical simulations results in body-force terms, which are too large; operation of such a pre-calibrated actuator results in a weaker than expected control authority in the respective flow-control experiment.

Consequently, even though it is common practice to neglect any effect of the flow on the plasma actuator, the present results demonstrate the invalidity of this assumption. Second, the limited comparability of the Mach number based performance drop analysis is resolved with the scaling number \( K \). Representing the ratio of external and internal velocity, \( K \) characterizes the performance influence of all measured parameter combinations of flow speed and electrical operating conditions on one and the same curve. The two counteracting parameters \( V \) and \( d \) appear as compression and stretching parameters for the performance drop characterization along the curve, as indicated by respective arrows in Figure 3.11(b). Therefore, this novel scaling number \( K \) is a promising basis for an improved and more universal comparison of airflow-discharge interactions.

3.2.3.3 Luminosity Analysis

The electrical results of Sections 3.2.3.1 and 3.2.3.2 are cross-checked by means of luminosity analysis, as introduced by Kriegseis et al. [93]. Exemplarily, the recorded gray values \( G(x) \) of the discharge-light emission of Exp1 and Exp5 are shown in Figure 3.12. The corresponding gray-value isolines as used to calculate the resulting plasma length \( \Delta x \) according to (3.4) are highlighted white in the diagrams. Further (black) isolines are included in the diagrams in order to improve the readability of the gray-value distribution.

Even though the reduction of the plasma length \( \Delta x \) is negligible at Mach number \( M < 0.15 \), the performance drop can be identified from the local gray-value maxima. Apparently, the decreasing electrical performance \( P_A \) correlates with a decreasing peak intensity \( \hat{G} \) for increasing airflow velocities.

Similar to the power-consumption quantification, as discussed in Section 3.2.3.1, Figure 3.13 shows a detailed comparison of the actuator’s discharge peak intensity \( \hat{G} \) under quiescent-air conditions (Exp3) and at a free-stream velocity of \( U_\infty = 50 \text{m/s} \) (Exp4). The aforementioned offset when comparing the results of Exp3 and Exp4 can be identified from
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Figure 3.11: High-speed influence on the relative plasma-actuator performance $\Pi_{P_A}$ and corresponding drop $\Psi_{P_A}$ (Exp1, Exp2, Exp4, Exp5, Exp6); (a) as a function of free-stream velocity $U_\infty$ and Mach number $M$; (b) as a function of the scaling number $K = U_\infty / v_d$. 
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Figure 3.12: Gray value distribution $G(x)$ of emitted discharge light as function of free-stream velocity $U_\infty$ and corresponding Mach number $M$.
Figure 3.13: Discharge-peak intensity $\hat{G}$ as function of operating voltage $V$ (a) and frequency $f$ (b) for free-stream velocities $U_\infty = 0$ (--- Exp3) and $U_\infty = 50$ m/s (· · · · · Exp4).

Figure 3.13, also considered to be a significant drop (paired t-test: $t = 4.8, df = 15, p<0.001$). This cross-check confirms the hypothesis[93] that electrical performance $P_A$ and peak intensity $\hat{G}$ correlate with one another. Consequently, the performance drop of plasma actuators under airflow impact occurs due to the reduced discharge intensity of the operative plasma actuator.

An achievement of quantitatively comparable results based on the peak intensity $\hat{G}$ would require a careful calibration of the light emission measurement setup. Consequently, a quantitative comparison of $\Pi_{\hat{G}}$ for all experiments is not immediately possible, since the gray value data presented in this manuscript was recorded with different equipment, without the required calibration. The advanced post-processing strategies presented here were developed after the measurement campaigns were completed. Nevertheless, from the comparison of Figures 3.13 and 3.8 it can be concluded that the performance drop of plasma actuators under airflow impact occurs due to the reduced discharge intensity of the operative plasma actuator.

### 3.2.4 Resumee Section 3.2

The adverse effect of the airflow on the plasma-actuator performance has been investigated. The performance and the occurring reduction was identi-
fied and quantified by means of simultaneous Lissajous-figure and discharge-light emission analysis according to the procedures discussed in Chapter 2.

Normalization of the determined performances with the respective quiescent-air performance allowed a quantitative comparison of the relative performance $\Pi$ and the corresponding performance drop $\Psi = 1 - \Pi$ of data obtained at different power levels. It has been demonstrated that the performance drop $\Psi$ can easily exceed 30% even at Mach numbers below 0.5, depending on the operating conditions of the plasma-actuator setup, which represents a significant reduction of the plasma actuator control authority.

In addition, it has been shown that the plasma actuator performance is already reduced by almost 10% for Mach numbers $M < 0.2$. The identification of this drop is important information, especially for lower velocity ranges. Obviously, it is essential to be aware of this reduction when thinking for instance of plasma-assisted stall control of unmanned aerial vehicles (UAVs), since 10% loss in plasma actuator control authority can become a major issue for such flow-control scenarios. Therefore, particular care must be taken when the calibration of any discharge based flow control device is conducted in quiescent air before it is subjected to the intended flow situation.

Obviously, the issue of changing plasma actuator performance, thus control authority, has to be considered for both numerical simulations and experimental parameter studies. On the one hand CFD based estimations will inevitably result in an overprediction of the flow-control success, whereas on the other hand experimentalists will obtain a weaker than expected effect of any experimental DBD-based flow control approach.

Furthermore, the performance drop $\Psi$ affects the impedance of the electrical setup as well, since the effective discharge capacitance of plasma actuators is considerably affected by changing airflow velocities, which changes the magnitude of the load in the electrical circuit. Consequently, any plasma actuator flow control system, which has previously been impedance matched for a particular reference Mach number, can be considerably detuned by simply varying the free stream velocity.

Based on the velocity ratio of the airflow speed $U_\infty$ and the ion drift $v_d$ a new scaling number $K$ has been introduced. Representing the ratio of an external and an internal velocity measure of the discharge, $K$ characterizes the performance influence of the entire measured parameter combinations of airflow speed and electrical operating conditions on one and the same curve in the $\Pi_{PA} - K$ diagram. Consequently, this novel scaling number serves as an improved and more universal basis for the comparison of
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airflow-discharge interactions.

The investigations and results discussed in this section have been published in:

3.3 Environmental Influences: Pressure and Airflow Effect

The close interrelation between breakdown voltage and ambient pressure is well known from the Paschen curves [124]. Above the Stoletow point the breakdown voltage decreases with decreasing ambient pressure for a given geometry (see e.g. Vollrath [162]). Consequently, the discharge intensity changes for changing ambient pressure $p$ at constant operating voltages $V$. Abe et al. [2] and Benard et al. [18, 19], for instance, report a significant increase of power consumption $P_A$ and plasma length $\Delta x$ for decreasing pressure levels in the range of $p = 0.2 - 1$ bar. Pavon et al. [126] mention a pressure influence in combination with their airflow impact reports when changing the plasma actuator location on an airfoil’s relative chord position $x/c$. Furthermore, a comprehensive study of pressure effects by Valerioti and Corke [157] shows the nonlinear relation of ambient pressure and plasma-actuator thrust production for varying pressure levels ($p = 0.17 - 9.0$ bar).

To combine the previously reported insight into pressure sensitivity [161, 69, 169, 18, 19, 2, 157] with the present knowledge about the airflow influence on the plasma-actuator performance [125, 12, 98, 88], the next section addresses the systematic analysis of these effects from simultaneous measurements of both pressure level $p$ and airflow speed $U_\infty$.

3.3.1 Experimental Procedure

3.3.1.1 Wind-Tunnel Facilities

Low and moderate speed experiments at different pressure levels (Exp1, Exp2) were conducted in the Göttinger-type vacuum wind tunnel facility (HoWK) at the Technische Universität Dresden. The main purpose of the HoWK is the realization of density dependent calibration of hot-wire probes, as reported by Frey [59]. The nozzle of the open test section (diameter $d_N = 100$ mm) enters a vacuum chamber, where operating velocities up to $U_\infty = 100$ m/s can be achieved. This chamber can be evacuated down to re-entry conditions [59].

The flow speed was increased up to 100 m/s for Exp1 (Exp2) by increments of 20 m/s (respectively 50 m/s), while operating voltage and frequency were held constant. This procedure was repeated, where the initial pressure level inside the wind tunnel of $p = 1$ bar was reduced by increments of 0.1 bar (respectively 0.05 and 0.2 bar) down to the final pressure
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Figure 3.14: Experimental setup in the HoWK wind tunnel; (a) Test section and actuator plate, (b) electrical setup.

level of \( p = 0.1 \) bar.

3.3.1.2 Plasma Actuator Measurement Setup

Simultaneous measurements of the discharge-light emission and of the electrical quantities were performed at varying airflow velocities and ambient pressures.

The plasma actuator was mounted on an acrylic plate at the beginning of the open test section (see Figure 3.14). The actuator consists of two electrodes (35 \( \mu \)m thick copper tape) separated by a dielectric made of several layers of Kapton\(^{\circledR}\) tape with a total thickness of 0.4 mm. The exposed electrode is \( w_1 = 3 \) mm wide whereas the grounded electrode has a width of \( w_2 = 60 \) mm to prevent a restriction of discharge extension at lower pressure levels. To eliminate the influence of the forming boundary layer, the actuator was mounted directly at sharp leading edge, as indicated in Figure 3.14(a).

The light emission experiments were performed by means of a Photron camera (Photron FASTCAM MC2, 512 \( \times \) 512 pixels, 8 bit, 50 fps) equipped with a Nikon lens (Nikon 105 mm, AF Micro NIKKOR f/2.8D). Additionally, for each parameter setting unscaled snapshots were taken by a Canon Camcorder (Legria HF200, HD, 1920 \( \times \) 1080 pixels) to obtain qualitative color pictures of the plasma domain. The electrical measurement setup comprises an oscilloscope (LeCroy WaveJet354, 4CHs, 500kp/Ch, 1GS/s) to record the (AC sine-wave) operating voltage \( V \) (Testec HVP-15HF, 1000:1) and the probe voltage \( V_p \) (LeCroy PP006A, 10:1) of the
3.3 Environmental Influences: Pressure and Airflow Effect

The charge-probe capacitor $C_p = 22 \, \text{nF}$.

To evaluate the combined influence of ambient pressure and airflow velocity, the simultaneous measurements of electrical and optical discharge characteristics were quantified according to the methods described in Chapters 2.1 and 2.2.2. To characterize the airflow influence on the different experiments by a key number, the relative performance $\Pi_\phi$ and $\Psi_\phi$ are used as introduced in Chapter 3.2.2.3.

3.3.2 Analysis of the combined effects

As an introductory illustration of the parameter space, extracts of the unscaled camcorder snapshots of Exp2 are shown in Figure 3.15, representing the plasma actuator’s spatial discharge distribution. Identical pressure levels are displayed in rows and identical airflow velocities are orientated column-wise. The quantitative results of electrical and light emission experiments are presented in Sections 3.3.2.1 and 3.3.2.2.

Similar to the published images of Abe et al. [1, 2] and Benard et al. [18, 19], the left columns of both images show the pressure influence on the discharge intensity under quiescent air condition. Obviously, the (chord-wise) plasma extent increases considerably for decreasing pressure levels. Moreover, although the effect of the airflow velocity on the discharge distribution is less salient at lower pressure levels ($p = 0.1 - 0.3 \, \text{bar}$), the adverse influence can be recognized with the unaided eye.

3.3.2.1 Pressure Effects

Power consumption $P_A$, plasma length $\Delta x$ and characteristic capacitances $C_0$ and $C_{\text{eff}}$, as obtained from Exp1 and Exp2, are plotted against the pressure $p$ for different velocities ($U_\infty = 0 - 100 \, \text{m/s}, \, M = 0 - 0.29$) in a combined performance diagram in Figure 3.16. At first glance, for all quantities the set of curves of all velocities collapses to one and the same curve, which confirms the preliminary finding, that the velocity influence is considerably weaker than the pressure effects. The values of either discharge specific quantity are reduced by at least one order of magnitude as the pressure level is increased by one order. This result was expected, since it is common practice in gas discharge literature [75, 67, 137] to quantify the involved phenomena in terms of $E/p$, i.e. the ratio of field strength and ambient pressure. Consequently, a pressure reduction leads to the same results as an increase of the operating voltage. Note the constant values for the cold capacitance $C_0$ for all parameter combinations, which
Figure 3.15: Plasma-actuator discharge distribution (Exp2); extract of unscaled camcorder snapshots; rows: constant pressure $p$, columns: constant airflow velocity $U_\infty$; actuator dimensions $(L, w_1, w_2)$ are indicated by white arrows.
3.3 Environmental Influences: Pressure and Airflow Effect

Figure 3.16: Joint performance diagram showing the results of power consumption $P_A$, plasma length $\Delta x$ and characteristic capacitances $C_0$ and $C_{\text{eff}}$ as a function of the pressure level $p$ (Exp1, Exp2); the respective sets of curves correspond to different airflow velocities $U_\infty = 0 - 100 \text{ m/s}$ (■ cp. Figure 3.17).

Confirms the discharge independent pure passive component character of $C_0$ (cp. Kriegseis et al. [93, 95]).

Nevertheless, to neglect the adverse influence of the airflow with respect to the stronger pressure effect would be an invalid oversimplification as will be demonstrated in the following section.
3 Performance Control

Figure 3.17: Physical quantities $\phi$ and relative performance $\overline{\Pi}$ at $p = 0.1$ bar as a function of flow speed (Exp8); (a) power consumption $P_A$, (b) plasma length $\Delta x$, (c) discharge capacitance $C_{\text{eff}}$; - - - corresponding regression lines $\phi^*$ (respectively $\Pi_{\phi}^*$).

3.3.2.2 Superimposed Airflow Influence

To gain deeper insight into the airflow influence at varying pressure levels, the recorded data have to be analyzed separately for the different pressure levels. Exemplarily, the results of the data obtained from Exp1 for velocities of $U_\infty = 0 - 100$ m/s at $p = 0.1$ bar are shown in Figure 3.17, where the measured quantities $P_A$, $\Delta x$, $C_{\text{eff}}$ appear on the left ordinate in the diagrams.

The accuracy of definition (3.5) for $\Pi_\phi$ depends on the quality of the quiescent-air value $\phi|_{M=0}$. Consequently, without further post processing any quiescent air outlier would lead to unreliable curves of relative performance. A linear decrease of relative performance $\Pi$ with increasing flow speed can be assumed for the lower and moderate velocity range ($M \leq 0.3$), based on the recent reports of Kriegseis et al. [88] about airflow-impact analysis of plasma actuators.

To avoid a normalization based on outliers, the regression line

$$\phi^*(M) = \phi^*|_{M=0} + \beta M$$

is fit in a least-squares sense to every data set. Subsequently, all relative performances are determined according to the modified definition (3.5), i.e.
3.3 Environmental Influences: Pressure and Airflow Effect

Figure 3.18: Relative performance $\Pi_\phi$ and corresponding drop $\Psi_\phi$ as a function of airflow speed $U_\infty$, $M$ for different pressure levels $p$ (Exp1); - - - denote respective regression lines $\Pi^*_\phi$, $\Psi^*_\phi$. Note the different ordinate scales in the two rows of diagrams.

$$\Pi_\phi = \frac{\phi(M)}{\phi^*|_{M=0}},$$

(3.14)

where $\phi^*|_{M=0}$ is the quiescent-air value of the regression line $\phi^*(M)$. The normalized regression lines are determined according to

$$\Pi^*_\phi = \frac{\phi^*(M)}{\phi^*|_{M=0}} = 1 + \xi_\phi M,$$

(3.15)

where

$$\xi_\phi = \frac{\beta}{\phi^*|_{M=0}}$$

(3.16)

is the relative slope of the performance.

The relative performances $\Pi_\phi$ according to (3.14) are included on the right ordinates of Figure 3.17 and the normalized regression lines $\Pi^*_\phi$ are included with dashed lines in the diagrams. $\Pi$ and the corresponding drop $\Psi = 1 - \Pi$ of Exp8 are shown in Figure 3.18 as function of airflow speed $U_\infty$, $M$, where the different pressure levels appear in separate diagrams.
3 Performance Control

Figure 3.19: Relative slopes $\xi_\phi$ of the adverse airflow influence as function of the pressure level $p$.

It is obvious from Figure 3.18 that the airflow impact on the discharge is not compensated by favorable pressure effects, since the relative performance drop $\Psi$ is clearly visible. Moreover, the adverse airflow influence on the relative performance becomes more intense at lower pressure levels. This result is more clearly demonstrated by the direct comparison of the relative slopes $\xi_\phi$, which are shown in Figure 3.19. The relative slopes $\xi_\phi$ of all quantities $P_A$, $\Delta x$ and $C_{eff}$, decrease for reduced pressure levels, corresponding to a steeper decline of the relative plasma-actuator performance $\Pi$.

3.3.3 Resumee Section 3.3

The combined analysis of pressure variations and adverse effect of the airflow on the plasma actuator performance has been investigated. The actuator’s performance and its variations were identified and quantified by means of simultaneous Lissajous-figure and discharge-light emission analysis.

In accordance with previous reports of pressure effects, an increasing plasma actuator discharge intensity is observed for decreasing pressure levels. In addition, the adverse airflow influence on the actuator performance is demonstrated for all constant pressure levels. Despite the improved discharge intensity at lower pressure levels, the seemingly improved performance of the plasma actuators is accompanied with a more pronounced drop $\Psi$ of the relative performance $\Pi$. This insight is especially important for flow control applications with transient and/or fluctuating flow situations, as for instance with in-flight applications at different altitudes, flight
3.3 Environmental Influences: Pressure and Airflow Effect

velocities or shock positions in case of transsonic conditions.

Finally, it must be concluded from the present study that any advanced discharge based flow control needs to take into account the airflow impact as well as changing environmental conditions to assure a constant control authority of plasma actuators. First attempts to counteract these influences are discussed in Section 3.1.

The investigations and results discussed in this section have been published in:


3.4 On-line Performance Control

As has been shown in the previous sections, changes of environmental conditions act differently on the plasma actuator performance. An increase in relative humidity, for instance, monotonously reduces the power consumption, but first leads to a flow rate augmentation of the resulting wall jet occurs at moderate humidity levels before an overall decrease occurs for both quantities. Similar non-trivial interrelationships are observed when the ambient pressure changes. The close interrelation between breakdown voltage and ambient pressure is well known from the Paschen curves [124]. In consequence, above the Stoletow point [138] any reduction of pressure increases the discharge intensity. In contrast, the transferred momentum does not change monotonously with decreasing pressure, but rather a (local [157]) maximum is observed for the induced thrust below ambient condition before it decreases under further pressure reductions. Changes in altitude for any aerial vehicle imply simultaneous changes of all state variables, where decreasing pressure and temperature act competitively on the discharge performance. In addition, the aerodynamic influence of the free-stream velocity on the gas-discharge intensity has recently been identified to significantly reduce the actuator performance (see Sections 3.2 and 3.3).

Obviously, the variety of possible combinations of promoting and diminishing effects on the discharge intensity limits the predictability of environmental influences on the plasma actuator performance.

To improve the predictability and to ensure constant plasma actuator performance during operation, a real-time and in-situ evaluation of the performance is desirable. The following section addresses this requirement with a real-time-characterization approach, which allows a closed-loop performance control of the electrical plasma actuator setup. Due to the closed-loop character of the proposed control concept, a compensation for any performance changes can be successfully achieved without a detailed knowledge about the complex interrelationship between the involved thermodynamic and kinematic environmental parameters.

In a first step the above presented performance monitoring approaches are modified such that the required data is continuously acquired and processed to provide online and continuous performance data at a sufficiently high rate. Then, in a second step, any identified performance changes can be counteracted using a closed-loop control circuit, based on the known interrelations of the involved quantities.

This novel tool is then tested in an artificial flow situation of changing pressure and Mach number provided by a blowdown wind tunnel. However,
3.4 On-line Performance Control

Figure 3.20: Sketch of experimental setup. (a) wind tunnel test section and overhead camera (CAM); (b) detailed view of electrical plasma actuator setup and closed-loop circuit comprising function generator (FG), power supply (PS), high voltage (HV) transformer, notebook computer (NB) and plasma actuator.

the main objective of the next section is to outline the potential of the closed-loop control concept for discharge based aerodynamic flow control.

3.4.1 Experimental Procedure

The experiments were conducted in the blowdown wind tunnel of the Technische Universität Darmstadt.

This wind tunnel is designed to operate at Mach numbers in the range of $0.4 < M < 4$. The dimensions of the test section are $150 \times 150 \text{ mm}^2$. The plasma actuator consisting of two copper electrodes ($w_1 = 2.5\text{ mm}$, $w_1 = 10\text{ mm}$) and a Kapton dielectric ($d = 0.4\text{ mm}$) was mounted on the side window of the test section, as shown in Figure 3.20. To retroactively verify the online evaluation of the performance, similar to previous investigations [93, 12, 88] a CMOS camera (Phantom V12.1, $512 \times 512$ pixels, 24fps; Nikon 105 mm, AF Micro NIKKOR f/2.8D) was used to record the spatio-temporal light emission of the discharge during the power-consumption analysis.

The electrical control circuit, as shown in Figure 3.20(b), was built up using a computer based digital oscilloscope (Picotech PicoScope4424, 4CHs, 2500p/Ch, 10MS/s) to record the operating voltage $V$ (Testec HVP-15HF, 1000:1) and the voltage $V_p$ across the charge probe capacitor $C_p = 22\text{nF}$.
3 Performance Control

Figure 3.21: Transient flow conditions during the experiments. Time traces of static and total pressure $p, p_t$, Mach number $M$; characteristic times are labeled as $t_1, t_2, t_3$.

(LeCroy PP006A, 10:1). The operating voltage $V$ was generated by a high voltage transformer circuit board (GBS Elektronik, Minipuls2), which was driven by an external laboratory power supply (Volcraft VSP 2410, variable output 0–24 V DC) and a function generator (GW Instek, SFG-2004, fixed frequency (sine): $f = 12.0$ kHz). The power consumption and the light emission analysis is conducted as described in Chapters 2.1 and 2.2.2.2.

Reproducible airflow conditions were achieved, operating the blowdown wind tunnel at a constant valve position, thus generating a blowdown from initially $M = 0.85$ (cruise flight) to quiescent air at changing pressure levels ($p = 0.9–1.5$ bar). The resulting time traces of the state variables static and total pressure $p, p_t$ are shown in Figure 3.21, alongside the Mach number $M$.

In Figure 3.21 three characteristic times are highlighted for $p(t_1) = p_{\text{max}}, p(t_2) = p_0, p(t_3) = p_{\text{min}}$ with the purpose of distinguishing pressure and air-speed effects on the discharge performance. For the sake of completeness, the interdependency of pressure and Mach number is stated, i.e.

$$
\frac{p}{p_t} = \left( \frac{\gamma - 1}{2} M^2 + 1 \right)^{\frac{1}{\gamma - 1}},
$$

where $\gamma$ is the isentropic exponent of gas ($\gamma = 1.4$ for diatomic gases).
3.4.2 Online Diagnostic and Control Approach

The recorded data of operating voltage $V$ and probe voltage $V_p$ were continually acquired by the notebook computer from the digital oscilloscope. The consumed power $P_A$ for monitoring or controlling purposes was computed from voltage-charge cyclograms for every time step $t^j$ as discussed before. When using the diagnostic tool in closed-loop control mode, the algorithm furthermore compares $P^j_A$ with a pre-set power level $P^*_A$ and calculates the deviation

$$\Delta P^j_A = P^j_A - P^*_A$$

(3.18)

as the relevant control error. The control signal for the next time step $t^{j+i}$ is estimated based on a PID control algorithm

$$\Omega^{j+1} = K_p \left( \Delta P^j_A \right) + \frac{1}{T_i} \int_0^{t^j} \left( \Delta P^j_A \right) dt + K_d \frac{d}{dt} \left( \Delta P^j_A \right).$$

(3.19)

This type of controller has been chosen for the ease of adjusting the controller gains for robust and stable controller characteristics. After the output $\Omega^{j+1}$ has been calculated it is fed into the control input of the power supply, where it is transformed directly into the corresponding voltage for the high voltage transformer (see Figure 3.20(b)).

For all experiments, the quiescent air operating conditions of $f = 12$kHz, $V = 10$kV and $p_0 = 1.028$bar resulted in an initial power consumption $P_A = P^*_A = 7.1$W for $t < 0$s. The experiments and corresponding basic parameter settings are listed in Table 3.4.
Table 3.4: List of conducted experiments and corresponding basic parameter settings; constant settings: \( f = 12 \text{kHz}, V = 10 \text{kV}, P_A^* = 7.1 \text{W} \) and \( p_0 = 1.028 \).

<table>
<thead>
<tr>
<th>Exp.-Nr.</th>
<th>controller concept</th>
<th>time step ( \Delta t ) [ms]</th>
<th>characteristic times [s]</th>
<th>Mach number ( M )</th>
<th>max./min. static pressure [bar]</th>
</tr>
</thead>
<tbody>
<tr>
<td>ExpA</td>
<td>-</td>
<td>500</td>
<td>( t_1 ) ( t_2 ) ( t_3 )</td>
<td>2.4 7.8 14.5</td>
<td>0.84 1.45 0.90</td>
</tr>
<tr>
<td>ExpB</td>
<td>PD</td>
<td>500</td>
<td>( t_1 ) ( t_2 ) ( t_3 )</td>
<td>2.4 7.8 14.5</td>
<td>0.84 1.45 0.90</td>
</tr>
<tr>
<td>Exp1</td>
<td>-</td>
<td>17.3</td>
<td>( t_1 ) ( t_2 ) ( t_3 )</td>
<td>3.1 13.8 22.3</td>
<td>0.86 1.52 0.89</td>
</tr>
<tr>
<td>Exp2-5</td>
<td>PI</td>
<td>17.3</td>
<td>( t_1 ) ( t_2 ) ( t_3 )</td>
<td>3.1 13.8 22.3</td>
<td>0.86 1.52 0.89</td>
</tr>
</tbody>
</table>
3.4 On-line Performance Control

Table 3.5: List of tasks to be processed during each performance-control iteration and corresponding (average) run time.

<table>
<thead>
<tr>
<th>task</th>
<th>description</th>
<th>average run time [ms]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.</td>
<td>data acquisition</td>
<td>15.9</td>
</tr>
<tr>
<td>2.</td>
<td>filtering</td>
<td>0.8</td>
</tr>
<tr>
<td>3.</td>
<td>data separation</td>
<td>0.3</td>
</tr>
<tr>
<td>4.</td>
<td>power computation</td>
<td>0.1</td>
</tr>
<tr>
<td>5.</td>
<td>control computations</td>
<td>0.1</td>
</tr>
</tbody>
</table>

3.4.3 Advanced Control Circuit

To develop a more effective control algorithm it is necessary to reduce the size of the control loop, i.e. minimize $\Delta t$. In order to eliminate the time consuming communication between both software packages, the source code for the Lissajous-figure analysis was translated from Matlab to LabVIEW. In addition, a program parallelizing multiple tasks has been developed to optimize the computational time for each time step. This program uses pipe-lining technology and is especially powerful on multicore processors.

The data acquisition of the digital oscilloscope samples the two channels ($V$ and $V_p$) in block mode with 250 data points at a sample rate of 1 MS/s. These data points are filtered using a Savitzky-Golay-filter [142] and then separated cycle-wise ($K = 1$) into individual Lissajous-figures á 83 data points. For each block the power consumption is calculated from equation (2.2), based on which the new control signal of the closed-loop control circuit is estimated according to equation (3.19). The basic tasks processed for each iteration are summarized in Table 3.5. Furthermore, the table shows the average run time of the tasks for a standard dual core laptop computer (2.2 GHz, 4GB RAM).

For an optimized pipe-lining-program all parallelized tasks have to be distributed over the available CPU cores to minimize the overall runtime. Obviously, the data acquisition (task 1) takes most of the time. Therefore, at the same time as the current data block is acquired using CPU#1 the program performs all other computations (tasks 2-5) with the previous data block using CPU#2 as sketched in Figure 3.22. In total the program needs about $\Delta t = 17.3$ ms to perform a complete cycle of data acquisition and computations for 250 data points per channel at a sampling rate of 1 MS/s, which is an improvement of one order of magnitude with respect to
3 Performance Control

Figure 3.22: Timing diagram for the pipelined performance control circuit running on two CPU cores (according to NI Developer Zone).

consumed time.

As the repetition rate of the controller was increased, the system dynamics also changed. Practice showed that the PD-controller used before was not suitable for the given noise level, as the $\Delta t$ reduction resulted in higher noise gradients. Consequently, a PI controller based on Eq. (3.19) was chosen.

In order to determine the controller gains the open loop step response was analyzed using the Ziegler-Nichols step response method [170]. In the literature there are different ways shown for determining the controller gains $K_p$ and $T_i$. Åström and Hägglund [11] determine the system latency with the help of a line of best fit through the point of the highest slope of the process variable whereas the PID Control Toolkit user manual from National Instruments uses a reference point of the process variable which has 63.2% of the maximum value of the process variable.

Additionally the time gap $\Delta t$ between two measurements is still in the same order of magnitude as the system latency (see Figure 3.23), such that an accurate determination of the system latency beyond the temporal resolution of the control circuit is impossible. For the step response shown in Figure 3.23 this led to a proportional gain $K_p$ ranging from 0.2 to 1 and an integral time $T_i$ ranging from 0.05 to 0.1. Therefore these values served as a first starting point for tuning the controller gains manually. Based on this semi-manual procedure it was possible to implement a PI controller with reduced overshoot, minimized steady state error and suppress oscillations.

3.4.4 Demonstration of the automated performance control

Five experiments were conducted with the advanced control algorithm based on the PI-control concept. Exp1 was operated in monitoring mode to
provide a reference for the discharge performance variations. Subsequently, four experiments in control mode (Exp2-Exp5) were conducted, see Table 3.4.

### 3.4.4.1 Monitoring/Controlling

Figure 3.24(a) shows the result of the new monitoring experiment (Exp1). The improvement of the diagnostic tool due to the increased sampling rate is clearly visible even in the monitoring mode.

At the first instant of time after the wind tunnel valve is opened at $t = 0$ the typical actuator power peak can be recognized in the diagram due to the passing expansion wave. Thereafter, the significant power drop occurred at $t_1$ ($P_A = 3.7$ W) under adverse pressure and airflow conditions. At $t_2$ the performance reduction solely due to airflow influence is less pronounced ($P_A = 6.1$ W), before the favorable pressure conditions lead to an increased performance of $P_A = 9.6$ W at $t_3$. The wind tunnel valve is closed at $t = 43$ s.

The results of Exp2 (controlling mode) are shown in Figure 3.24(b) as a direct comparison to Exp1. Both initial performance peak and subsequent drop are reduced significantly as the controlled mimics the respective slopes with opposite signs. At $t ≈ 7$ s the variations of environmental conditions are entirely compensated, i.e. the remaining control error $\Delta P^j_A$ is minimized.

Qualitatively, the slope of the control signal $\Omega$ of the controlled case directly mirrors the slope of the power $P_A$ of the uncontrolled case.
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![Graphs of consumed power and control signal for monitoring and controlling modes of operation.](image)

(a) monitoring mode (Exp1)  
(b) controlling mode (Exp2)

Figure 3.24: Results of consumed power $P_A$ and control signal $\Omega$ for (a) monitoring and (b) controlling mode of operation (advanced PI-control concept).

3.4.4.2 Parameter Optimization

Although considered an artificially strong change of the environmental conditions, the extreme performance gradients in the time span between $t = 0$ and $t_1$ emerged as an excellent test case scenario for further control circuit optimization efforts. In particular, an interval $J$ was defined, which was used to determine the relative standard deviation

$$\sigma_{P_A} = \frac{1}{P_A^*} \sqrt{\frac{1}{J-1} \sum_{j=1}^{J} \left[ P_A^j - P_A^* \right]^2}$$

of the controlled cases as a measure of control success. Different combinations of control parameters $K_p$ and $T_i$ were implemented in the control algorithm (3.19), as listed in Table 3.6 together with the respective values of $\sigma_{P_A}$. The results of all experiments are shown in Figure 3.25.

The reduction of the control error is reflected directly in the standard deviation, which is 24.22% for the open loop (monitoring) experiment Exp1 and has been reduced to 1.7% for the optimized closed loop controller in experiment Exp5. However the success of control is highly dependent on the choice of the control parameters. A higher control gain $K_p$ increases the speed of the controller but comes at the cost of an increasing actuating variable which is naturally limited. The influence of the controller gain can be seen in the beginning of the experiment at the difference of Exp4 in comparison to Exp5, where the first performance drop is reduced from
3.4 On-line Performance Control

Table 3.6: List of control parameters $K_p$ and $T_i$ as used for equation (3.19); relative standard deviation $\sigma_{P_A}$ for the chosen interval $J$ of $t_{\text{max}} - t_{\text{min}} = 10$ s (cp. Figure 3.25).

<table>
<thead>
<tr>
<th>Exp</th>
<th>$K_p$</th>
<th>$T_i$</th>
<th>$\sigma_{P_A}$ [%]</th>
</tr>
</thead>
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<td>monitoring</td>
<td></td>
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</tr>
<tr>
<td>Exp2</td>
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<td>5.99</td>
</tr>
<tr>
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<td>0.2</td>
<td>0.03</td>
<td>5.73</td>
</tr>
<tr>
<td>Exp4</td>
<td>0.06</td>
<td>0.005</td>
<td>9.07</td>
</tr>
<tr>
<td>Exp5</td>
<td>0.1</td>
<td>0.005</td>
<td>1.70</td>
</tr>
</tbody>
</table>

Figure 3.25: Comparison of the actuator power consumption $P_A$ for different combinations of control parameters $K_p$ and $T_i$ as used for equation (3.19); considered evaluation interval $J$ is indicated by arrows (cp. Table 3.6).
3 Performance Control

\[ P_{A}^{\text{min}} = 4.43 \text{ W to } P_{A}^{\text{min}} = 5.57 \text{ W} \] by an increase of \( K_p \). Figure 3.25 also yields that a reduction of \( T_i \) enhances the control performance by decreasing the steady state error, but this also decreases the stability margin of the controller. If \( T_i \) is chosen to low the controller shows heavy oscillations which lead to instability.

3.4.5 Resume Section 3.4

This section demonstrates an automated counteraction of changing environmental conditions, i.e. changes of pressure level and airflow speed, on the resulting plasma actuator performance.

Continuing in the direction of previous studies \([12, 93, 95, 88] \), a novel online characterization approach is introduced, allowing an in-situ condition monitoring of the discharge performance. Moreover, this new tool features a closed-loop control circuit, which assures constant plasma actuator performance independently of the environmental conditions. A PI-control algorithm is successfully implemented, closing the loop between discharge measurements and power supply of the electric setup. Based on the relative performance \( \Pi \) as evaluation measure, the slope of the control signal \( \Omega \) of the controlled case were identified to directly mirror the slope of the power \( P_A \) of the uncontrolled case.

From this result the conclusion can be drawn, that by means of steadily processed real-time performance data it is possible to achieve a constant plasma actuator performance during operation under fluctuating and transient flow conditions. This is an important insight implying significant consequences, since beyond the common purpose of favorably manipulating the airflow, any advanced DBD-based flow control system will necessarily require an appropriate closed-loop performance control of the discharge device.

First successful incorporation of the closed-loop control circuit for in-flight experiments on a full sized motorized glider is demonstrated by Duchmann et al. (Chapter 5). Application of the online-control tool assures constant control performance of the actuator despite variations of the environmental conditions (relative humidity, temperature, density) during transition control experiments.
3.4 On-line Performance Control

The investigations and results discussed in this section have been published in:

3 Performance Control
4 Transition Control

Chapter 4 gives an overview on the preparatory developments of experimental procedures for subsequent in-flight experiments. The developments of new concepts for transition control with the goal to promote effectiveness and efficiency of the entire flow-control system are discussed.

The mechanisms discussed in this work are depending on the expenditure of additional energy and therefore belong to the field of active flow control. Besides distinguishing reactive and predetermined control, flow-control mechanisms can be classified into effects that result from a modification of the mean flow and effects that are based on a modification of velocity fluctuations without an effect on the mean flow.

The active transition delay using plasma actuators can be categorized into these two groups: The boundary layer stabilization can be achieved using a continuous mode of operation, as presented by the author in [71]. With this approach a quasi-steady momentum is added to the flow, directly acting on the mean velocity profile of the boundary layer. As can be shown with linear stability analysis (LSA), the stability features of the boundary layer downstream of the plasma actuator are modified in such a way that the amplification of the disturbances is impeded and transition can be delayed.

On the other hand, unsteadily operated actuators can be used to act (or counteract) directly on the instabilities growing inside the boundary layer, which otherwise would lead to an early transition to turbulence. This approach is called active wave cancellation (AWC). The waves are velocity fluctuations inside the boundary layer and are sensed using appropriate sensors. From this information a control algorithm continuously calculates an operating signal for the downstream situated actuator to cancel out these velocity fluctuations without modifying the mean flow. The active wave cancellation is a procedure with a long history in fluid mechanics. As actuators for this purpose, zero-net mass flow actuators for suction and blowing through slots or mechanically driven membranes on the surface as actuators are most established. The feasibility of active wave cancellation using plasma actuators was first demonstrated by the author in [72].

The investigations discussed in this chapter are the consequent advancement of the previous proofs-of-concept. Previous works were conducted
at relatively low velocities below $10\,\text{m/s}$. One of the main goals of the subsequent work was to increase the free-stream velocity to above $30\,\text{m/s}$ to prepare the flow control systems for in-flight experiments. Besides the low velocities, other shortcomings of previous work are the use of artificial disturbance sources to excite TS waves and their too large amplitudes. To eliminate these weaknesses more sophisticated, surface mounted sensors as well as powerful closed-loop control algorithms had to be employed.

Section 4.1 focuses on the boundary-layer stabilization (modification of the mean flow). The main achievements are the elimination of the artificial excitation of the TS waves—in other words: the ability to delay naturally occurring transition—, a more than doubled free-stream velocity of $20\,\text{m/s}$ and a numerical and analytical background for the hydrodynamic stability as the main mechanism for this boundary-layer control approach.

Further advancements of the active wave cancellation procedure are presented in Sections 4.2, 4.3 and 4.4. A large jump forward was made by incorporating high sophisticated control algorithms, specially developed for this purpose. These algorithms were kindly provided by Prof. Rudibert King and Dr. Nikolas Goldin from the Technische Universität Berlin. In order to prepare future in-flight experiments, the AWC experiments were directly conducted on an aerodynamic airfoil, i.e. a wing glove for an aircraft, which is described in detail in Section 5.1.2. The first subsection (Section 4.2) is called Classic Approach because the operation mode of the plasma actuator is almost unchanged as compared to previous work: the actuator is operated at a carrier frequency to create the plasma. This carrier frequency is amplitude modulated at the TS wave frequency. However, a novelty is the almost sinusoidal amplitude modulation of the carrier frequency in contrast to the rectangular modulation in previous works. This type of modulation was made possible by a new high voltage generator, Minipuls 2.1, specially developed for these experiments.

The boundary-layer stabilization requires non-negligible energy expenditure since it is based on the manipulation of the mean flow. The big advantage of this procedure is the broadband effectiveness, independent of the orientation of the waves with respect to the actuator and of their quality and frequency content. Concerning the efficiency and energy requirements the active wave cancellation has clear advantages but requires relatively clean waves parallel to the actuator. Sensors and control circuits are necessary. The advantages and disadvantages of both procedures inspired the idea to combine both methods in one boundary layer. The AWC
would be applied upstream, where clean and two-dimensional waves with low noise are still present. Farther downstream, after restarted growth of the waves, an actuator for boundary-layer stabilization might be successful in once more reducing the amount of disturbances in the boundary layer. Section 4.3 describes an experiment in which both methods are combined in one single actuator. This actuator is operated continuously to conduct the boundary-layer stabilization and "on top" of this continuous forcing, an amplitude modulation additionally performs an active-wave cancellation. Or the other way around: the amplitude modulation for the active wave cancellation is conducted with a DC offset to simultaneously perform a boundary-layer stabilization. This new concept proves very effective, capable of delaying transition on an airfoil much farther than each single method.

A completely different but equally new AWC concept is presented Section 4.4. It is based on the fact that the force production of the plasma actuator is highly unsteady on the timescale of the operation frequency of the actuator. The working principle of plasma actuators is based on the application of an AC voltage that excites one discharge event during each half cycle. The discharges take place during the positive and negative slopes of the supply voltage and the force production is coupled to these discharge events. The two discharges produce force peaks with opposing directions (push and pull events). A significant difference in the magnitude of the force of both events yields a net force that remains after one complete cycle. This unsteadiness is usually made negligible by operating the actuator at a frequency orders of magnitude higher than the frequencies relevant for the flow. For the Direct Frequency Mode, discussed in Section 4.4, it is the unsteadiness of the force production at the timescale of the supply voltage that is used for counteracting the Tollmien-Schlichting waves. To achieve this, the plasma actuator is operated at the same frequency as the TS waves and the phase relation between the operating signal and the waves is optimized to achieve an attenuation of the waves. In this manner the push and pull events do not partially annihilate each other leaving only a weak net force, but each single push and pull event is exploited to manipulate the flow. This should lead to an order of magnitude increase in efficiency. The new actuation principle turns the disadvantage of plasma actuators of creating opposing forces within one cycle, into an advantage and makes full use of this feature. Another motivation for the application of the direct frequency mode is the following: With the classic operation mode, plasma actuators are operated at a carrier frequency an order of magnitude above
4 Transition Control

de the velocity fluctuations (TS waves) inside the boundary layer. This carrier frequency is amplitude modulated at the TS wave frequency in order to cancel these fluctuations. At higher free-stream velocities the frequency of the TS waves increases and quickly reaches several kilohertz. This leads to a conflict with the carrier frequency, which cannot be arbitrarily increased. A natural limit for the frequency of the TS waves, that can be cancelled with this approach, exists. This is not the case for the the direct frequency mode. More explicitly: TS waves of several tens of kilohertz can theoretically be cancelled, although higher frequencies might cause difficulties due to the previously mentioned limit for the plasma actuator operation frequency.


4.1 Boundary-Layer Stabilization

In order to further advance the development of the previously demonstrated technique for delaying the transition by boundary-layer stabilization the complexity of the experimental setup is increased in several steps. The objective is to gradually move to more realistic conditions as will be found in in-flight experiments. One main requirement is the increase of the free stream velocity to the lowest flight-velocities of the research aircraft of the Technische Universität Darmstadt. Another important modification of the experimental procedure is to omit the artificial excitation of Tollmien-Schlichting waves in order to delay the naturally occurring transition.

To meet these increased requirements a flat plate wind-tunnel setup is used in the investigations presented in this setup. The free stream velocity is increased from 6-10m/s in the previous experiments to 20m/s and a displacement body under the ceiling of the test section of the wind tunnel is designed to create a pressure gradient that promotes the naturally occurring transition to take place at a desired location on the flat plate.

Boundary-layer measurements along a flat plate are conducted with hot-wire anemometry, accompanied by particle image velocimetry (PIV) for the characterization of the actuator-induced flow modifications. A stability analysis reveals more detailed insight into the stabilizing effect and enables empirical transition prediction. In contrast to other investigations, the DBD actuator is operated at a frequency about \(20\) times larger than the unstable Tollmien-Schlichting wave frequency, such that it’s effect on the flow can be described as a time-invariant body-force distribution even in terms of hydrodynamic stability.

4.1.1 Experimental Procedure

The experiments reported within this chapter are conducted following the guidelines of Saric [154, pp. 886 - 896] who provides a good overview of the difficulties involved in measuring boundary-layer transition. The transition experiments are conducted in an open-circuit wind tunnel with a 1 : 24 contraction nozzle. An average turbulence intensity of \(Tu = 0.24\%\) is measured at the inlet of the 450mm \(\times\) 450mm test section, containing a flat plate of 1600mm length with an 1 : 6 elliptical leading edge and inclinable trailing-edge flap for adjustment of the stagnation point. The flat plate provides an insert to flush mount a single dielectric barrier discharge actuator 350mm downstream of the leading edge. Since transition would naturally occur far downstream in the test section for the given velocity,
4 Transition Control

Figure 4.1: Displacement body and flat plate in test section. Maximum contraction located at leading edge ($x = 0$mm).

A displacement body is installed on the tunnel upper wall (Fig. 4.1) to destabilize the laminar boundary-layer flow. The maximum thickness of the displacement body is located at the beginning of the flat plate, creating an adverse pressure gradient downstream of the leading edge.

In order to quantify the pressure gradient, the free-stream velocity is measured at several streamwise locations ($x = 0.21 - 0.69$m). The results of the measurement for a nominal free-stream velocity of 20m/s are illustrated in Fig. 4.2. Circles indicate the dynamic pressure calculated from the velocity signal of a reference sensor (vane anemometer at the leading edge) during simultaneous hot-wire measurements outside of the boundary layer ($y = 25$mm) along the flat plate (squares). A linearized fit for the hot-wire signal, converted into dynamic pressure, is applied and the parameters are indicated in the diagram. Whereas the wind-tunnel bulk velocity $u_{Ref}$ at the leading edge of the flat plate remains constant throughout all measurements, the downstream evolution of the hot-wire velocity $u_{CTA}$ indicates a positive pressure gradient of 83.6Pa/m along the test section. The pressure gradient can be normalized by considering the evolution of $c_p = 1 - (u/U_\infty)^2$ along the plate. For velocities of $U_\infty = 18 - 22$m/s, pressure data collapse along a straight line and yield $\Delta c_p/\Delta x = 0.4m^{-1}$. This closely resembles the conditions on the pressure side of the wing glove used for the in-flight experiments. Since a pressure gradient is imposed for these experiments, no analytical solution for the boundary-layer development is available to compare the experimental results with. For the same reason, the concept of the virtual leading edge, which is commonly used to account for leading-edge effects in the experiment and to relate the downstream dimension to the analytical Blasius solution, cannot be applied here.

A DISA 55M01 hot-wire bridge is used to measure flow velocities in constant temperature setting. The single-wire probe can be traversed via
4.1 Boundary-Layer Stabilization

Figure 4.2: Pressure gradient along the flat plate at $U_\infty = 20$ m/s. Illustrated are the dynamic pressures calculated from the averaged reference velocity and the hot wire velocity signal.

The actuator effect in the immediate vicinity of the electrode is characterized using a Dantec Nano-S 2D – 2C particle image velocimetry system similar to the setup used in [41]. The Dantec FlowSense 2M CCD camera with $1600 \times 1200$ pixel resolution and equipped with a Nikon AF Mikro Nikkor 105mm lens is adjusted to a field of view of $32\text{mm} \times 24\text{mm}$. A Nd:YAG Litron dual-cavity laser with a Dantec 80X70 light-sheet optic provides a maximum power output of $135\text{mJ}$ per cavity to illuminate the DEHS-seeded wind-tunnel test section. The repetition rate of the laser pulses is limited to 15Hz. In order to reduce background light reflections a monochromatic filter at the laser wavelength of $\lambda = 532\text{nm}$ is installed on the lens. 10bit images are acquired in double-frame mode and for the correlation of the velocity vectors, an interrogation area size of $16 \times 16$ pixels with an overlap of 50% is chosen.
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Figure 4.3: Lissajous figure of actuator charge $Q$ vs. voltage $V$.

4.1.1.1 Actuator Characterization

The flat plate provides an insert for a single DBD actuator 350mm downstream of the leading edge (compare Fig. 4.1). The actuator extends 400mm in the spanwise dimension and exerts a force in the streamwise direction. Copper tape of 75$\mu$m thickness constitutes the exposed and covered electrodes which have a width of 2.5mm and 10mm, respectively. Between the electrodes, five layers of Kapton$^{\text{R}}$ tape with a total thickness of 300$\mu$m provide the dielectric barrier. A sinusoidal high voltage is applied via a MiniPuls 2.1 generator from GBS Elektronik. The resonant frequency of this combination of actuator and high voltage generator is $f = 6$kHz, yielding optimal efficiency of the body force generation. This frequency is kept constant throughout the investigation. Forte et al. [58] first measured a temporal unsteadiness of the force production for cyclic voltage supply. Despite the alternating polarity and associated unsteadiness of the body force production, the frequency is well beyond the receptivity limit of the boundary-layer flow investigated. Stability analysis shows that all disturbances in the high frequency range are effectively damped and do not influence the transition process.

The actuator peak-to-peak voltage $V$ is varied between 8 and 13kV. The power consumption is a function of the applied voltage and the dielectric material. It can be measured by integration of the Lissajous plot of actuator charge $Q$ over voltage $V$ as introduced by Manley [106]. Pons et al. [130] first employed a monitor capacitance for single DBD actuators whereas details on the power measurements for the present actuator con-
4.1 Boundary-Layer Stabilization

<table>
<thead>
<tr>
<th>$V[kV]$</th>
<th>$P_A[W/m]$</th>
<th>$F[mN/m]$</th>
<th>$u_{max}[m/s]$</th>
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</tr>
</tbody>
</table>

Table 4.1: Comparison of actuator voltage, power consumption, integral thrust and maximum induced velocity in quiescent air for the investigated actuator settings.

figuration are found in Kriegseis et al. [95]. The Lissajous plot for the case of $V = 10kV$ is shown in Fig. 4.3. In this configuration, the 400mm long actuator consumes 18.4W of electrical power which translates to a relative power consumption of $P_A = 45.9W$ per meter actuator length. Kriegseis et al. [93] describe a correlation between the power consumption of DBD actuators and the thrust measured with a force balance. It has to be kept in mind that the measured thrust is not identical to the force produced by the actuators. Only about 70% of the produced force is used for flow acceleration since wall-friction forces have to be considered. More sophisticated measurements [97] enable the characterization of the spatial body force distribution next to the actuator and discussion of the parasitic force terms. Such data enables extraction of the actual body force imposed on the fluid and delivers a force field suitable for implementation in computational simulations as shown in Section 4.1.3.1.

Since the relation between consumed power and actuator thrust collapses onto a straight line for different operation frequencies in [93] and an identical actuator setup is used in this study, the actuator thrust $F$ per meter actuator length can be estimated. The evaluated combinations of actuator voltage, power consumption and integral body force are presented in Table 4.1. The results suggest a slight non-linear dependence between actuator voltage and power consumption. The relation between power consumption and produced thrust is approximately linear. Both observations are in accordance with results reported by Kriegseis et al. [93].

In quiescent air, DBD actuation induces a laminar wall-jet which scales with similarity laws formulated by Glauert [65]. The jet can be charac-
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terized by its thickness and the maximum induced velocity $u_{max}$ which is appended to Table 4.1. The ratio between this maximum induced velocity and the free-stream flow velocity is commonly used [64, 105] for quantification of the actuator impact. For the presented cases, the maximum wall jet velocity is measured in quiescent air using PIV and related to the free-stream velocity of $U_\infty = 20 \text{m/s}$, yielding a ratio in the range of $0.05 - 0.29$. These low values do not lead to an overshoot of the velocity profiles with associated destabilization. However, the authors wish to emphasize that the maximum velocity alone does not sufficiently characterize the integral impact of the additional momentum and that the integral actuator thrust is a more convenient quantity for impact quantification. Henceforth, the integral thrust production or the equivalent power consumption are utilized when referring to different actuation cases. A detrimental effect of increased airflow velocities on the power consumption is reported by Kriegseis et al. [96] and Magnier et al. [105], and an according decrease of the actuator thrust may be assumed. Nevertheless, Kriegseis et al. [96] experienced negligible effects for flow velocities below 50 m/s in the same wind tunnel with the same actuator setup. Therefore, such velocity effects are neglected in the following. Since the airspeed is kept constant, such effects would influence all power settings in the same way, scaling the absolute values but maintaining the relative dependencies.

4.1.2 Hot-wire Transition Measurements

Under conditions of low free-stream turbulence and in the absence of acoustic disturbances or surface roughness, the boundary-layer transition is expected to be triggered by two-dimensional Tollmien-Schlichting waves. The linear amplification process of these modal disturbances is governed by the stability of the boundary layer which is accessible by analysis of the Orr-Sommerfeld equation. The later stages of transition are characterized by breakdown of the Tollmien-Schlichting waves into three-dimensional, chaotic structures and increasing velocity fluctuations inside the boundary layer. The stochastic nature of the transition process complicates repeatability and sets high demands on experimental accuracy.

Statistical measures of time-resolved boundary-layer data are commonly used for analysis of the transition process. Amplified Tollmien-Schlichting waves and turbulent breakdown lead to a higher standard deviation of the velocity signal with a peak during maximum intermittency of the flow. The intermittency region contains randomly occurring turbulent spots such that the flow undergoes a stochastic state change between laminar and
4.1 Boundary-Layer Stabilization

Figure 4.4: Transition behavior of boundary-layer flow characterized by the hot-wire standard deviation at $y/\delta_1 = 1$. Turbulent. Further downstream, the standard deviation decreases as the flow leaves the intermittency region and approaches the fully turbulent state.

The solid lines in Fig. 4.4 illustrate the maxima of the hot-wire signal standard deviation within the boundary layer at $y/\delta_1 = 1$ along the streamwise direction, measured on three consecutive days without DBD actuation. Starting from the laminar state with small velocity perturbations, the flow exhibits increasingly random fluctuations leading to an increase of the standard deviation of the sensor signal. The standard deviation culminates within the intermittency region where the flow switches between laminar and turbulent state. The circled lines indicate the results under a DBD thrust of 11$mN/m$. Despite certain deviations due to the experimental sensitivity to changed ambient conditions, the peak of the signal standard deviation is moved to farther downstream locations in all three experiments and an average transition delay of $\Delta x_{tr} \approx 50$mm is observed. Considering the location of unaffected transition at $x = 500$mm, this corresponds to an increase of the transition Reynolds number of 10%.

In addition to the development of the standard deviation along the streamwise direction, the average velocity profiles and the disturbance profiles along the wall-normal coordinate indicate the boundary-layer state. Fig. 4.5 illustrates the mean velocity profiles (solid lines) as well as the disturbance profiles (dashed lines) at four different streamwise locations,
normalized with the free-stream velocity $U_\infty$. The ordinate shows the wall-normal position normalized with the displacement thickness $\delta_1$. Two different cases, without (asterisks) and with (circles) DBD operation at $F = 11\text{mN/m}$, are presented. Additionally, the solution of a laminar boundary-layer computation as discussed in Section 4.1.3.1 is illustrated as a dotted line at the first streamwise position (a) to indicate the accurate agreement between measurements and computations. For the disturbance profiles the hot-wire signal is bandpass filtered within the TS instability range ($100 - 500\text{Hz}$, compare Section 4.1.3.1) and the standard deviation is calculated at each wall-normal location. For better visualization, the values are multiplied by a factor of 100. At the first measurement location (a), only small velocity fluctuations are measurable inside the laminar boundary layer. The small differences for the cases with and without DBD operation are hardly distinguishable from measurement noise and random velocity fluctuations. At this early stage, the boundary-layer disturbances are still too small to experience a dramatic influence by the boundary-layer stabilization. The averaged velocity profiles on the other hand no longer show significant differences since the stabilizing impact is limited to a small region downstream of the actuator. As the disturbances propagate downstream (b), they are amplified and eventually show typical characteristics of Tollmien-Schlichting waves. These characteristics are the global disturbance maximum within ($y/\delta_1 \approx 1$) and another local maximum on top of the boundary layer ($y/\delta_1 \approx 3$), as well as the minimum close to the boundary-layer edge ($y/\delta_1 \approx 2$). Although an impact on the mean velocity profiles can hardly be distinguished, the disturbance amplitude under the influence of the actuation is reduced. At farther downstream locations (c), the disturbance loses the typical TS-wave shape as the breakdown into three-dimensional structures is initiated. Here, the actuated velocity profile still shows laminar characteristics whereas a turbulent or highly intermittent shape is obtained without flow control. In the fully turbulent flow (d), the maximum standard deviation decreases and is situated directly at the wall. As soon as the controlled flow is fully turbulent, it does not exhibit any differences compared to the uncontrolled case any more.

Apart from statistical measures of the disturbance, integral boundary-layer quantities indicate the onset and completion of transition with similar accuracy [9]. The time-averaged velocity profiles experience a dramatic shape change during transition, as seen in Fig. 4.5. This can be characterized by the shape factor $H_{12} = \delta_1/\delta_2$ as the ratio between displacement thickness $\delta_1$ and momentum loss thickness $\delta_2$. In contrast to the statis-
4.1 Boundary-Layer Stabilization

Figure 4.5: Velocity profiles (solid lines) and standard deviation of filtered hot-wire signal (100-500Hz, dashed lines) for four different streamwise locations $x$, without (asterisks) and with (circles) DBD operation.
tical analysis, integral quantities from time-averaged data neither require high temporal resolution of the measurement technique nor sophisticated filtering of the signal subjected to ambient noise. A comparison of both approaches and associated uncertainties is given in Section 4.1.2.1.

Solid lines in Fig. 4.6 illustrate the distribution of the shape factor $H_{12}$ along the streamwise coordinate $x$ without DBD operation. Lines with circles as markers indicate experimental results obtained under operation of the single DBD actuator. The scatter of the results for three different days is higher than for the standard deviation measurements presented in Fig. 4.4. In the initially laminar phase between $x = 400 - 470\,\text{mm}$, the shape factor $H_{12}$ being larger than 2.6 agrees with the theory for flow subjected to adverse pressure gradients. Downstream of the actuator location it drops continuously to a value of 1.4 characterizing a fully turbulent velocity profile. For the case with DBD actuation, the drop starts farther downstream as compared to the case without DBD forcing. Since the shape factor experiences a monotonous decrease throughout the transition region and no clearly specified value relates to the onset or end of transition, a threshold has to be defined to compare the cases with and without DBD actuation. To quantify the distance $\Delta x_{tr}$ by which the transition is delayed, a value of $H_{12} = 2$ is used in accordance with earlier investigations by Grundmann and Tropea [73]. This yields an average transition delay of $\Delta x_{tr} = 30\,\text{mm}$ with significant scatter of the results of different days. The uncertainty of the presented method is discussed below.

4.1.2.1 Uncertainty Discussion

In both Fig. 4.4 and Fig. 4.6 the transition delay obtained by application of local forcing with $11\,\text{mN/m}$ is apparent. Nevertheless, dependent on the observed quantity, it varies between $\Delta x_{tr} = 30 - 50\,\text{mm}$. Accurate determination of the shape factor $H_{12}$ requires measurements with high spatial resolution close to the wall due to the emphasis of the integral evaluation of $\delta_1$ for $y \to 0$. Since the resolution of hot-wire data is limited by the probe prong diameter and heat conduction into the wall, only data extrapolation provides a value corresponding to the no-slip condition. At positions where the probe has physical contact with the surface, velocities are overestimated and constant. This effect is used to truncate the data and remove all data contaminated by heat conduction. A second-order polynomial fit is applied for the subsequent $10 - 20$ data points to extrapolate down to the wall. Due to the limited measurement resolution in wall-normal direction, the wall position can be determined with $0.1\,\text{mm}$ accuracy. Dependent on
the displacement thickness, this yields an uncertainty of $H_{12}$ in the order of 0.1.

Within the intermittency zone, the boundary-layer shape is stochastically changing. In order to obtain a statistically satisfactory representation of the average state, a high number of independent samples is necessary. This requirement in combination with the large amount of measurement positions in wall-normal and streamwise direction contradicts the necessity to conclude the measurement within a reasonable span of time, avoiding actuator deterioration and damage. Ten thousand samples are acquired at 10kHz sampling rate as a compromise between statistical uncertainty and experiment duration. Small measurement errors lead to relevant changes of the integral values $\delta_1$ and $\delta_2$. The uncertainty of the shape factor evaluation complicates the quantification of the transition delay $\Delta x_{tr}$.

In total, the uncertainty of $H_{12}$ evaluation renders the standard deviation a more robust quantity for transition detection. Nevertheless, a sufficient number of stochastically independent data samples may not be available at all locations, e.g. close to the DBD actuator between $x = 300$mm and $x = 400$mm. In this crucial region for actuator evaluation, optical measurement techniques with poor macroscopic temporal resolution (i.e., low repetition rate) can provide averaged velocity fields for further investigation as described in Section 4.1.3.

Figure 4.6: Transition behavior of boundary-layer flow characterized by shape factor H12.
4 Transition Control

Figure 4.7: Spectral analysis of the hot-wire signal for the positions (a) 400mm, (b) 440mm, (c) 480mm, (d) 640mm.

4.1.2.2 Spectral Content

The spectral content of the velocity signal is plotted in Fig. 4.7 for different streamwise locations. The dotted curve illustrates the frequency content in the free-stream outside the boundary layer where only the broadband, low-amplitude free-stream turbulence is covered. The solid line presents the data for a position $y = 0.1$mm above the flat plate without DBD forcing and is contrasted with the case with DBD operation (dashed line). It should be noted that a low-pass analog filter with a cutoff frequency of $f_c = 3.15kHz$ is applied to remove electromagnetic noise produced by the DBD actuation. Therefore, a drop of the spectra beyond this frequency can be seen in the plots. In the laminar region of the boundary layer (a), a prominent frequency range around 250Hz dominates. The natu-
rally amplified frequencies inside the boundary layer range between 100 and 500Hz which corresponds well to stability analysis (Section 4.1.3.1). The disturbance amplitudes increase during the downstream development (b), indicating the growth of two-dimensional Tollmien-Schlichting waves. It is for this position that the positive influence of the DBD actuator becomes apparent. The velocity fluctuations without DBD actuation increase and start to spread to higher frequencies, whereas with the DBD actuator turned on, the state remains similar to that shown in (a). As the boundary layer transitions (c) without the flow control, a broadband increase of the fluctuations occurs until finally reaching the chaotic, turbulent state characterized by the monotonic spectrum at (d) \( x = 640 \text{mm} \). For the actuated case, this transitional behavior is postponed. The overall level of the frequency content of the laminar free-stream is one order of magnitude lower than within the turbulent boundary layer.

4.1.2.3 Actuator Thrust Variation

The actuator voltage and subsequently the thrust magnitude is varied to evaluate the influence on the transition delay. The development of the hot-wire standard deviation is again used to quantify the transition delay presented in Fig. 4.8. At low actuator thrust, the obtained transition delay is hardly measurable. Due to the nature of dielectric-barrier discharges, a threshold voltage around \( U_A = 6 \text{kV} \) exists below which no plasma is observed for the actuator configuration used. Just slightly above this threshold, the influence on transition is rather insignificant. For higher applied body forces, the transition delay effect is enhanced, but care has to be taken not to exceed the breakdown voltage which destroys the actuator. This voltage is defined by the material and thickness of the dielectric. Excessively high voltages and associated fluid accelerations have the additional disadvantage that an overshoot in the velocity profile can occur, which leads to a destabilization of the flow, rendering the actuation useless for transition delay. Grundmann [70] indicated the existence of an optimum body force for transition delay. With the present setting and due to breakdown-voltage limitations within this study, no optimum can be identified, but it remains promising to increase the DBD thrust if adequate dielectric material is available, especially at higher free-stream velocities. Array configurations with consecutive DBD in streamwise direction are another way to improve the overall momentum transfer.
4 Transition Control

Figure 4.8: Obtained transition delay $\Delta x_{tr}$ dependent on integral actuator thrust $F$.

4.1.3 Influence on Hydrodynamic Stability

Analysis of the localized influence of DBD actuation on the boundary-layer profiles next to the exposed electrode allows a quantification of the impact on the flow stability. Unfortunately, this area cannot be assessed by hot-wire anemometry. Electrically conducting materials may not be inserted in the zone of ionization since uncontrolled discharges would destroy the probe. Nevertheless, optical measurement techniques can be employed to quantify the interaction of the plasma with the flow close to the high voltage electrodes (e.g. [79, 58, 131, 41, 40]). Although the velocity profile is only slightly changed, the shape factor is altered significantly at the actuator location. Knowing that the shape factor is closely related to the stability properties of the flow, e.g. the critical Reynolds number as shown by Wazzan et al. [164], a local change of this value indicates an impact on hydrodynamic stability and subsequently the expected transition location. Fig. 4.9 illustrates the shape factor $H_{12}$ calculated for velocity profiles obtained by particle image velocimetry (PIV) upstream (circles) and downstream (squares) of the actuator. In order to resolve the actuator effect and guarantee a sufficient resolution of the boundary layer in wall-normal direction, the measurement window of 32mm width is traversed to four downstream locations, yielding a total field of view of 120mm. To reduce the influence of measurement uncertainties and surface reflections on the calculated shape factor, ensemble averaging and spatial averaging in streamwise direction are necessary. 2000 single velocity fields from PIV are ensemble averaged to reduce the impact of mean-flow velocity fluctuations.
and random errors. In streamwise direction, twenty velocity profiles are averaged in sections of 6mm to reduce the error by surface reflections and enhance the accuracy close to the wall. From these averaged profiles, the local shape factor can be calculated with sufficient accuracy to derive a trend of the development upstream and downstream of the actuator. The linearized trends are illustrated by solid lines which are fitted by a least square procedure. Upstream, the shape factor slope due to the adverse pressure gradient is slightly exaggerated due to the measurement uncertainties. Despite these uncertainties, a clear drop of the shape factor by approximately $\Delta H_{12} = 0.35$ is noted at the actuator location. The shape factor subsequently increases and approaches the value upstream of the actuation since the actuator effect is spatially confined. This local decrease may be interpreted similar to a local flow acceleration, i.e. a negative pressure gradient, which is stabilizing the flow. Albrecht et al. [3] showed that for electromagnetically forced flow a monotonic relation between the shape factor and the critical Reynolds number cannot be assumed. Locally, the flow can be even more stable than an asymptotic exponential profile obtainable from continuous boundary-layer suction. Therefore, a linear stability analysis of the velocity profiles has to be conducted.
4 Transition Control

![Figure 4.10: N-factor contours and neutral stability curve of flow without DBD actuation.](image)

**4.1.3.1 Linear Stability Analysis**

The stability of the laminar flow along the flat plate is investigated by a linear stability analysis in the spatial framework, based on the procedure presented by Schmid and Henningson [144]. The stability analysis cannot be conducted directly with experimentally determined velocity profiles due to resolution limitations and non-monotonic derivatives caused by measurement uncertainties. Nevertheless, the necessary boundary-layer profiles of the streamwise velocity component are obtained from a numerical solution of the laminar boundary-layer equations ([42], adapted from [28]) based on the measured pressure gradient (compare Section 4.1.1). The solver numerically approximates the partial differential boundary-layer equations by finite differences based on a Falkner-Skan transformation of the numerical grid. The representation in form of differentiation matrices leads to a linear system of equations which is solved with a Thomas algorithm in Matlab.

The numerical solution to the well-known Orr-Sommerfeld equation for two-dimensional disturbances yields the predicted disturbance growth rates for given disturbance frequencies [39]. Integration of the growth rates along the streamwise direction starting from the point of neutral stability allows the formulation of the N-factor [159]. Despite being based on linear theory and therefore neglecting important characteristics of the transition to turbulence, the empirical $e^N$-method can be used as an indicator for the transition occurrence.
4.1 Boundary-Layer Stabilization

It is generally agreed that transition does not "occur" suddenly, nevertheless we will henceforth assume that the critical N-factor $N_T$ defines the position of completed transition, resulting in fully turbulent flow. The value of $N_T$ is dependent on the initial disturbance conditions, mainly governed by the turbulence intensity $Tu$ of the free-stream flow. Mack [102] proposed a functional relationship $N_T = -8.34 - 2.4ln(Tu)$ valid for $1e^{-3} < Tu < 1e^{-2}$. For the given flow with a free-stream turbulence intensity of $Tu = 0.24\%$, this yields a critical $N_T = 6.14$.

Fig. 4.10 illustrates the N-factor development of the flow without DBD actuator. The N-factor is calculated by integration of the growth rates obtained from stability computations, and iso-contours are plotted as a function of the downstream position $x$ and the dimensional frequency $f$. Although the stability computations are conducted in normalized coordinates employing the Blasius reference length scale $\delta$ and the normalized frequency $F$, the results shown in Fig. 4.10 are presented in physical dimensions to facilitate comparison to the experiments. The neutral stability curve is plotted as a thick solid line to separate the stable and unstable flow regime. An additional dotted isoline is included which represents the level of $N = N_T = 6.14$ calculated before. This value is first reached for a frequency of $f = 380\text{Hz}$ in the vicinity of $x = 0.48\text{m}$. This result agrees reasonably well with the experimentally determined location of maximum standard deviation (see Fig. 4.4) around $x = 0.5\text{m}$ without flow control.

In order to analyze the flow under influence of a single DBD actuator, the spatial force distribution experimentally acquired by Kriegseis et al. [97] is implemented in the boundary-layer solver. The matrix representation of the laminar boundary-layer equations enables easy implementation of the additional source term. The force field is normalized with the corresponding boundary-layer scales and interpolated on the numerical grid of the boundary-layer solver as described in detail in [42]. The integral value of the force field is equal to the actuator thrust used in the experiments, i.e. $F = 11\text{mN/m}$. The solution of the linearized boundary-layer equations is illustrated as a dashed line in Fig. 4.11 and it indicates a sharp drop of the shape factor of $\Delta H_{12} = 0.3$ at the actuator location. The value of the shape factor drop is comparable to the PIV result of $\Delta H_{12} = 0.3$. The gradual evolution between $x = 0.3\text{m}$ and $x = 0.42\text{m}$ from Fig. 4.9 is included in the plot. In contrast to the experimental data, the shape factor asymptotically approaches the solution of the baseflow. A strong gradient with subsequent asymptotical behavior appears physically reasonable due to the local confinement of the actuator force. Also included in the plot are...
4 Transition Control

Figure 4.11: Shape factor evolution measured by hot-wire and PIV compared to the numerical boundary-layer solution.

the experimental results from hot-wire measurements downstream of the single operated DBD actuator with and without operation. All results are in reasonably good agreement between $x = 0.3\,\text{m}$ and $x = 0.5\,\text{m}$ considering the experimental difficulties of determining the shape factor. Since the boundary-layer solver does not predict transition and therefore delivers a fully laminar solution, the development of the shape factor in the intermittency region downstream of $x = 0.5\,\text{m}$ deviates from the measured values obtained with hot-wire anemometry.

Since the body force stabilizes the flow within the normally unstable region, the neutral stability curve cannot be represented by a single line and contour plots reveal an unusual shape. Therefore, an analysis of the N-factor evolution for discrete disturbance frequencies is more convenient. The disturbance evolutions are provided in Fig. 4.12 (a) for the case without actuation. In accordance with the conclusions from Fig. 4.10, the most amplified disturbance for the transition threshold $N_T = 6.14$ is approximately $f = 380\,\text{Hz}$ in the vicinity of $x = 0.48\,\text{m}$. The effect of the local stabilization due to DBD operation is illustrated in Fig. 4.12 (b). The cumulative growth factor downstream of the actuator location $x = 0.35\,\text{m}$ is significantly reduced for all frequencies. This is in qualitative agreement with computations by Séraudie et al. [145] observing a N-factor decrease on a 2D airfoil geometry. The disturbance amplitudes start to grow again a few millimeters downstream, but the location of crossing the transition
4.1 Boundary-Layer Stabilization

Figure 4.12: N-factor evolution for discrete disturbance frequencies (various markers) with transition threshold $N_T = 6.14$ (dashed line) without (a) and with (b) DBD actuation. The threshold is moved to $x = 0.54m$. This shift of the threshold location of approximately 60mm downstream overpredicts the experimentally measured effect on transition. Additionally, the stability analysis reveals that the frequency relevant for transition is shifted towards lower values, here to approximately $f = 320\text{Hz}$.

4.1.4 Resume Section 4.1

It could be shown that DBD actuation can delay transition initiated by naturally developing Tollmien-Schlichting waves under adverse pressure gradients. The stabilizing effect does not only lead to an attenuation of single frequency disturbances as shown before in [71], but also delays transition initiated by naturally occurring broadband multi-frequency instabilities. A downstream shift of the transition location in the range of $10 - 95\text{mm}$ is obtained by varying the imparted body force between $7.8$ and $17.7\text{mN/m}$. Statistical and integral, experimentally determined quantities are compared concerning the quantifiability of the distance by which transition is delayed. The locally confined stabilizing effect of the DBD actuator under adverse pressure gradient is analyzed by optical measurements of the velocity-profile modification close to the DBD actuator. A sudden drop of the shape factor is caused due to an actuator thrust of $F = 11\text{mN/m}$. Optical measurements show a decrease of approximately $\Delta H_{12} = 0.35$ whereas a computational study indicates $\Delta H_{12} = 0.35$. The numerical solution of the lin-


4 Transition Control

ear boundary-layer equations with implemented actuator force distribution shows good agreement with the measured boundary-layer properties and linear stability analysis is conducted with this data. The results suggest that the local growth rates are decreased for a broad range of frequencies, implying that the boundary layer is locally stabilized against linear disturbances of such frequencies.

The results support manifold experimental observations of transition delay with DBD actuators. Under adequate conditions of operation, the imparted momentum locally alters the boundary-layer profiles and increases the hydrodynamic stability of the boundary-layer flow. If the actuator is positioned close to the point of neutral stability and well within the linear disturbance regime, instabilities of all frequencies are damped before the flow returns to a laminar base state. At sufficiently high operation frequencies of the dielectric barrier discharge, a negative influence on the stability due to unsteady force production can be neglected since high frequency content is naturally damped by viscous effects. With the presented configuration of a single DBD actuator, the transition Reynolds number can be increased by approximately 10%. Since a material dependent breakdown voltage limits the maximum thrust for boundary-layer control and since excessive forcing increases the chances of other instability mechanisms dominating the transition process, it appears most promising to apply consecutive actuator arrays with lower power levels in the linear amplification range of Tollmien-Schlichting instabilities.

The measured transition location correlates well with an empirical $e^N$-prediction if the turbulence intensity of the free-stream is considered. To enhance the credibility of stability theory based transition prediction and to finally enable a customized design of DBD based transition control, more experimental results at varying ambient conditions are necessary. Variable pressure gradients and flow velocities as well as lower turbulence intensities are accessible in free-flight experiments, as discussed in Section 5

The investigations and results discussed in this section have been published in:

The aim of the investigations presented in this section is to raise the technological level of the active wave cancellation using plasma actuators in order to demonstrate the applicability of this flow control method for future airborne applications. This involves the application of more sophisticated closed-loop control algorithms, the development of new high-voltage hardware for a better adjustment of the unsteady force production, as well as the integration of flow sensors and actuators into an aerodynamic airfoil instead of a flat plate. The goal is to perform active wave cancellation in free-flight using the Grob G 109b aircraft. This flight platform uses a wing glove, i.e. a sleeve which is slid over the normal wing, allowing instrumentation to be mounted between the wing and the surface of the sleeve. This way the wing of the aircraft is in not modified structurally. The glove can also be mounted on a wing segment in the large 2.2m x 2.9m wind tunnel of TU Darmstadt to conduct preparatory studies for the planned in-flight experiments. The first wind tunnel experiments on this newly developed setup are described in the present section. The Tollmien-Schlichting (TS) waves are excited artificially in order to provide controlled conditions for the development of the entire system.

Figure 4.13: Position of disturbance source (DS), plasma actuator (PA) and seven boundary layer measurement locations on the pressure side of the airfoil.
4 Transition Control

Figure 4.14: Wing glove setup mounted vertically in the wind tunnel. View from the front on the pressure side.

4.2.1 Experimental Procedure

The airfoil of the wing glove is based on the laminar DU-84-158 profile. The pressure side of the profile has been modified in such a way as to provide a low pressure gradient. Under wind tunnel conditions, this pressure gradient can be adjusted between slightly positive and negative values by changing the angle of attack.

A disturbance source (DS) has been mounted on the pressure side of the wing glove as shown in Figure 4.13, where the positions of the plasma actuator (PA) and 7 downstream measurement locations can be seen as well. This disturbance source consists of 20 point sources, each driven by a miniature loudspeaker (VISATON, K-16 50 Ohm). The point sources are all driven in phase by a sinusoidal operating signal and are placed side-by-side in spanwise direction with a spacing of $z = 20\, mm$. A disturbance source of this type was originally developed at the Technische Universität Berlin and has been used successfully for the excitation of 2-dimensional TS waves, as for example described in [128]. The TS waves developing behind each of the point sources eventually merge into a single quasi two-dimensional wave front downstream of the line of excitation.
4.2 Active Wave Cancellation: Classic Approach

The glove itself is made of glass-/carbon-fiber composite material, as typically used in glider design. To insulate the plasma actuator electrically from the glove, Plexiglas inserts with a thickness of 1.5mm have been integrated on both sides of the airfoil. CNC-machined spanwise grooves allow for the flush-mounted application of the control actuators. The inserts are exchangeable in case the position of actuators or flow sensors has to be modified. Figure 4.14 shows the setup mounted in the wind tunnel. The wing section holding the glove is mounted vertically on the external force balance of the wind tunnel. The angle of attack can be adjusted and was set to $\alpha = 1^\circ$, the free-stream air speed in the wind tunnel was 10m/s for this set of experiments. The boundary layer has been measured using conventional hot-wire techniques. The constant temperature anemometry (CTA) system consists of a 3-channel DANTEC Streamline (90N10, 90C10) and a KEMO BM8 dual-channel filter/amplifier, which has been used to remove unwanted electromagnetic noise from the error sensor signal of the control system. A traverse system, which is decoupled from the wing glove, has been used for boundary-layer investigations. This system is mounted at the end of the wind tunnel test section and allows for 3-axis movement of flow sensors (Figure 4.14, background).

The compact high-voltage generator Minipuls 2.1 which has been newly developed by GBS Elektronik GmbH now allows for an analogue modulation of the high-frequency plasma operation voltage instead of the digital modulation used before. The signal generator-board features separate inputs for the plasma operating frequency and the amplitude modulation, which is controlled by a voltage signal between 0V and 5V. The waveform can be prescribed arbitrarily by the control system. This analogue modulation feature yields much higher attenuation rates, since a precise adjustment of the force amplitude produced by the plasma actuator is now possible.

Additionally, 32 pressure tabs each have been integrated on both sides of the wing glove, in order to monitor the pressure distribution around the airfoil. For this purpose stainless steel capillary tubing has been built into the composite structure during the construction phase, and tabbed into after the completion to the glove. The spacing of the pressure tabs is variable and was chosen according to the expected pressure gradient with a more dense placement in areas of high gradient, like close to the leading edge. These pressure signals are recorded using a 64 channel miniature piezo-resistive differential pressure scanner (ESP-64HD) from Measurement Specialties (formerly Pressure Systems Inc.) in combination with a Chell CanDAQ data acquisition board.
4 Transition Control

![Figure 4.15: Pressure coefficient $c_p$ for suction side (dotted line) and pressure side of airfoil (solid line) measured at a free-stream velocity of $U_\infty = 10 \text{m/s}$ and an angle of attack of $\alpha = 1^\circ$.](image)

4.2.1.1 Base Flow Analysis

In Figure 4.15 the pressure coefficient $c_p$ is plotted vs. the non-dimensional chord length for the given flow situation ($U_\infty = 10 \text{m/s}$ and $\alpha = 1^\circ$). It can be seen that on the pressure side of the airfoil, where the experiments have been conducted, a small but nevertheless positive pressure gradient is present. This condition promotes transition due to the slightly destabilizing pressure gradient and thereby guarantees a TS development over a long enough streamwise distance in order to conduct transition delay experiments.

In a preparatory step proper functionality of the artificial disturbance source had to be proven and the amplitude of the introduced TS waves had to be adjusted to a value high enough to promote transition, but well inside the linear range at the location of the control actuator. In order to extract the small TS wave amplitude from the flow data, a phase-locked CTA technique has been employed. The hot-wire measurements are analyzed by phase-averaging the data in relation to the sinusoidal operating signal of the disturbance source. This way the signal of the TS waves is separated from
4.2 Active Wave Cancellation: Classic Approach

Figure 4.16: TS amplitude plotted over the wall-normal distance $y$ at the location of the control actuator measured using a phase-locked CTA technique.

random fluctuations present in the boundary layer. Figure 4.16 illustrates the results of this investigation.

The maximum TS amplitude at the location of the control actuator was adjusted to a value of $A_{TS}/U_\infty = 0.5\%$ at an external velocity of $U_\infty = 10 \text{m/s}$. This amplitude is assumed to be located well inside the range of linear TS amplification. Secondly, the typical eigenfunction of a TS wave is visible with the maximum amplitude inside the boundary layer close to the wall, a minimum close to the boundary layer’s edge and a second smaller maximum outside of the boundary layer. The phase jump of $180^\circ$ is visible as well. This confirms that the artificial disturbance source excites a clean TS wave train, with an amplitude still in the linear regime at the position of the control actuator.

4.2.1.2 Control Strategies

In collaboration with the Technische Universität Berlin two different control algorithms were tested. Both systems have been utilized successfully for flow control purposes before, albeit with different actuators [66]. First an extremum-seeking controller [16] is used which automatically optimizes the phase shift between the incoming wave train and the wave created by the plasma actuator, second an adaptive control algorithm [15], consisting
of a FIR-model which is adapted online via the filtered-xLMS algorithm, is implemented. Both approaches use an error sensor downstream of the plasma actuator to automatically optimize a control function. Whereas the extremum-seeking controller is only used for damping single frequency TS waves, the adaptive filtered-xLMS controller is also capable of controlling naturally occurring TS waves with mixed frequency content.

With both control algorithms a substantial transition delay could be achieved. Presented here are the results obtained using the adaptive filtered-xLMS algorithm.

Figure 4.17 shows the attenuation rate measured at the location of the error sensor (x=50mm downstream of the plasma actuator, y = 0.2mm) using the adaptive filtered-xLMS control algorithm.
4.2 Active Wave Cancellation: Classic Approach

Figure 4.18: Evolution of $u'_{\text{rms}}$ along the chord at a constant wall-normal distance of $y = 0.9\text{mm}$ downstream of the plasma actuator.

Berlin.

Looking at the downstream development of the transition process this significant wave attenuation leads to a substantial transition delay. Figure 4.18 shows the rms-value of the u-velocity fluctuation recorded at a constant height ($y = 0.9\text{mm}$) above the wall along the chord. For the base flow case without artificial excitation ($\circ$), the flow remains laminar until a location of $x = \sim 550\text{mm}$ downstream of the control actuator. The aerodynamic profile of the wing glove has an S-shape in the region close to the trailing edge leading to flow separation. The flow separation triggers transition at this downstream position in any case. With excitation applied ($\Box$), the transition region moves far upstream and the typical sharp rise of the u-velocity fluctuation ($u'_{\text{rms}}$) becomes visible. Finally, in the controlled case ($\times$), this artificially promoted transition can be substantially delayed by about $75\text{mm}$ (5.6% chord).

A spectral analysis of the hot-wire signal (Figure 4.19) reveals the frequency content of the boundary layer at the measurement positions 1, 3 and 6 and confirms the previously described findings. Close to the control actuator (Figure 4.19, left) the reduced rms-levels are primarily due to an amplitude reduction of the dominant TS frequency right downstream of the actuation. This very narrow band attenuation reverts to a more broadband amplitude reduction farther downstream (Figure 4.19, center), before the boundary layer becomes turbulent in both cases with excitation (Figure 4.19, right).
Figure 4.19: Spectral analysis of the flow velocity fluctuations at three characteristic measurement positions (1, 3 (top) and 6 (bottom) from left to right).
4.2 Active Wave Cancellation: Classic Approach

4.2.2 Resume Section 4.2

In order to achieve the goal to successfully demonstrate active wave control using DBD plasma actuators in free-flight, several development steps could be accomplished, as presented in this section. The AWC setup has been migrated from a flat plate to a full-scale wing glove, capable to be tested on a Grob G 109b motorized glider. The development of new light-weight, high-voltage power supplies, allows for a precise modulation of the actuators momentum production. Compared to the rectangular carrier-frequency modulation used in earlier investigations this enables high TS wave attenuation rates. In collaboration with the Technical University of Berlin two control algorithms have been implemented and tested. Promising transition delay rates of about 5.6, % chord could be demonstrated on the wing glove in the wind tunnel.

The investigations and results discussed in this section have been presented in:

4 Transition Control

4.3 Hybrid Transition Control

In order to conduct active boundary-layer control in Tollmien-Schlichting (TS) wave induced transition, traditionally two approaches are investigated. Boundary-layer stabilization modifies the stability features of the flow in order to delay the growth of boundary layer disturbances indirectly. On the other hand active wave cancelation (AWC), aims at the direct damping of disturbances by linear negative superposition. Both methods were discussed above. A combination of the two transition control mechanisms in a single DBD plasma actuator is investigated in the following study.

4.3.1 Experimental Procedure

The experiments have been conducted using the same setup as introduced in Section 4.2.1 and shown in Figure 4.14.

The airfoil exhibits a low, almost linear pressure gradient over a long chordwise distance on its pressure side. Under wind tunnel conditions this pressure gradient can be adjusted for a given Reynolds number by variation of the angle of attack \( \alpha \). For the investigated flow situation (\( Re = 1.41 \cdot 10^6 \), \( \alpha = 1^\circ \)) a slightly destabilizing pressure gradient is established, which promotes the growth of boundary-layer disturbances. For providing controlled experimental conditions, TS waves with a frequency of \( f = 120 \text{Hz} \) have been introduced artificially into the boundary layer upstream of the plasma actuator. The disturbance eigenfunction at the location of the error sensor (\( x/c = 0.391 \)), which is used for the adaptive control, has been reconstructed utilizing a phase-averaged hotwire technique (Figure 4.20). The amplitude of the TS waves, normalized with the freestream velocity at this location, is \( A_{TS}/U_\infty = 1.31\% \).

4.3.2 Working Principle of the Hybrid Operating Mode

In order to introduce the hybrid transition control mode for DBD plasma actuators the different working principles are compared in this section.

On the one hand the plasma actuators can be operated continuously at a constant high alternating voltage. Even though the force production is highly unsteady during each discharge cycle [23], a quasi-steady momentum addition to the boundary layer can be assumed due to the mass inertia of the fluid (Figure 4.21 (2)). However, the operating frequency must be located well outside of the boundary layer’s unstable frequency range to prevent an introduction of additional disturbances. The added momentum
modifies the mean flow, such that the resulting boundary-layer profile is less likely to develop an inflection point; hence hindering the amplification of Tollmien-Schlichting waves. This method has been applied successfully for the purpose of boundary-layer stabilization in several experimental studies ([73], [145], [40]).

On the other hand an amplitude modulation can be applied to create a pulsed momentum production at TS wave frequency (Figure 4.21 (3)). This method is based on the introduction of a non-continuous momentum into the boundary layer which can, by careful adjustment of phase, amplitude and frequency in relation to the incoming TS wave, lower the velocity disturbance amplitude by negative superposition. This so called active wave cancelation (AWC) is not based on the modification of the mean flow and the magnitude of the applied force is typically significantly lower than in the case of continuous actuation [73].

The hybrid transition control mode combines these two operating principles (Figure 4.21 (4)). A small amplitude modulation is applied to the operating voltage, while the actuator keeps running continuously. This way the stabilizing effect of the high continuous momentum input on the boundary layer is combined with an effective amplitude reduction due to AWC. Higher damping rates have been observed, increasing the achievable overall transition delay significantly.

Figure 4.20: Reconstructed eigenfunction of the disturbance at the position of the error sensor ($x/c = 0.367$) using phase-averaged hotwire data.
Figure 4.21: Operating modes for the plasma actuator (in relation to a TS wave signal (1)): Continuous mode (2), active wave cancellation (3), hybrid mode (4). Shown are (qualitatively) the operating voltage (black) and the momentum production over time (red).
4.3 Hybrid Transition Control

4.3.3 Control Approach

The active cancelation of TS waves depends on the generation of a counterwave of the same frequency $\Omega$ with the correct phase $\phi$ and amplitude $a$. Even though it is possible to set these parameters manually for a continuous wave train, a self-tuning setup is necessary for more realistic conditions of naturally occurring rather than artificially excited TS waves. One possibility is the use of extremum seeking control, a method which uses a perturbation of the optimization parameter to automatically optimize a cost functional dependent on this parameter. However, this control approach is only applicable for continuous wave trains as presented in this study.

Since the phase relation between the disturbance and the counterwave is the most critical parameter, extremum seeking control is applied to this variable alone. We use an input $v = a \sin(\omega t + \phi)$, where $u = \phi$ is the optimized parameter. Note that $v$ only represents the modulated input, to which the constant offset still has to be added. As a cost functional, we use the remaining disturbance at the error sensor, as characterized by the root-mean-square (rms) value of the sensor signal.

The goal is to automatically find the value of $u$ which minimizes $y$. For this, it is assumed that the slow behavior of the system can be approximated by an equilibrium input-output map $y = F(u)$, such that $\lim_{t \to \infty} y(t) = y$ for $u(t) = u = \text{const}$. This way, the problem is reduced to finding the minimum of a static map. This is achieved by adding a sinusoidal perturbation to $u$ which varies slower than the system’s dynamics. This perturbation can be used to estimate the gradient $\partial y / \partial u$. With this estimation, a gradient descent approach with fixed step size parameter $K$ is used to find the minimum.

For the estimation of the gradient, a modified version of the approach proposed by Henning [74] is used. An extended Kalman Filter (EKF) based on the model of a static process is used for the estimation. However, while Gelbert [60] uses two time-shifted pairs $(u, y)$ as inputs, here a third pair is added in order to guarantee observability of the system at all times.

4.3.4 Results

Figure 4.22 summarizes the most important results of the transition delay experiments at $Re = 1.41 \cdot 10^6$ and $\alpha = 1^\circ$. In the transition region, the typical rise of the fluctuation amplitude is visible in the intermittency zone before $u_{\text{rms}}'$ settles to the level of turbulent flow. The streamwise locations where an amplitude of $1 m/s$ is exceeded has been chosen to define the onset
of transition for the various cases. For the baseflow case \((\times)\) the boundary layer stays laminar in the complete measuring range. With the artificial disturbance source operating, the transition location moves to \(x = 200\,mm\) downstream of the plasma actuator \((\Box)\). For pure active wave cancelation with individually optimized parameters the transition delay was \(\Delta x = 64\,mm\) \((\ast)\), while operating the plasma actuator in a continuous fashion at \(8kV_{pp}\), the transition delay amounts to \(\Delta x = 205\,mm\) \((\diamond)\). Combining the two methods as described before with newly optimized parameters for the AWC, the transition region is moved downstream significantly farther by \(\Delta x = 327\,mm\) \((\lozenge)\) and transition is still not completed at the end of the measurement region.

![Figure 4.22](image)

Figure 4.22: Downstream development of \(u'_{rms}\) at a wall-normal position of \(y = 1.5\,mm\) for baseflow, AWC, continuous mode and hybrid mode at \(8kV_{pp}\).

Analyzing the power density spectra of the error sensor signal (Figure 4.23) of a typical set of experiments reveals the achieved attenuation rates at the location of the error sensor. Operation of the disturbance source generates a clear peak in the spectrum with an amplitude of \(L_{dB} = -26.6\,dB/Hz\) at the TS frequency of \(120\,Hz\) \((\Box)\). In hybrid mode of operation a maximum reduction of \(\Delta L_{dB} = 33.1\,dB/Hz\) is visible \((\diamond)\), which corresponds to an amplitude reduction of 97.8\%, whereas active wave cancelation and continuous operation of the plasma actuator generate an
intermediate amplitude reduction of 88.5% (⋆) and 64.5% (○), respectively. Note that continuous actuation results in a more broadband attenuation as compared to pure active wave cancelation. This explains the fact, that even though the attenuation rate at TS frequency in continuous mode is lower, a larger transition delay (Figure 4.22) could be achieved.

Continuous operation of this specific plasma actuator at a peak-to-peak voltage of $U = 8kV_{pp}$ and a frequency of $f = 9.75kHz$ consumes a power of $P_A = 41.5W/m$. The additional (sinusoidal) modulation around this value for the combination with the active wave cancelation causes no measurable change of the average power consumption. The modulation needed for AWC merely appears as a slightly higher standard deviation in the time trace of the high voltage signal.

### 4.3.5 Resumee Section 4.3

The combination of boundary layer stabilization and active wave cancelation in one single DBD plasma actuator offers great benefits for the application to transition control. The achievable transition delay rates in hybrid mode appear to be larger than the effect of the single isolated methods. The robustness of the stabilization approach is combined with the energy...
4 Transition Control

efficiency of the active wave cancellation. The new operation mode comes with a number of different parameters that should be optimized for highest energy efficiency. Most important is the magnitude of the continuous forcing, which appears to be a parameter that should be automatically and continuously adjusted, since stronger continuous forcing leads to better performance but also to higher energy requirements. The superior transition delay performance comes without further energy requirements when compared to the case of continuous actuation. Overall, the limit of the achievable transition delay is not reached with the results presented. The interplay of the underlying mechanisms will be focused on in greater detail in upcoming investigations.

The investigations and results discussed in this section have been published in:

4.4 Acive Wave Cancellation: Direct Frequency Mode

For conducting active wave cancellation using plasma actuators following the classic operation mode (Section 4.2) a sufficiently large difference between the TS wave frequency (modulation frequency) and the operating frequency of the plasma actuator is essential. However, with increasing flow speed, the unstable frequency band will shift to a higher range, until a sufficient difference between carrier frequency and TS wave frequency cannot be maintained anymore. Therefore it was already suggested by the author in [70] to make use of the plasma actuators unsteady force production during one cycle of the operating voltage and to directly operate the actuator at the TS wave frequency.

Over the past years numerous experimental ([57, 58]) and numerical ([23, 24, 155]) studies have investigated the mechanisms that are responsible for the plasma actuators force generation. Throughout the process, the time-resolved force distribution has been discussed with much controversy, leading to the well known "push-push" and "push-pull" theories. Despite this controversial point, it is commonly accepted, that the plasma actuator produces a local unsteady force, which is fluctuating with the same frequency as the applied high voltage. By analyzing the plasma actuators response electrically [130] it became clear that the discharge regime varies largely between the positive and the negative half cycles, which is the cause for this unsteady force production. Accompanying investigations using optical measurement techniques in the direct vicinity of the plasma region confirmed these results [46]. It is this asymmetric behavior that makes the plasma actuator a candidate to be operated in direct frequency mode. A careful adjustment of the phase relation between the TS wave and the actuator excitation signal can thereby potentially cancel the waves.

This section presents a proof-of-concept for this approach in combination with a control system for active wave cancellation with the goal to demonstrate transition delay on a wing model. It is a joint effort between the Technische Universität Darmstadt, ONERA Toulouse and the Technische Universität Berlin, the latter of which provided control algorithms necessary for this study.
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Figure 4.24: Two-dimensional wing model of the ONERA-D airfoil mounted inside the wind tunnel. It is equipped with a hollow dielectric insert (blue) holding two plasma actuators.

4.4.0.1 Experimental Procedure

4.4.0.2 Subsonic wind tunnel facility and ONERA-D wing model

The presented experiments have been conducted in the subsonic open-return wind tunnel located at the research facilities of ONERA Toulouse. It features a low turbulence level $0.5 \cdot 10^{-3} < T_u < 0.5 \cdot 10^{-2}$ depending on the free-stream velocity, which ranges from 5 to 75 m/s. This facility operates at ambient conditions and is well suited for transition experiments.

The ONERA-D airfoil, which has been utilized during this project, is a symmetric airfoil specifically designed for transition control investigations. As illustrated in Figure 4.24, a two-dimensional model of this airfoil is mounted horizontally within the test-section of the wind tunnel. It has a chord length of $c = 0.35m$. The angle of attack can be adjusted between $\alpha = -8^\circ$ and $\alpha = +3^\circ$ in order to adapt the upper side pressure gradient and thus the natural transition location. For this experiment the angle of attack is set to $\alpha = +2^\circ$ and natural transition is located close to $x/c = 60\%$ for the given flow situation ($U_\infty = 7m/s$). The setup also allows for an adjustment of the yaw angle ($\beta$), which is set to zero in this case. The existing model was modified to be equipped with plasma actuators. A 5mm thick insert made of dielectric material Lab850 was placed at...
the leading edge region matching the model shape. Additionally, the wing is equipped with 15 pressure taps on the upper side.

### 4.4.0.3 Electrical Setup

As shown in Figure 4.25 two DBD plasma actuators are placed at 10% and at 30% chord, respectively. The upstream actuator (DBD1) serves as disturbance source to artificially excite a single frequency TS wave train. The plasma actuator downstream (DBD2) is utilized as the transition control device. The DBD actuators used in this experiment consist of two thin 30cm long spanwise orientated rectangular electrodes made of copper tape. The electrodes are positioned asymmetrically on both sides of the dielectric insert. The two upper electrodes have been polished in order to reduce their thickness down to 0.05mm to prevent them from promoting transition.

Two TREK power amplifiers (model 30/20, $\pm 30kV$, $20mA_{peak}$) supply the power for the sinusoidally driven actuators. Two hot wire probes (Dan-tec, 55P15) have been employed to analyze the flow downstream of the control actuator. One is mounted on a traverse system and serves for measurements of boundary layer velocity profiles, whereas a stationary hot wire probe ($x/c = 40\%, y = 0.2mm$) is used as error signal for the control system. In-house LabVIEW codes at ONERA allow for automated hot wire calibration, as well as automatic traversing of the probe along the contour of the model. Filtering is applied online within the controller software in
order to remove the electromagnetic noise emitted by the plasma actuators from the hot wire signal.

4.4.0.4 Closed-Loop Control Setup
In collaboration with the Technische Universität Berlin, a robust extremum-seeking control algorithm \cite{16} has been implemented, which was provided for these experiments and has been successfully applied for flow control purposes previously. The system utilizes the signal of the stationary hot wire as error sensor to automatically optimize a control function. In this case the controlled variable is the phase shift between incoming TS wave and the flow structures created by the plasma actuator. This control algorithm is running on a dSPACE real-time processing unit. Due to its robustness this algorithm is well suited to control artificially exited, monofrequent TS waves. By slowly and periodically deflecting the system out of its current working point (perturbation), the gradient $f$ of the error signal according to a change of the controlled variable, which is the phase shift in this case, is determined. The phase relation between TS wave train and the flow structures created by the plasma actuator is then continuously adapted along this gradient, which drives the system into a minimum.

4.4.1 Linear Stability Analysis of the Base Flow
Control of Tollmien-Schlichting instabilities using plasma actuation has been undertaken on the upper side of the unswept ONERA-D airfoil for an angle of attack $\alpha = +2^\circ$. One half of the symmetric airfoil and the dimensionless velocity distribution $U/U_\infty = (1 - c_p)^{1/2}$ are plotted in Figure 4.26. The experimental data (square symbols) have been interpolated and smoothed in order to provide a more refined external velocity distribution necessary for the boundary layer computations. For an angle of attack of $\alpha = 2^\circ$, the suction peak is well pronounced and followed by a strong deceleration region promoting TS development and amplification.

This strong deceleration induces an significant raise of the displacement thickness $\delta_1$ as represented in Figure 4.27. At the same time, the momentum thickness $\delta_2$ progressively increases. This implies a significant augmentation of the shape factor $H_{12} = \frac{\delta_1}{\delta_2}$ up to a relatively high value of $H_{12} = 3.5$. Boundary layer computations have been carried out up to 60% of chord where separation occurs. Numerical boundary layer velocity profiles, at 30% chord (full line) and 55% chord (dashed line), have been plotted and compared to hot wire probing (symbols). Even though
4.4 Active Wave Cancellation: Direct Frequency Mode

Figure 4.26: Normalized external velocity distribution along the unswept ONERA-D airfoil for an angle of attack $\alpha = 2^\circ$. Symbols stand for measurements, dashed line for interpolated values.
some discrepancy exists, in particular at $x/c = 55\%$ just upstream of the separation, the agreement is rather good.

To describe the laminar-turbulent transition, it is common practice to distinguish three successive processes. The first, taking place close to the leading edge, is the receptivity. It describes the means by which external disturbances (such as free-stream turbulence or noise as well as wall surface imperfections) excite the eigenmodes of the boundary layer. In the following amplification phase, these eigenmodes develop into periodic waves which are convected in the streamwise direction. Some of them are linearly amplified and will trigger transition further downstream. Their evolution is well described by the linear stability theory. Once the amplitude of the waves is large, non-linear interactions occur and rapidly lead to turbulence (third step). In the framework of classical linear stability theory, disturbances are introduced as:

$$q'(x, y, z, t) = \hat{q}(y) \cdot \exp(-\alpha_i x) \cdot \exp(i(\alpha_r x + \beta z + \omega t)) \quad (4.1)$$

where $q'$ is a fluctuation (velocity, pressure or temperature) and $\hat{q}$ its amplitude function (here $x$ is perpendicular to the leading edge and $y$ normal
4.4 Active Wave Cancellation: Direct Frequency Mode

Figure 4.28: Linear stability analysis for a free-stream velocity of $U_\infty = 7 \text{m/s}$. Computations have been performed using CASTET stability code.

to the wall). Considering the spatial theory, $\alpha = \alpha_r + i\alpha_i$ is the complex wave-number in the $x$ direction. The spanwise wave-number $\beta$ and frequency $\omega$ are real. Introducing expression 4.1 in the Navier-Stokes equations leads to a system of ordinary differential equations for the amplitude functions. The stability of the flow depends on the value of the imaginary part of the longitudinal component of the amplification vector $\alpha_i$. When positive the flow will be stable, when negative, the perturbation will be amplified and transition will be triggered.

$$N = \ln\left(\frac{A}{A_0}\right) = \int_{x_0}^{x} -\alpha_i(\xi)d\xi$$ (4.2)

To quantify the amplification of disturbances, it is common practice to introduce the so-called N factor given by relation 4.2, where $A$ is the amplitude of the disturbance at a streamwise position $x$. Physically, the N factor describes the total amplification rate of small disturbances along the propagation path. Considering low velocity two-dimensional flow, only two-dimensional waves ($\beta = 0$) need to be considered (N factor is simply computed by integrating $-\alpha_i$ in streamwise direction), since Squire’s theorem states that they are the most relevant ones.

Figure 4.28 represents the linear stability computation on the upper side of the ONERA-D airfoil for an angle of attack of $\delta_2$ and an upstream velocity of $U_\infty = 7 \text{m/s}$. A first analysis has been undertaken for frequen-
cies between 200 Hz and 1 kHz in steps of $\Delta f = 200$ Hz (Figure 4.28(a)). As the frequency increases, the maximum of amplification is reduced and moves towards the leading edge. A computation with higher resolution, $200$ Hz $< f < 400$ Hz with $\Delta f = 40$ Hz, corresponding to the order of magnitude of the plasma actuation frequency, is presented in Figure 4.28(b). At 30% of chord, the location of DBD2, the most unstable frequency is around $f \approx 280 / 320$ Hz. In general, the numerical findings obtained with the linear stability analysis agree well with the experimental observations.

### 4.4.2 Experimental Results

The experiments have been split into two phases. During an initial testing phase experience had to be gained performing active wave cancellation on the ONERA-D wing model, as well as to use an additional plasma actuator as an artificial disturbance source on this airfoil. Following this step, the feasibility of the direct frequency mode for active wave cancellation using a single DBD plasma actuator had to be proven. In order to do so, a setup employing a beat frequency approach without the use of a closed-loop controller was chosen, reproducing the experiments of Grundmann and Tropea [72]. This allows for time efficient parameter studies to find appropriate settings and the corresponding attenuation rates. In the second testing phase transition delay on the wing model has been demonstrated with closed-loop control applied.

#### 4.4.2.1 Predetermined Active Wave Cancellation

For this set of measurements the excitation frequency at the upstream actuator DBD1 has been set to a value close to the naturally occurring TS frequencies ($f_{DBD1} = 250$ Hz). As the artificially excited waves travel downstream, they reach the control actuator (DBD2) which was operated at a slightly shifted frequency ($f_{DBD2} = 251$ Hz) in order to create a beat frequency with the two signals due to the continuously changing phase relation.

Some typical results from these experiments are presented in Figure 4.29 for a free-stream velocity of $U_\infty = 6.5$ m/s. The hot wire measurements shown, were taken at $x/c = 40\%$ inside the boundary layer at a wall-normal distance of $y = 0.4$ mm. The base flow case (Figure 4.29(a)) exhibits a low fluctuation level within the CTA signal of $u'_{rms} = 0.015$ m/s. With excitation (Figure 4.29(b)) this disturbance level is raised to $u'_{rms} = 0.076$ m/s. Applying the control (Figure 4.29(c)) a slow oscillation of the amplitude
Figure 4.29: Velocity signal given by the hot wire probe located at $x/c = 40\%$ at a wall-normal position of $y=0.4\text{mm}$. (7a) Base flow (left), (7b) with excitation (DBD1 on, DBD2 off; center), (7c) beat frequency experiment (DBD1 on, DBD2 on; right).
4 Transition Control

Figure 4.30: Time trace of U-velocity fluctuation induced by DBD1 (dashed line) compared to the base flow case (solid line) \((f = 250 Hz, U_\infty = 6.5 m/s, x/c = 40\%)\).

of the TS waves farther downstream the second actuator develops, with a maximum amplitude above the one of the unaffected waves (amplification) and minimum amplitude below the unaffected wave (damping) resulting in an almost unchanged rms-value of \(u'_{\text{rms}} = 0.074 m/s\) in this case. Figure 4.30 shows a time trace of the excited TS wave signal with smaller time scale (dashed line) in comparison to the base flow case (solid line), revealing that a clean TS wave train has been produced by DBD1.

Two important results emerge from these experiments. First of all the unsteady momentum production of the plasma actuator can be utilized to excite TS waves, if applied at the appropriate position, amplitude and a frequency the flow is susceptible to. Secondly and most important, the direct frequency approach for flow control proved to be applicable and can be utilized for active wave cancellation.

4.4.2.2 Transition Delay Using Closed-Loop Control

Following the promising beat frequency experiments, closed-loop control has been applied in order to demonstrate transition delay using the direct frequency approach. The free-stream velocity is \(U_\infty = 7 m/s\) in this case, the angle of attack remains at \(\alpha = +2^\circ\).

A spectral analysis 35mm downstream of DBD2 (40% chord) reveals the frequency content of the flow, as shown in Fig. 4.31. The signal of the stationary error sensor of the closed-loop control system is used for this
4.4 Active Wave Cancellation: Direct Frequency Mode

Figure 4.31: Spectral analysis of the error sensor signal for base flow (DBD1 off, DBD2 off), excitation (DBD1 on, DBD2 off), and controlled case (DBD1 on, DBD2 on) \((f = 280Hz, U_\infty = 7m/s, x/c = 40\%)\).

Figure 4.31 shows that introducing the excitation at 10% chord (DBD1 on, DBD2 off) produces the expected peak around 280Hz as well as an overall increase in the turbulence level as transition is being promoted. This increase is visible at the error sensor, since its location is close to the point of transition for the excited case \((\approx 47\% \text{ chord})\). Applying the control (DBD1 on, DBD2 on) the TS peak at 280Hz can be reduced by about one order of magnitude. This effect is accompanied by a decreased the overall turbulence level.

analysis. Plotted is the power spectral density in dB/Hz over frequency at a wall-normal position of \(y = 0.2mm\). In the base flow case (DBD1 off, DBD2 off) two frequency peaks, one at 250Hz and a wider peak around 340Hz, are prominent. These frequencies represent the naturally occurring TS waves present in the boundary layer for the given flow situation. However, as had been shown with linear stability analysis, frequencies around 340Hz are damped downstream of DBD2, with the limit for the unstable frequency band being about 300Hz. A frequency sweep in the unstable range revealed that an excitation at 280Hz is leading to the cleanest TS wave signal at the location of the error sensor. Consequently it was decided to use this frequency for the subsequent AWC experiments.
Figure 4.32: rms-value of CTA signal moving along the chord of the airfoil at a constant wall distance of $y = 1\, mm$ ($f = 280\, Hz$, $U_\infty = 7\, m/s$).

Figure 4.32 depicts a typical result of the transition delay studies. The rms-value of the longitudinal velocity fluctuations recorded at various down-stream locations is plotted for a constant distance above the wall within the boundary layer. It is well known that transition is characterized by an abrupt increase of velocity fluctuations [143] due to the at first intermittent spots and then continuous presence of turbulent flow. The dark blue curve ($\triangle$) represents the natural transition case with the onset of transition at about 60% chord, i.e. neither the disturbance source nor the control actuator is operating. Turning on the disturbance source, the TS wave amplitude is significantly increased at $f = 280\, Hz$ which moves the transition region upstream to about 40% chord ($\Box$). Then, with the control system active, the region of transition can be shifted downstream significantly by about 10% chord length ($\diamond$).

### 4.4.3 Resumee Section 4.4

A single dielectric-barrier discharge actuator operated in direct frequency mode has been utilized to effectively delay transition on an airfoil. The delay of the transition was achieved by active cancellation of artificially
excited TS waves. In contrast to previous active wave cancellation experiments using DBD actuators, the actuators driving signal was not modulated at the TS wave frequency, but the actuator was operated directly at the TS wave frequency. This approach exploits the fact that the force production of the DBD plasma actuator is highly unsteady at the operation frequency. A robust extremum-seeking controller supplied by the Technische Universität Berlin has been used to optimize the phase relation between incoming TS wave and the unsteady force production. A substantial transition delay of about 10% chord could be achieved on the ONERA-D airfoil for the case of artificial TS wave excitation. This work is the first to demonstrate the feasibility of wave cancellation with plasma actuators operated at TS frequency. This is considered to be an important step towards wave cancellation with plasma actuators at significantly higher free-stream velocities and higher TS wave frequencies. The proof-of-concept was successful, despite the fact that the attenuation rates of the TS waves are not yet as high as in other DBD AWC experiments. A careful design and adaptation of the actuator will be necessary for further improvement of the cancellation results.

The investigations and results discussed in this section have been presented in:

4 Transition Control
5 In-Flight Transition Control

This chapter presents the culmination of eight years continual plasma-actuator research at the Technische Universität Darmstadt. A drag reducing flow control system is demonstrated successfully on a full size aircraft. The development of the experimental setup started with simple proof-of-concept experiments - an innovation made in Darmstadt [71] - and has its preliminary climax with this successful in-flight demonstration. Numerous issues and questions arose after the first demonstrations and were processed to increase the technological level of the flow control approach. Among them are new analysis tools of the actuators’ behavior and features (Chapter 2), new findings on the influence of ambient conditions on the actuator performance and new procedures for their compensation (Chapter 3) and fundamental research on the basic understanding and optimization of the underlying fluid-mechanical mechanisms (Chapter 4). All these developments and findings have contributed to the experimental work discussed in this chapter.
5 In-Flight Transition Control

5.1 Experimental Setup

The description of the experimental setup is divided into a brief description of the aircraft and the wing glove that provides the space and surface for the integration of the sensors and actuators, each described in a separate subsection.

5.1.1 Motorized Glider G109

For the free-flight investigations, a Grob G 109b motorized glider is manned with two crew members. The aircraft combines the advantages of vibrationless gliding flight with a 96 kW engine for autonomous take-off and altitude gain. The trapezoidal wing with a span of 17.4 m and aspect ratio of 15.9 utilizes the Eppler E580 natural laminar flow (NLF) airfoil with subtle forward sweep. With a taper ratio of 0.55, the sweep angle at the leading edge is almost zero, minimizing spanwise pressure gradients and creating almost two-dimensional flow along the chord such that amplified Tollmien-Schlichting waves are expected to dominate any transition process either induced artificially or through atmospheric disturbances.

In order to host measurement equipment and accommodate a NLF wing glove, which is discussed in detail in Section 5.1.2, several modifications of the airframe structure as well as non-permanent customizations are necessary and certified by the German federal aviation authority (LBA). To account for the additional payload, a temporary "permit to fly" is issued to allow a maximum take-off weight of $m_{MTOW} = 950$ kg. The angle of attack ($\alpha$) exploitable for the experiment ranges between $\alpha_{\text{min}} = -3^\circ$ close to the maximum speed limit and $\alpha_{\text{max}} \approx 13^\circ$ just before complete flow separation.

5.1.2 Natural Laminar Flow Wing Glove

In order to quantify the influence of atmospheric turbulence on natural laminar flow airfoils, Weismüller [167] developed a wing glove for assembly on the right glider wing. Such a wing glove slides over the outer half of the tapered wing and matches smoothly at one defined spanwise position, enabling implementation of arbitrary sensors without interfering with the airframe structure. A custom designed airfoil forms a rectangular section of $1.35\,\text{m} \times 1.35\,\text{m}$. The characteristics of the pressure side of this airfoil predestines it for flow control investigations since the pressure gradient is almost linear and adjustable between moderate positive and negative
values, expanding the region of possible transition locations. The flow over the wing glove was numerically investigated and shown to be largely two-dimensional, except for the lateral tapered regions connecting the wing glove to the glider wing. In the center portion, an exchangeable acrylic measurement insert accommodates a streamwise array of 15 microphones as well as a single DBD actuator. An underwing pod for storage of equipment completes the wing glove. More details on the design, fabrication and equipment of the glove were discussed in Section 4.2.1.

5.1.3 Sensors

Along the outboard portion of the wing glove, 64 pressure taps are distributed over the suction and pressure side to resolve the pressure distribution during flight. A single boom protruding into the flow is installed on a front socket on the leading edge to enable the measurement of static and total pressure and to accommodate a X-hot-wire probe for acquisition of flow speed $U_\infty$ and angle of attack $\alpha$.

In order to acquire time-resolved velocity data of the boundary-layer along the exchangeable measurement insert, a light-weight, three-axis traversing system was developed which can be installed on either side of the wing glove. The accuracy of wall-normal positioning of a velocity probe at the tip of a probe holder is 0.1 mm for highly resolved boundary-layer profiles. A single hot-wire probe connected to a Dantec MiniCTA constant temperature anemometer (CTA) bridge provides the velocity signal and resolves the velocity fluctuations contained inside the boundary-layer. The hot-wire equipment is calibrated to the expected flight speed and angle-of-attack range and a circuit-integrated analogue low-pass filter is set to 3 kHz. Although higher frequencies can be resolved, this filter is chosen to minimize disturbing effects due to high-frequency DBD operation.

An array of 15 microphones, arranged in streamwise direction underneath the measurement insert, resolves the transition region with 30 mm accuracy. The connection to the surface is provided by one small orifice of 0.2 mm diameter for each microphone. The microphone and hot-wire signals are both sampled at a frequency of 16 kHz. An overall sketch of the pressure side of the wing glove instrumented with the different sensors is provided in Figure 5.1.

The ambient flow conditions encountered during flight are measured on the left wing. Two pressure sensors determine the static and dynamic pressure at a second boom, mounted upstream of the left wing. The temperature is measured with a PT1000 temperature sensor and a humidity
sensor is installed to determine the current air density. A Dornier Flight Log, which is a wind vane following the incident flow, is mounted on the boom and provides the angle of attack $\alpha$ as well as the sideslip angle $\beta$. All ambient data is sampled at a frequency of 10 Hz.

### 5.1.4 Dielectric Barrier Discharge Actuator

A flush-mounted DBD plasma actuator is positioned on the wing glove at $x_{DBD} = 440$ mm downstream of the leading edge, corresponding to $x_{DBD}/c = 0.33$. It consists of 0.3 mm Kapton tape between two copper electrodes which are powered by an alternating high voltage of varying intensity at a fixed driving frequency $f = 7.8$ kHz. The high voltage is generated by a GBS Elektronik MiniPuls 2.1 and can be adjusted via an analogue 0-5 Volt input signal $\Omega$ to the high-voltage generator, thus enabling momentary control of the applied actuator force.

### 5.2 Influence of Ambient Conditions

As thoroughly discussed in Chapter 3 non-negligible effects of ambient conditions on the actuators’ performance were observed. In flight, several of these influential parameters change significantly, and would result in variations of the performance and force production of the actuator. To evaluate the in-flight performance of the flow control device, results from actual
flight measurements are presented in the following. This enables an ef-
fect quantification of ambient condition changes on DBD performance and
is the basis for a closed-loop control of the actuator performance during
in-flight flow-control experiments.

5.2.1 Power Measurements

As a basis for the performance quantification the actual power consumption
of the plasma actuator is measured. The investigations discussed in Section
2.2 showed that a close correlation between the power consumption and the
force magnitude exists. To measure the voltages of the driving signal and of
the capacitance to calculate the power consumption, a Picotech PicoScope
4424 4-channel digital oscilloscope is installed in the underwing pod and
connected to the acquisition PC via USB. The sampling frequency of the
voltage signal is 1 MHz to finely resolve the alternating voltage signals and
enable time-resolved power measurements. The post-processing routines to
derive the instantaneous power consumption from the acquired voltage data
is implemented in LabVIEW together with the processing of the ambient
condition data.

5.2.2 Quantification of Ambient Condition Influences

In order to evaluate the effect of ambient condition variations on the power
consumption, the DBD actuator is operated with a constant input signal
Ω applied to the corresponding port of the high voltage generator. Under
constant ambient conditions, this produces a constant voltage amplitude
and power consumption of the DBD device. During flight, the DBD power
consumption and the ambient flow data are measured to correlate the data.

The typical flight procedure for flow-control experiments is a motorless
gliding descent starting from 10,000ft altitude. During the descent the
static pressure and the temperature increase. Humidity φ may vary due to
clouds and weather conditions, influencing not only the DBD performance
but also the air viscosity and the Reynolds number Re. The static pressure
from the ambient air data acquisition is indicated in Figure 5.2 (a), showing
increasing pressure during the descent. Due to the atmospheric dependence
between temperature and pressure, the temperature also increases slightly.
The relative air humidity φ during this specific flight remains almost con-
stant at 67%±2% whereas the momentary power consumption is illustrated
to derive the dependence between the pressure and DBD power consump-
tion.
The trend of decreasing power consumption with increasing pressure agrees well with the observations by other authors [2, 89, 17]. A 3% pressure increase leads to a 4% decrease of the consumed power, agreeing with the linear dependence reported by Kriegseis [89].

A horizontal, motor-powered flight is conducted to remove the influence of pressure changes while flying through air layers of varying humidity beneath the cloud base level. A strong humidity variation by 30% is contrasted to the power consumption in Figure 5.2 (b). Due to saturation and condensation on the surface, the humidity reads 100% at the beginning and the end of the flight segment. This cannot be resolved by the humidity sensor but strongly influences the power consumption as also reported by [21]. A gradient of the power consumption is therefore also found in regions of saturated, constant humidity. The power variation is in the order of 5% for a 30% humidity change.

Obviously, the relative sensitivity of DBD performance towards humidity gradients is minor than for pressure changes. Nevertheless, the pressure decreases slightly but continuously during the measurement flights, whereas strong humidity gradients can occur instantaneously. During regular atmospheric flights both influences can couple and additional, unpredicted effects may lead to DBD performance variation. Therefore, a closed-loop control of the actuator power is desired to maintain a constant performance during flow-control measurements. The following section introduces the concept and shows the successful application of a closed-loop control.
algorithm in the free-flight experiments.

5.2.3 Closed-Loop Control

In the following the power consumption is used as an input to an automated PI closed-loop controller implemented in *LabVIEW*. A PI architecture without any differential component is chosen since the incoming power data can be processed at iteration frequencies in the kHz range, orders of magnitude faster than the detection of ambient condition variations. The empirical Ziegler - Nichols method \cite{170} reveals suitable controller parameters. The controller output $\Omega$ is compared to the changing relative humidity $\phi$ for two flight situations in Figures 5.3. In order to enhance comparability to the experiments reported in the following section, the power consumption is henceforth normalized with the DBD actuator length in spanwise direction. Figure 5.3 (a) illustrates measurements while crossing beneath a cloud base in motorized horizontal flight. The humidity increases significantly and the controller output is adjusted while maintaining the power constant at $P_A = \bar{P}_A = 15 \text{ W/m}$. During the gliding flow-control flights, no such abrupt changes of the ambient conditions occur since these are performed either above or far from clouds. Nevertheless, the aptitude of the controller to level out different target powers under varying conditions needs to be demonstrated. Figures 5.3 (b) indicates the successful maintenance of a much higher power level $P_A = \bar{P}_A = 69.2 \text{ W/m}$. The ability of the control algorithm to provide constant DBD performance at various magnitudes is relevant for the flow-control experiments reported in the following section.

5.3 In-Flight Transition Delay

In order to perform in-flight transition control experiments, motorless gliding descents are required at quiescent air conditions. The flights are performed early in the morning, when the air is calm with low atmospheric turbulence due to buoyancy effects and thermals. After climbing to a terminal altitude of 10,000 ft, the engine is switched off and the propeller blades turned to a gliding position. The desired flight state is approached and maintained as close as possible, trying to minimize pilot induced oscillations and aircraft eigenmodes by adequate flight procedures. Before a minimum safety altitude is reached, the motor is turned on and the same measurement schedule is performed with DBD actuator switched on. This
approach ensures the best repeatability of the environmental conditions for the transition delay experiments.

### 5.3.1 Transition Measurements with Microphones

Microphone signals are qualified to determine the location of transition since the wall pressure fluctuation level rises for all frequencies as the flow becomes turbulent. The 15 microphones installed under the exchangeable measurement insert thus allow the transition location in streamwise direction to be determined and monitored during a measurement flight. The boundary-layer state can quickly be evaluated by considering the signal spectra from a fast Fourier transformation (FFT) of the sensor signal. Each data block for the FFT has a size of 16,000 values, corresponding to one second of measurement time, and is high-pass filtered with a cutoff frequency at 100 Hz. Although frequencies up to 8 kHz are resolved, the plot only shows the frequency range of interest for the transition investigations. Since the microphones are connected to the flow through a 0.2 mm diameter orifice, the transmission behavior for frequencies higher than 2 kHz is limited.

If the spectral amplitudes of frequencies $f$ are visualized for each streamwise sensor position $x/c$ in a color level plot, Figure 5.4 (a) is obtained. Red illustrates high amplitude levels whereas low amplitudes are colored blue. The amplitudes of all frequencies rise significantly at a position of
5.3 In-Flight Transition Delay

$x/c = 0.6$, which indicates transition at this streamwise position. The plot also shows a higher signal level for positions upstream of $x/c = 0.6$ and frequencies of $600 - 800$ Hz, which is the expected frequency range of natural disturbances contained within the boundary-layer flow. The results confirm that microphone signals are adequate to detect boundary-layer instabilities and the laminar-turbulent transition on the wing glove. Major disadvantages of the microphone measurements are sensitivity variations stated as $10 \text{mV/Pa } \pm 2.5 \text{dB}$ by the manufacturer in combination with manual installation and possible surface orifice clogging. Due to these uncertainties, only 8 of the 15 microphones provided adequate signals, limiting the spatial resolution for transition quantification.

![Spectrogram of microphone signals without (a) and with (b) traverse installed](image)

(a) No traverse  (b) Traverse installed

Figure 5.4: Spectrogram of microphone signals without (a) and with (b) traverse installed, $\alpha = 0.7^\circ$, $U_\infty = 38 \text{ m/s}$.

The test flight shown in Figure 5.4 (a) is performed at an angle of attack of $\alpha = 0.7^\circ$ and $U_\infty = 38 \text{ m/s}$ flight speed without the boundary-layer traverse system being installed on the wing. Figure 5.4 (b) illustrates the measurement for the same flight conditions after installing the traverse system. Now the flow is turbulent downstream of the $x/c = 0.47$ and exhibits discrete frequency peaks at approximately $300$ Hz and $600$ Hz. These disturbance frequencies originate from acoustic excitation due to flow separation on the traverse support. As illustrated, the traverse significantly affects the transition process on the wing glove pressure side. Nevertheless, the traverse is necessary to overcome the spatial resolution limitations of the microphone array and confirm the effect of DBD operation in Section 5.3.2.
5 In-Flight Transition Control

The disturbance effect of the traverse can also be noted in Figure 5.5 (a), showing the pressure distribution for both cases. The ram effect of the installed traverse system causes a decreasing pressure downstream of $x/c = 0.6$ due to the proximity of the traverse support to the pressure taps. For comparison of the experimental data with numerical investigations, the cumulative effects of all such disturbances has to be kept in mind.

Figure 5.5: Pressure distribution and environmental conditions during measurement flight at $\alpha = 0.7^\circ$.

5.3.2 Transition Delay Measurements

The spectrograms in Figures 5.4 (b) and 5.6 (a) illustrate the transition for flight at an angle of attack of $\alpha = 0.7^\circ$ and $U_\infty = 38 \text{ m/s}$ flight speed with installed traverse but without DBD operation. Laminar flow is found at streamwise locations up to approximately $x/c = 0.47$ with a confined range Tollmien-Schlichting instabilities between $f = 600 - 800 \text{ Hz}$. Downstream of $x/c = 0.47$, a broadband increase of disturbance magnitudes associated with transition to turbulence occurs. The effect of the single DBD actuator operated at a controlled power level of $P_A = 66.6 \text{ W/m}$ becomes apparent in Figure 5.6 (b). A delay of the transition is indicated by further downstream occurrence of the broadband amplitude increase around $x/c = 0.5$. Additionally, the unstable disturbance frequencies around 700 Hz are effectively attenuated. Correct quantification of the transition delay is difficult due to the microphone spacing and erroneous sensor readings. Based on
linear interpolation, approximately 3% chord length are estimated from
the microphone measurements. Nevertheless, these results show the first
successful DBD transition delay performed in flight under atmospheric con-
ditions. A variation of the actuator power consumption reveals increasing
transition delay effectiveness up to the power of $P_A = 66.6 \text{ W/m}$, beyond
which no further effect is obtained. To optimize the energy efficiency of the
flow control discussed in Section 5.4, the least possible power consumption
for the highest possible flow control effect should be selected.

Figure 5.6: Spectrogram of microphone signals for $\alpha = 0.7^\circ$, traverse sys-
tem installed, DBD on/off.

Hot-wire boundary-layer data can be obtained with the traverse system
mountable on the wing glove. Boundary layer profiles were recorded on the
pressure side of the wing glove for different streamwise positions to local-
ize the transition region. For take-off and landing, the mounted hot-wire
sensor is positioned 20 mm away from the surface to reduce the risk of struc-
tural damage or probe destruction due to vibrations. Immediately before
the measurement, the hot wire is traversed down to the surface to ensure
that the measured velocity profile starts directly at the wall. Each velocity
profile is recorded with 45 non-equidistant steps in the wall-normal direc-
tion, finely resolving the wall-proximity and obtaining sufficient averaged
velocity values outside the boundary-layer. All hot wire data are tempera-
ture corrected [115] to account for the changing ambient conditions during
flight.

Figure 5.7 illustrates normalized velocity profiles acquired at four stream-
wise positions within the transition zone on the wing glove at $\alpha = 0.56^\circ$, $U_\infty = 38.6$ m/s. Solid lines indicate measurements without, dashed red lines with DBD operation at $P_A = 66.6$ W/m. With increasing streamwise distance, the velocities measured directly at the wall increase continuously without flow control. This indicates the increasing momentum exchange within the wall-near region associated with temporal occurrence of turbulent spots. The last velocity profile at $x/c = 0.5$ exhibits the turbulent characteristic of a fuller velocity distribution in the proximity of the wall. For the flow-control case, the velocity profiles do not show significant variation along the streamwise dimension. The boundary-layer thickness increases insignificantly over the illustrated 10% chord and no evidence of initiated transition is found if the DBD actuator is operated.

Despite this indication of postponed transition in the case of DBD forcing, the uncertainty of the data acquisition needs to be addressed. The variation of the normalized freestream velocity at the boundary-layer edge by 4% indicates that the flight state cannot be maintained throughout the complete measurement. Data acquisition at 45 wall-normal positions for each velocity profile plus traversing of the probe sum up to six minutes duration for one complete set of data, during which the environmental conditions change more dramatically as compared to the former investigations. A necessary limitation to four profiles impedes a detailed analysis of the shape factor evolution. During the measurement, the plane descends 3,000 ft and the Reynolds number increases by 5.9% due to increasing air density and viscosity, even if the flight velocity is maintained. To ensure the comparability of results, the same flight maneuvers are performed once without and, after climbing to the initial altitude, again with DBD actua-
5.3 In-Flight Transition Delay

Figure 5.8: Standard deviation of the hot-wire signals at the wall \((y = 0 \text{ mm})\) under varying flight states, DBD on/off at \(P_A = 54.2 \text{ W/m}\).

If only the velocity fluctuation at the wall is considered, a finer resolution in streamwise direction can be combined with lower acquisition times through dramatic reduction of measurement positions. A trend of the hot-wire velocity fluctuations can be identified for flights without and with DBD actuation, characterizing the transition process. Figure 5.8 illustrates the standard deviation of the hot-wire velocity signal \(\sigma_u\) as the probe is traversed along the streamwise direction \(x/c\) in steps of 5 mm and positioned directly on the glove surface \((y = 0)\). A least-square-fitted trend line is approximated by a 4th-order polynomial for each case. Measurements without flow control at \(\alpha = 0.6^\circ, U_\infty = 38.6 \text{ m/s}\), shown in black squares in Figure 5.8(a), indicate a maximum value of the signal standard deviation at the downstream end of the measurement domain around \(x/c = 0.51\). This peak of the standard deviation is illustrative for the transition process and coincides with the position of maximum flow intermittency [8]. For this flight state, the signal peak with DBD operation at \(P_A = 54.2 \text{ W/m}\) is beyond the downstream end of the observable domain, and only the parallel displacement of the trend line slopes \(\Delta_{\text{par}}\) can be compared, rendering the transition delay 2.5%. A more precise quantification is possible for the
flight at higher speed $U_\infty = 39 \text{ m/s}$ at a decreased angle of attack $\alpha = 0.4^\circ$, illustrated in Figure 5.8(b). For both the controlled and uncontrolled cases, the intermittency peak is observed within the measurement area. The displacement of the peak $\Delta_{\text{peak}}$ and of the slopes both indicate a transition delay of 2%. The trend of diminishing transition delay for lower $\alpha$ is consistent with the decreasing boundary-layer stability due to flow speed and pressure gradient augmentation.

### 5.4 Efficiency Estimate

The efficiency of active flow control can be defined by relating the net power savings to the invested energy per time. Moreau et al. [112] estimate the power savings of boundary-layer flow control by integration of the momentum flux across a control volume. The change of the momentum distribution due to DBD operation is experimentally measured with a pitot tube and transformed into fluidic power by applying the conservation equation of kinetic energy.

In steady flight, the necessary propulsion power counterweights the aircraft resistance, which is to a large portion constituted by the wing drag. If the wing profile drag $D$ can be reduced, the fluidic power consumption

$$P_p = DU_\infty$$

is diminished. The drag coefficient $c_D$ is necessary to calculate the profile drag

$$D = c_D \frac{\rho}{2} l_{\text{ref}} U_\infty^2$$

where the reference length $l_{\text{ref}} = c = 1.35 \text{ m}$ is the wing glove chord length. The drag coefficient $c_D$ is composed of a pressure drag component, $c_{D,p}$, and a friction drag component, $c_{D,f}$. The friction drag coefficient accounts for the viscous interaction of the flow with the surface within the boundary layer. Computational fluid dynamics enables flow simulations, yielding all flow quantities of interest. Simple panel methods are sufficient to deliver the lift and drag of airfoils at specified flow conditions. For the present investigation, the Hess-Smith panel method in Xfoil is employed to simulate the flight conditions on the wing glove at $U_\infty = 38 \text{ m/s}$ and $\alpha = 0.7^\circ$. The drag coefficients are derived from two simulations with fixed transition on the pressure side, conservatively representing either the natural transition conditions ($x_{\text{trans}}/c = 0.47$) or the transition location under DBD influence ($x_{\text{trans}}/c = 0.5$).
The drag coefficient $c_D$ for the case with postponed transition is $\Delta c_D = \Delta c_{D,f} = 0.00008$ smaller as compared to the natural transition scenario without flow control. In both simulated cases, the pressure drag component is equal such that the drag coefficient reduction is caused only by the friction component. If these values are processed with equations (5.1) and (5.2), a reduction of the necessary propulsion power by $\Delta P_p = 3.63 \text{W/m}$ is computed.

The flow-control efficiency can then be defined by the ratio of the saved propulsion power $\Delta P_p$ to the actuator power consumption $P_A$.

$$\eta = \frac{\Delta P_p}{P}$$

(5.3)

The power consumed in the flight experiments is closed-loop controlled at $P_A = 66.6 \text{W/m}$, yielding a total efficiency of $\eta = 5.4\%$. In the current setting, no net gain is achieved since the power consumption significantly exceeds the power savings, which is a typical result encountered in active flow-control experiments. The calculations do not include the electrical efficiency $\eta_e \approx 60\%$ defined by Kriegseis [89] as the ratio of power supplied to the high voltage generator $P_{\text{input}}$ and the power $P$ consumed for the force generation. Additionally, the simplified evaluation based on Xfoil computations does not account for a local skin-friction increase at the actuator location due to the flow acceleration reported by Quadros [136]. These factors indicate overrated power savings, but positive effects are also disregarded. The propulsion thrust created by the actuator in flow direction could be utilized by a moving object but cannot be considered in this simplification. Flight experiments by Weismüller [167] using the same wing glove geometry show that Xfoil underestimates the absolute drag in comparison to measurements with a wake rake by a factor of approximately 0.75. Discussing the efficiency of DBD actuation, Kriegseis [89] differentiates between the net efficiency of flow control, $\eta_{FC}$, the electric efficiency $\eta_e$ related to the high voltage power supply, a fluid mechanic efficiency $\eta_{FM}$ and a savings rate $\eta_s'$ related as

$$\eta_{FC} = \eta_e \eta_{FM} \eta_s'.$$

(5.4)

The fluid mechanic efficiency $\eta_{FM}$ is defined as the ratio of power transferred into the fluid, $P_{FM}$, and the power $P_A$ supplied to the DBD actuator. In the context of the current work, the savings rate defines the ratio of the saved propulsion power $\Delta P_p$ to the fluid mechanic power $P_{FM}$. Since the electric efficiency has not been considered in this manuscript, the above estimated
efficiency $\eta = 5.4\%$ can be related to the fluid mechanic efficiency and the savings rate,

$$\eta = \frac{\Delta P_D}{P_A} = \frac{P_{FM}}{P_A} \frac{\Delta P_D}{P_{FM}} = \eta_{FM} \eta_s'.$$

(5.5)

The fluid-mechanic efficiency, quantifying the conversion of electric energy to kinetic fluid energy in otherwise quiescent air by currently available DBD actuators, has been determined by Kriegseis [89] and Jolibois and Moreau [82] in the order of $\eta_{FM} \approx 0.1\%$. Considering this value, the savings rate $\eta_s' = \eta/\eta_{FM}$ turns out to be in the order of 50 to yield the estimated net efficiency $\eta = 5.4\%$. It is this leverage effect of adequate actuator application which enables effective flow control for specific purposes, e.g. flow stabilization and subsequent drag reduction.

In total, the estimated efficiency may be considered a reasonable guess and, keeping in mind the remarkable savings rate for this specific flow control application, renders a net gain plausible if the flow-control setup can be further enhanced. This can either be achieved by elevated $\eta_{FM}$ of the flow control device, e.g. by new actuator configurations and materials, or by enhancing the savings rate $\eta_s'$ by means of optimized, additional actuator positions.

### 5.5 Resume Chapter 5

In-flight transition control employing a single DBD actuator is successfully demonstrated on a full-sized motorized glider. To achieve this, the influence of ambient condition variations on the DBD power consumption is characterized. Although a significant impact of humidity and pressure variations is observed, the implementation of a closed-loop controller enables a constant flow control authority during gliding measurement flights. The laminar-turbulent boundary-layer transition is delayed on the pressure side of the experimental wing glove for approximately 3% of the chord length. This leads to a reduction of the airfoil drag by 1.9% as estimated from Xfoil calculations, and constitutes a significant step forward, considering the low-speed laboratory experiments to which DBD transition control was limited before these flight tests. The overall energy efficiency achieved is in the range of 5.4%, representing an unexpected good value, when considering that the control approach with the highest energy requirements was tested.
The investigations and results discussed in this chapter have been published in:


and were submitted to the AIAA Journal.
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6 Conclusions

The investigations summarized and discussed in this monograph represent the current state-of-the-art of plasma-actuator based boundary-layer control. This application of plasma actuators makes full use of the advantages of this type of flow control actuator. Most prominent are the two facts that the actuator is extremely light and thin, therefore easy to install, and that the actuation principle is free of any moving parts. The latter feature stands in contrast to other widely used flow-control actuators, e.g. piezo-type actuators, that require mechanical decoupling to not introduce unwanted vibrations into the structure, which are of the same frequency range as the disturbances in the flow.

Two disadvantages of plasma actuators are the limited magnitude of the force production and energy efficiency. The limits in the force production are more an issue for other applications such as separation control. There are no control authority issues with plasma actuators for active wave cancellation. The low electrical efficiency can be compensated by a strong flow control effectiveness, a first promising demonstration was presented in Section 5.4, where 50 times the fluid mechanical power introduced by the actuator was gained by an effective drag reduction.

The topic of energy efficiency was to date not seriously pursued in the plasma actuator community, mainly due to missing robust procedures to determine it precisely. The methods presented in this work now allow for a detailed efficiency analysis and first resilient comparisons with other flow control devices are possible.

Furthermore, the optimization of the boundary-layer control approach in terms of energy efficiency is also now possible. The availability of new concepts that make optimal use of the unique features of plasma actuators, is shown by introducing two operation modes of plasma actuators for active wave cancellation. The hybrid transition control mode, combines two methods and is successful in maintaining the inherent advantages of both combined approaches. The direct-frequency mode on the other hand overcomes the limits in terms of frequency capabilities and thereby opens the possibility for high-speed applications. Simultaneously it represents a method to eliminate one of the last main drawbacks of plasma actuators.
6 Conclusions

The force production is composed of two differently strong force pulses of opposing directions. Usually, the net remaining force is used for the flow control mechanism. The direct-frequency mode makes full use of both force pulses and therefore also offers a significant improvement of the efficiency of the actuator’s energy conversion.

However, the analysis of the energy efficiency of active wave cancellation using the different approaches is yet to be performed. It is expected, that the gain of 50, as achieved using the energy intensive boundary-layer stabilization, is easily tripled by the active wave cancellation. Since the drag increases with the square of the velocity, higher flight velocities will further help to increase the efficiency. An improvement of the energy efficiency by a factor of 5-10 is imaginable for the direct frequency mode. It seems plausible that an efficiency greater than one can be achieved in the future.

Another often discussed issue of plasma actuators is their durability and their not entirely understood dependency on environmental conditions. The investigation of the latter is systematically performed in Chapter 3 and new concepts and findings, as well as measures to counteract influences are thoroughly discussed. The durability falls into the topic of the fabrication of plasma actuators, which has not been discussed in this work. Manufacturing procedures and materials, such as LTCC (low temperature co-fired ceramics), sputtering of electrodes, purely ceramic electrodes and dielectric, electrode printing and etching and high performance printed circuit materials were tested and have yielded promising results. Thus some confidence can be placed in the expectation that issues of durability and surface roughness can be solved in the near future.

The plasma actuator technology and its development for boundary-layer control must be considered as a possible future technology with verified potential to reduce the energy requirements of future aircraft. This work discusses numerous unknowns and open questions, and elaborates concepts that demonstrate the requirement and meaningfulness of further research efforts. One can question how important the cancellation of Tollmien-Schlichting waves for transport aircraft is. Especially in the case of swept wings, the transition scenario is no longer dominated by the occurrence of such instable waves. The active control of cross-flow transition is more complex and experimentally more difficult to access than the scenarios discussed here. The same holds for the mechanisms and processes that are responsible for the drag generation in turbulent boundary layers. Both topics are certainly of greater importance for the transport aircraft industry. However, the multi-disciplinary complexity of plasma actuators makes the
development of a flow control system for these fluid mechanical phenomena impossible before further maturing of the plasma-actuator technology. For this reason it is reasonable to further develop such a new flow control system under more simple conditions. With this point of view, the development of transition delay systems for two-dimensional laminar boundary layers can also be considered as a test case to raise the plasma actuator technology from its infancy to a more mature option for future industrial application.
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Acknowledgements

I want to thank Prof. Dr. C. Tropea. The confidence he has instilled in me over the years and his efforts to provide an environment of financial security, office space, unrestricted access to facilities, international exchange and much more, are the basis of my professional career. Prof. Tropea is always helping improve the quality of our scientific work, to question the completeness of the investigations and interpretations, and to organize all activities in a strategic manner.

For an experimentalist, the cooperation with the machine shop is of greatest importance. Ilona Kaufhold, the machine-shop foreperson, and her team of machinists, have provided the best possible support for which I want express my gratitude. Monika Medina from the administrative team has ensured a frictionless processing of our projects in all aspects, even in matters beyond her call of duty.

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The first priority of our team was to collaborate such that the output and success of the team was greater than the output of all individuals. Being successful in this respect was especially difficult because the major scientific goal was to conduct the in-flight experiments presented in Chapter 5. It represented a great jump to conduct such experiments after the simple proof-of-concept that were made previously. Numerous developments had to be made and challenges had to be overcome, many of which required great personal commitments from my colleagues. Most memorable, but only one of many examples, was the fabrication of the first plasma-actuator
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wing glove, that demanded a great deal of patience and stamina from Armin Kurz. Without significant experience in the field of laminar boundary-layer flows the colleagues needed to have complete confidence in the feasibility of this project. This confidence was necessary even at times when a detailed understanding of our plasma actuator and our wind tunnels impeded a straight-forward collection and publication of results. It is this complete confidence in the significance of plasma actuators that has remained with us and has united us as a team.
# Nomenclature

## Latin letters

### lower case

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### Nomenclature

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<td>$F_b$</td>
<td>[N]</td>
<td>body force</td>
</tr>
<tr>
<td>$G(x)$</td>
<td>[-]</td>
<td>streamwise gray value distribution</td>
</tr>
<tr>
<td>$\hat{G}$</td>
<td>[-]</td>
<td>relative peak intensity</td>
</tr>
<tr>
<td>$G_p$</td>
<td>[-]</td>
<td>peak value of $G(x)$</td>
</tr>
<tr>
<td>$G_b$</td>
<td>[-]</td>
<td>background value of $G(x)$</td>
</tr>
<tr>
<td>$H_1$</td>
<td>[m]</td>
<td>lever arm</td>
</tr>
<tr>
<td>$H_2$</td>
<td>[m]</td>
<td>lever arm</td>
</tr>
<tr>
<td>$H_{12}$</td>
<td>[-]</td>
<td>boundary layer shape factor</td>
</tr>
<tr>
<td>$I$</td>
<td>[A]</td>
<td>current through the actuator</td>
</tr>
<tr>
<td>$I_{\text{input}}$</td>
<td>[A]</td>
<td>electrical input current</td>
</tr>
<tr>
<td>$J$</td>
<td>[-]</td>
<td>evaluation interval</td>
</tr>
<tr>
<td>$K$</td>
<td>[-]</td>
<td>number of discharge cycles</td>
</tr>
<tr>
<td>$K_d$</td>
<td>[-]</td>
<td>control parameter</td>
</tr>
<tr>
<td>$K_i$</td>
<td>[-]</td>
<td>control parameter</td>
</tr>
<tr>
<td>$K_p$</td>
<td>[-]</td>
<td>control parameter</td>
</tr>
<tr>
<td>$M$</td>
<td>[-]</td>
<td>Mach number</td>
</tr>
<tr>
<td>$L$</td>
<td>[mm]</td>
<td>plasma actuator length</td>
</tr>
<tr>
<td>$N$</td>
<td>[-]</td>
<td>N-factor</td>
</tr>
<tr>
<td>$N_t$</td>
<td>[-]</td>
<td>integral disturbance amplitude threshold</td>
</tr>
<tr>
<td>$P_{\text{A}}$</td>
<td>[W]</td>
<td>consumed electrical actuator power</td>
</tr>
<tr>
<td>$P_{\text{input}}$</td>
<td>[W]</td>
<td>electrical input power</td>
</tr>
<tr>
<td>$P_{\text{A}}^*$</td>
<td>[W]</td>
<td>preset power consumption</td>
</tr>
<tr>
<td>$P_i^*$</td>
<td>[W]</td>
<td>power level at time step i</td>
</tr>
<tr>
<td>$P_{\text{A}}^\text{min}$</td>
<td>[W]</td>
<td>minimum recorded power consumption</td>
</tr>
<tr>
<td>$\bar{P}_{\text{A}}$</td>
<td>[W/m]</td>
<td>average actuator power consumption</td>
</tr>
<tr>
<td>$\Delta P_P$</td>
<td>[W]</td>
<td>propulsion power, saved due to drag reduction</td>
</tr>
<tr>
<td>$P_{\text{FM}}$</td>
<td>[W]</td>
<td>power, transferred to the fluid</td>
</tr>
<tr>
<td>$Q$</td>
<td>[C]</td>
<td>charge crossing the electrodes</td>
</tr>
<tr>
<td>$Re$</td>
<td>[-]</td>
<td>Reynolds number</td>
</tr>
<tr>
<td>$S$</td>
<td>[m$^2$]</td>
<td>surface</td>
</tr>
<tr>
<td>$T_i$</td>
<td>[s]</td>
<td>control parameter</td>
</tr>
<tr>
<td>$T$</td>
<td>[K]</td>
<td>temperature</td>
</tr>
<tr>
<td>$T$</td>
<td>[s]</td>
<td>timer interval</td>
</tr>
<tr>
<td>$T_u$</td>
<td>[-]</td>
<td>turbulence intensity</td>
</tr>
<tr>
<td>$U$</td>
<td>[m/s]</td>
<td>streamwise flow velocity</td>
</tr>
</tbody>
</table>
Nomenclature

\( U_A \) [V] plasma actuator voltage
\( U_i \) [m/s] flow fields snapshot
\( U_\infty \) [m/s] free stream velocity
\( V \) [V] operating voltage
\( V \) [m^3] volume
\( V_{\text{input}} \) [V] input voltage
\( V_p \) [V] probe voltage
\( W(t) \) [N] weight balance signal
\( \bar{W} \) [N] averaged weight balance signal

Greek letters

<table>
<thead>
<tr>
<th>symbol</th>
<th>SI unit</th>
<th>description</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \alpha )</td>
<td>[( ^\circ )]</td>
<td>angle of attack</td>
</tr>
<tr>
<td>( \alpha )</td>
<td>[-]</td>
<td>wave number in ( x ) direction</td>
</tr>
<tr>
<td>( \alpha_r )</td>
<td>[-]</td>
<td>real part of the wave number</td>
</tr>
<tr>
<td>( \alpha_i )</td>
<td>[-]</td>
<td>imaginary part of the wave number ( \alpha )</td>
</tr>
<tr>
<td>( \beta )</td>
<td>[-]</td>
<td>wave number in spanwise direction</td>
</tr>
<tr>
<td>( \beta )</td>
<td>[-]</td>
<td>yaw angle</td>
</tr>
<tr>
<td>( \gamma )</td>
<td>[-]</td>
<td>isentropic exponent</td>
</tr>
<tr>
<td>( \delta_1 )</td>
<td>[m]</td>
<td>displacement thickness</td>
</tr>
<tr>
<td>( \delta_2 )</td>
<td>[m]</td>
<td>momentum loss thickness</td>
</tr>
<tr>
<td>( \delta )</td>
<td>[-]</td>
<td>boundary layer thickness</td>
</tr>
<tr>
<td>( \Delta_{\text{par}} )</td>
<td>[-]</td>
<td>transition delay, determined with parallel curve shifting</td>
</tr>
<tr>
<td>( \Delta_{\text{peak}} )</td>
<td>[-]</td>
<td>transition delay, determined with the peak values of the rms curve</td>
</tr>
<tr>
<td>( \Theta_A )</td>
<td>[( s^{\frac{3}{2}} A^{\frac{3}{2}} / W^{\frac{5}{2}} m )]</td>
<td>scaling number</td>
</tr>
<tr>
<td>( \phi )</td>
<td>[-]</td>
<td>relative air humidity</td>
</tr>
<tr>
<td>( \phi )</td>
<td>[-]</td>
<td>representative, general variable</td>
</tr>
<tr>
<td>( \phi )</td>
<td>[-]</td>
<td>phase angle</td>
</tr>
<tr>
<td>( \eta )</td>
<td>[-]</td>
<td>efficiency</td>
</tr>
<tr>
<td>( \eta_s )</td>
<td>[-]</td>
<td>savings rate</td>
</tr>
<tr>
<td>( \eta_e )</td>
<td>[-]</td>
<td>electrical efficiency</td>
</tr>
<tr>
<td>( \eta_E )</td>
<td>[-]</td>
<td>electrical efficiency</td>
</tr>
<tr>
<td>( \eta_{\text{FC}} )</td>
<td>[-]</td>
<td>net flow control efficiency</td>
</tr>
<tr>
<td>( \eta_{\text{FM}} )</td>
<td>[-]</td>
<td>fluid mechanic efficiency</td>
</tr>
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</table>
Nomenclature

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Unit</th>
<th>Description</th>
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<tbody>
<tr>
<td>$\rho$</td>
<td>[kg/m$^3$]</td>
<td>fluid density</td>
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<tr>
<td>$\sigma$</td>
<td>[-]</td>
<td>standard deviation</td>
</tr>
<tr>
<td>$\lambda_{\text{DBD}}$</td>
<td>[m]</td>
<td>wave length of the plasma light emission</td>
</tr>
<tr>
<td>$\omega$</td>
<td>[-]</td>
<td>frequency of a harmonic function</td>
</tr>
<tr>
<td>$\Phi$</td>
<td>[-]</td>
<td>general variable (exchangable)</td>
</tr>
<tr>
<td>$\Psi$</td>
<td>[-]</td>
<td>relative performance drop</td>
</tr>
<tr>
<td>$\Pi$</td>
<td>[-]</td>
<td>relative performance of the plasma actuator</td>
</tr>
<tr>
<td>$\Omega$</td>
<td>[V]</td>
<td>control signal</td>
</tr>
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</table>

Abbreviations

- AC: alternating current
- AWC: active wave cancellation
- CCD: charge-coupled device (camera sensor)
- CMOS: complementary metal oxide semiconductor
- CTA: constant temperature anemometer
- CV: control volume
- DBD: dielectric barrier discharge
- DEHS: Di-Ethyl-Hexyl-Sebacat
- DS: disturbance source
- EHD: electro hydro dynamic
- EKF: extended Kalman Filter
- FIR: finite impulse response (filter)
- FG: function generator
- FOV: field of view
- HV: high voltage
- HoWK: Göttinger-type vacuum wind tunnel facility
- LDA: laser doppler anemometry
- LMS: least mean square
- LSA: linear stability analysis
- NB: notebook
- NLF: natural laminar flow
- NWK2: low speed wind tunnel facility 2 at TU Darmstadt
- ONERA: Office national d’études et de recherches aérospatiales
- PA: plasma actuator
- PCB: printed circuit board
- PIV: particle image velocimetry
**Nomenclature**

<table>
<thead>
<tr>
<th>Abbreviation</th>
<th>Description</th>
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<tbody>
<tr>
<td>PID</td>
<td>proportional-integral-derivative controller</td>
</tr>
<tr>
<td>PMMA</td>
<td>Poly-Methyl-Methacrylat</td>
</tr>
<tr>
<td>PS</td>
<td>power supply</td>
</tr>
<tr>
<td>PTFE</td>
<td>Poly-Tetra-Fluor-Ethylene (Teflon)</td>
</tr>
<tr>
<td>RMS</td>
<td>root mean square</td>
</tr>
<tr>
<td>SD</td>
<td>surface discharge</td>
</tr>
<tr>
<td>TS</td>
<td>Tollmien-Schlichting (wave)</td>
</tr>
<tr>
<td>TVM</td>
<td>trisonic wind tunnel facility at TU Darmstadt</td>
</tr>
<tr>
<td>VD</td>
<td>volume discharge</td>
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<tr>
<td>WB</td>
<td>weight balance</td>
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<tr>
<td>xLMS</td>
<td>filtered x least mean squares (algorithm)</td>
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</table>
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