SWITCHING ARCS IN
PASSIVE RESONANCE HVDC
CIRCUIT BREAKERS

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Abstract

This work aims to systematically and accurately investigate switching arc characteristics in passive resonance high voltage direct current (HVDC) circuit breakers.

The replacement of classical band energy with fluctuating wind and solar power from peripheral locations in Europe will significantly challenge the European transmission grid in the future. Energy must be transmitted via cables for distances \( > 50 \text{ km} \) and via overhead lines for distances \( > 1000 \text{ km} \) with low losses. Voltage-Sourced-Converter (VSC) HVDC is considered to be superior to the classical alternating current (AC) transmission for long distance energy transfer, because it has significantly smaller losses and requires no reactive power compensation.

Mechanical circuit breakers are standard fault protection devices in AC networks but do not yet exist for HVDC with sufficient interruption performance. The current zero crossing, essential for arc extinction in mechanical breakers, is not inherently available in DC systems. This makes DC interruption more challenging than AC interruption. Passive resonance breakers excite an unstable current oscillation and create artificial current zero crossings by interaction of the switching arc with an LC-commutation circuit. This principle has been successfully applied for Metal-Return-Transfer-Breakers (MRTB) in operation. It is, however, limited in maximum interruptible current, takes too long for current zero creation and uses a large capacitor, which significantly contributes to the breaker costs.

Significant improvement of the interruption performance is expected if the arc chamber and nozzle design is optimized for passive resonant creation of current zero crossings in DC circuits. For this, the improvement of the switching arc characteristic is shown to be
more effective and most probably also more economical compared to the passive L and C components of the resonance path. The main goal of this thesis is a systematic characterization of different arc configurations for use in black-box simulations.

For that purpose, a novel arbitrary pulsed current source has been developed. By creation of complex current waveforms, (e.g. staircase-like currents and spikes superimposed on a current slope,) the transient and stationary arc characteristic can be measured independently of each other. Thereby, a more accurate parameter determination and a better validation of black-box models is achieved. In principle, the source could also be used to characterize the arc completely model-independent, by generation of step currents with variable slope steepness. Furthermore, a novel improved method for arc characterization has been developed. A flexible model circuit breaker has been used to investigate the effect of blow pressure, nozzle geometry, nozzle material and blow gas type.

The investigations confirmed that a falling stationary $UI$-characteristic with decreasing arc voltage at increasing current is a necessary condition for creation of passive resonance. A rising characteristic, the arc thermal inertia and high current gradients act as damping terms and inhibit passive resonance. For current amplitudes $< 2$ kA the following has been shown: a) an increase of blow pressure intensifies the falling slope of the stationary $UI$-characteristic and improves passive resonance, b) a narrow throat diameter and a large nozzle throat length exhibit a rising $UI$-characteristic and should therefore be avoided, c) nozzle material and blow gas type have shown only minor influence on the arc characteristics.

The results gained can be used to improve an existing HVDC MRTB in two ways: Firstly, if the time for current zero creation (and with it the total break time) is not important, the switching arc characteristic could be influenced so that a) the size of the capacitance is minimized or b) the interruption current amplitude is maximized. Application for this are MRTB or HVDC load break switches. Secondly, the time for current zero creation can be minimized by suit-
able arc chamber design changes with consequent changes in the arc characteristic. By this, together with an increase in maximum interruption current, existing MRTBs could be improved for use as HVDC circuit breakers. Such a passive resonance HVDC circuit breaker would be a low loss and low cost alternative to the recently proposed hybrid breakers, which use expensive and inherently lossy solid state components.
Zusammenfassung

Das Ziel dieser Arbeit ist die systematische und präzise Untersuchung von Schaltlichtbogencharakteristiken in Hochspannungs-Gleichstrom (HGÜ) Leistungsschaltern.

Der Ersatz klassischer Bandenergie durch fluktuierende Wind- und Sonnenenergie aus peripheren Standorten Europas stellt das Europäische Übertragungsnetz zukünftig vor grosse Herausforderungen. Energie muss in Off-shore-Kabeln über Distanzen > 50 km und in Übertragungsleitungen über Distanzen > 1000 km verlustarm zum Verbraucher geleitet werden. Hochspannungs-Gleichstromübertragung (HGÜ) wird gegenüber der klassischen Wechselstrom (AC)-Übertragung bevorzugt, aufgrund der markant kleineren Übertragungsverluste bei grossen Übertragungsdistanzen und aufgrund der Blindleistungskompensation, welche bei ersterer entfällt.


Eine wesentliche Verbesserung des Unterbrechungsverhaltens sol-
cher Schalter ist zu erwarten, falls die Lichtbogenkammer und die Düsengeometrie optimiert wird zur passiv resonanten Erzeugung von Stromnulldurchgängen. Es wird aufgezeigt, dass die Verbesserung der Lichtbogentaktistik effizienter und höchst wahrscheinlich auch ökonomischer ist verglichen mit der Optimierung der passiven L und C Elemente des Resonanzkreises. Das Ziel dieser Arbeit ist das systematische Charakterisieren von verschiedenen Lichtbogenkonfigurationen zur Verwendung in Black-Box Modellen.


Die Untersuchungen haben bestätigt, dass eine stationäre \( UI \)-Lichtbogentaktistik mit fallender Spannung bei steigendem Strom notwendig ist zur Erzeugung von passiver Resonanz. Eine ansteigende Charakteristik, die Lichtbogenträgheit sowie steile Stromgradien in den Lichtbogen verhindern passive Resonanz. Es konnte für Ströme \( \leq 2.5 \text{kA} \) folgendes gezeigt werden: a) Ein Erhöhen des Beblasungsdrucks verstärkt die passive Resonanz durch Erhöhen von Steilheit und Spannung der \( UI \)-Charakteristik, b) ein enger Durchmesser oder eine große Länge des engen Bereichs der Düse hingegen führen zu einer ansteigenden Charakteristik und sollten daher vermieden werden, c) verschiedene Düsenmaterialien und Beblasungsgase zeigten nur einen kleinen Einfluss auf die Charakte-
ristik.

Acknowledgement

At the start of this Ph.D. project, Prof Christian Franck told the following to me: "The times, when a scientific breakthrough could be achieved by a single genius person, working alone in his chamber, are long gone. Today, successful science is performed in a team of researches, working together and exchanging their ideas". This sentence illustratively expresses that this work would not have been possible without the support of many people.

I would like to thank Christian for the trust he set in me, to confide me with one of his key projects. The interesting field kept me continuously fascinated and motivated for the last three and a half years. In the daily work, Christian gave me the freedom to develop my own approaches, supported me when I needed guidance and always advised me with his expert knowledge. I believe that it is a rare skill, to find such a balance in leading his employees.

Warmly, I would like to thank Markus Bujotzek, Riccardo Bini and Manolis Panousis from the ABB Corporate Research. Their experience in circuit breakers research and valuable inputs in discussions, helped to set the right focus in the research work.

Carsten Leu from TU Ilmenau was a very welcome discussion partner during the work on a joint paper. At TU Ilmenau, Carsten applied for the first time the Drebenstedt method on passive resonant currents and is therefore very experienced in the field of experimental arc parameter determination. Carsten contributed the literature research on the classical parameter determination methods to our common paper, which also served as a basis for the corresponding theory section in this thesis.

Thanks to Dr. Timm Teich for careful proof reading of the manuscript and for his valuable inputs.
Several students worked in the scope of their master and semester projects on integral parts of the experimental setup. These were: Michael Leibl, Daniel Rothmund and Andreas Ritter in their semester project and Andreas Ritter, Moonjo Kang and David Gassner in their master project. Michael Leibl supported the set up and execution of the first measurements series on free burning arcs. Daniel Rothmund built the setup for the second measurement series with sinusoidal currents and performed experiments on free burning and radially blown arcs. He also assembled and characterized a flexible coil for the arbitrary current source. Andreas Ritter assembled the current source modules, programmed the code for the measurement automation, based on a code structure from Dominik Dahl, and validated the IGBT-controller performance in an intensive measurement study. Moonjo Kang and David Gassner assembled the model circuit breaker and they performed a measurement series on blow-pressure effects and contact movement effects in convection stabilized arcs.

The IGBT controller, a key component for the arbitrary current source, has been developed by WEMEL GmbH. In particular, I would like to thank Martin Weidmann from WEMEL for his excellent cooperation in the development phase. At the stage of controller development it has been uncertain, how the arc would behave as a load. With his foresight, Martin identified and avoided numerous technical challenges already in a very early planning phase and was always eager to maintain maximum possible flexibility of the controller hard- and software. This flexibility significantly facilitated later optimization of the controller for the investigated arc.

A major support in the whole three years was the technical staff in our team: Thanks to Hans-Jürg Weber, Henry Kienast, David Brühlmann, Claudia Stucki and Karin Sonderegger.

The mutual discussion with all colleagues in the High Voltage Group was an important pillar in the daily work. Thanks in particular to my office mates Ueli Straumann, Philipp Simka, Michael Strobach, Andreas Ritter, Moonjo Kang and Pascal Häfliger for the enriching time and mutual support. Especially, I would like to men-
tion Philipp Simka, who encouraged me to start as a Ph.D. student in Christians group.

I would also like to express my gratefulness to my parents Erika and Hansruedi Walter, who always supported me on my way and do that still.

Finally, I would like to thank my wife Ivana. She always encourages me, in what I am doing and supports me in so many different ways. She is very understanding for the numerous evenings where I was physically absent, trying to finish some work and for the even more numerous times when I was physically present but absent in my thoughts, trying to solve some problem. Thanks to her I found enough sleep at night after the birth of our son Luka in April to complete this work.

This work was financially supported by ABB Switzerland, Corporate Research.
List of own publications

Several journal and conference contribution resulted as an outcome of the research in this thesis. The content of selected publications has been integrated in the text of this work. These publications are indicated with a star (*):


[P4] M. M. Walter, M. Kang, C. M. Franck, Arc cross-section determination of convection stabilized arcs, 18th symposium on high voltage engineering (ISH), Seol South Korea, August 2013.*


M. M. Walter, C. M. Franck, Flexible pulsed DC-source for investigation of HVDC circuit breaker arc resistance, XVIII International Conference on Gas Discharges and Their Applications (GD 2010), Greifswald, Germany, 5th - 10th September 2010.
The following master students have made contributions to this project in the scope of their master and semester projects.


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<th>description</th>
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<tr>
<td>$A$</td>
<td>m$^2$</td>
<td>arc cross-section</td>
</tr>
<tr>
<td>$A_E$</td>
<td>m$^2$</td>
<td>arc cross-section (determined from electrical parameters)</td>
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<tr>
<td>$A_{\text{eff}}$</td>
<td>m$^2$</td>
<td>effective cross-section of cold gas flow for a nozzle throat with arc</td>
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<td>$A_N$</td>
<td>m$^2$</td>
<td>nozzle throat cross-section</td>
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<td>$A_P$</td>
<td>m$^2$</td>
<td>arc cross-section (determined from fluid dynamic parameters)</td>
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<td>$A_{V}$</td>
<td>m$^2$</td>
<td>arc cross-section (determined from optical parameters)</td>
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<td>arc cross-section (theoretical prediction)</td>
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<td>$a_{\text{pg}}$</td>
<td></td>
<td>fit coefficient of power law for stationary arc power loss</td>
</tr>
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</table>

\[
P = 1W \cdot a_{\text{pg}} \left( \frac{g}{1 \text{ S}} \right)^{b_{\text{pg}}}
\]

| $a_{\text{pi}}$ | | fit coefficient of power law for stationary arc power loss |

\[
P = 1W \cdot a_{\text{pi}} \left( \frac{I}{1 \text{ A}} \right)^{b_{\text{pi}}}
\]

| $a_{\text{ui}}$ | | fit coefficient of power law for stationary arc power loss |

\[
U = 1V \cdot a_{\text{ui}} \left( \frac{I}{1 \text{ A}} \right)^{b_{\text{ui}}}
\]

| $b_{\text{ui}}$ | | fit coefficient of power law for stationary arc power loss |

\[
U = 1V \cdot a_{\text{ui}} \left( \frac{I}{1 \text{ A}} \right)^{b_{\text{ui}}}
\]

| $b_{\text{pg}}$ | | fit coefficient of power law for stationary arc power loss |

\[
P = 1W \cdot a_{\text{pg}} \left( \frac{g}{1 \text{ S}} \right)^{b_{\text{pg}}}
\]

| $b_{\text{pi}}$ | | fit coefficient of power law for stationary arc power loss |

\[
P = 1W \cdot a_{\text{pi}} \left( \frac{I}{1 \text{ A}} \right)^{b_{\text{pi}}}
\]
<table>
<thead>
<tr>
<th>symbol</th>
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<tr>
<td>$C$</td>
<td>F</td>
<td>capacitance</td>
</tr>
<tr>
<td>$C_p$</td>
<td>J/(m$^3$K)</td>
<td>heat capacity at constant pressure</td>
</tr>
<tr>
<td>$c$</td>
<td>$\mu$s</td>
<td>fit coefficient of power law for thermal arc inertia $\tau = c(g/(1\ S))^d$</td>
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<tr>
<td>$d$</td>
<td></td>
<td>fit coefficient of power law for thermal arc inertia $\tau = c(g/(1\ S))^d$</td>
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<tr>
<td>$d_T$</td>
<td>m</td>
<td>duty cycle</td>
</tr>
<tr>
<td>$E$</td>
<td>V/m</td>
<td>Electric field</td>
</tr>
<tr>
<td>$\partial U/\partial I$</td>
<td>V/A</td>
<td>derivative of the stationary (non time dependent) $UI$ arc characteristic after the current</td>
</tr>
<tr>
<td>$\partial u/\partial i$</td>
<td>V/A</td>
<td>derivative of the transient (time dependent) $ui$ arc characteristic after the current</td>
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<tr>
<td>$E$</td>
<td>V/m</td>
<td>electric field</td>
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<tr>
<td>$f$</td>
<td>Hz</td>
<td>frequency</td>
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<tr>
<td>$f$</td>
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<td>fraction of of power loss by radiation</td>
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<tr>
<td>$g$</td>
<td>S</td>
<td>conductance</td>
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<tr>
<td>$\dot{g}$</td>
<td>S/s</td>
<td>temporal derivative of a transient conductance signal</td>
</tr>
<tr>
<td>$h_p$</td>
<td>J/kg</td>
<td>enthalpy of plasma</td>
</tr>
<tr>
<td>$\overline{h}$</td>
<td>J/kg</td>
<td>enthalpy for wall material evaporation and acceleration to sonic velocity</td>
</tr>
<tr>
<td>$I$</td>
<td>A</td>
<td>steady state current</td>
</tr>
<tr>
<td>$I_F$</td>
<td>A</td>
<td>steady state fault current</td>
</tr>
<tr>
<td>$I_T$</td>
<td>A</td>
<td>current amplitude with minimum steady state arc voltage in the $U(I)$ arc characteristic</td>
</tr>
<tr>
<td>$i$</td>
<td>A</td>
<td>time variant current</td>
</tr>
<tr>
<td>$i_F$</td>
<td>A</td>
<td>transient fault current</td>
</tr>
<tr>
<td>$j$</td>
<td>A/m$^2$</td>
<td>current density</td>
</tr>
<tr>
<td>$L_T$</td>
<td>m</td>
<td>nozzle throat length (cylindrical section)</td>
</tr>
<tr>
<td>$\dot{m}_S$</td>
<td>kg/(m s)</td>
<td>supersonic mass flow rate</td>
</tr>
<tr>
<td>$\dot{m}_{sub}$</td>
<td>kg/(m s)</td>
<td>subsonic mass flow rate</td>
</tr>
<tr>
<td>symbol</td>
<td>unit</td>
<td>description</td>
</tr>
<tr>
<td>--------</td>
<td>------</td>
<td>-------------</td>
</tr>
<tr>
<td>$P$</td>
<td>W</td>
<td>steady state cooling power of an arc (the parameter is typically assumed to be a function of conductance)</td>
</tr>
<tr>
<td>$P_{\text{cool}}$</td>
<td>W</td>
<td>instantaneous power removed from the arc column</td>
</tr>
<tr>
<td>$P_{\text{heat}}$</td>
<td>W</td>
<td>instantaneous power added to the arc column</td>
</tr>
<tr>
<td>$P_{\text{joule}}$</td>
<td>W</td>
<td>joule heating power</td>
</tr>
<tr>
<td>$P_{\text{H}}$</td>
<td>W</td>
<td>steady state cooling power of a hypothetical arc</td>
</tr>
<tr>
<td>$p$</td>
<td>bar</td>
<td>absolute pressure</td>
</tr>
<tr>
<td>$p_B$</td>
<td>bar</td>
<td>overpressure in pressure bottle (relative to $p_R = 1013$ mbar atmospheric pressure)</td>
</tr>
<tr>
<td>$p_{\text{dn}}$</td>
<td>bar</td>
<td>absolute downstream pressure</td>
</tr>
<tr>
<td>$p_{\text{H}}$</td>
<td>bar</td>
<td>overpressure in pressure chamber (relative to $p_R = 1013$ mbar atmospheric pressure)</td>
</tr>
<tr>
<td>$p_S$</td>
<td>bar</td>
<td>absolute stagnation point pressure</td>
</tr>
<tr>
<td>$p_{\text{up}}$</td>
<td>bar</td>
<td>absolute upstream pressure</td>
</tr>
<tr>
<td>$p_R$</td>
<td>bar</td>
<td>atmospheric reference pressure in $p_R = 1013$ mbar</td>
</tr>
<tr>
<td>$Q$</td>
<td>J</td>
<td>energy content stored in an arc</td>
</tr>
<tr>
<td>$R$</td>
<td>J/(mol K)</td>
<td>universal gas constant ($R = 8.314$ J/(mol K))</td>
</tr>
<tr>
<td>$R_1$</td>
<td>$\Omega$</td>
<td>stray resistance in the main path of a passive resonant breaker</td>
</tr>
<tr>
<td>$R_2$</td>
<td>$\Omega$</td>
<td>stray resistance in the resonance path of a passive resonant breaker</td>
</tr>
<tr>
<td>$r$</td>
<td>m</td>
<td>radius of arc</td>
</tr>
<tr>
<td>$U$</td>
<td>V</td>
<td>steady state voltage</td>
</tr>
<tr>
<td>$U_T$</td>
<td>V</td>
<td>arc voltage (determined by theoretical prediction)</td>
</tr>
<tr>
<td>$U_N$</td>
<td>W/m$^2$</td>
<td>net emission of radiation emitted from the plasma</td>
</tr>
<tr>
<td>$u$</td>
<td>V</td>
<td>time variant voltage</td>
</tr>
<tr>
<td>$V_B$</td>
<td>m$^3$</td>
<td>volume of pressurized bottles</td>
</tr>
<tr>
<td>$V_H$</td>
<td>m$^3$</td>
<td>volume of a pressure chamber</td>
</tr>
<tr>
<td>$v_s$</td>
<td>m/s</td>
<td>sonic velocity</td>
</tr>
<tr>
<td>$v_z$</td>
<td>m/s</td>
<td>axial plasma velocity (along z axis)</td>
</tr>
<tr>
<td>symbol</td>
<td>unit</td>
<td>description</td>
</tr>
<tr>
<td>--------</td>
<td>----------</td>
<td>-----------------------------------------------------------------------------</td>
</tr>
<tr>
<td>$T$</td>
<td>K</td>
<td>absolute temperature</td>
</tr>
<tr>
<td>$T_H$</td>
<td>K</td>
<td>absolute temperature in a pressure chamber</td>
</tr>
<tr>
<td>$T_R$</td>
<td>$T$</td>
<td>absolute temperature ($T_0 = 293 , \text{K}$ laboratory conditions)</td>
</tr>
<tr>
<td>$t$</td>
<td>ms</td>
<td>time</td>
</tr>
<tr>
<td>$L$</td>
<td>H</td>
<td>inductance</td>
</tr>
<tr>
<td>$X_{in}$</td>
<td>m</td>
<td>distance between upstream electrode and nozzle throat inlet</td>
</tr>
<tr>
<td>$X_{out}$</td>
<td>m</td>
<td>distance between nozzle throat outlet and downstream electrode</td>
</tr>
<tr>
<td>$x$</td>
<td></td>
<td>normalized radius</td>
</tr>
<tr>
<td>$z$</td>
<td>m</td>
<td>axial distance measured from upstream electrode</td>
</tr>
<tr>
<td>$\gamma$</td>
<td></td>
<td>ratio of specific heats (adiabatic index) (for air $\gamma = 1.4$)</td>
</tr>
<tr>
<td>$\theta$</td>
<td>s</td>
<td>thermal inertia of a single physical process in the arc column</td>
</tr>
<tr>
<td>$\lambda$</td>
<td>W/(mK)</td>
<td>thermal conductivity</td>
</tr>
<tr>
<td>$\rho$</td>
<td>kg/m$^3$</td>
<td>gas density</td>
</tr>
<tr>
<td>$\rho_p$</td>
<td>kg/m$^3$</td>
<td>gas or plasma density</td>
</tr>
<tr>
<td>$\sigma$</td>
<td>S/m</td>
<td>electric conductivity</td>
</tr>
<tr>
<td>$\tau$</td>
<td>s</td>
<td>thermal arc inertia (parameter is typically assumed to be a function of conductance)</td>
</tr>
<tr>
<td>$\tau_H$</td>
<td>s</td>
<td>thermal inertia of a hypothetical arc</td>
</tr>
<tr>
<td>$\xi$</td>
<td>J</td>
<td>heat flux potential</td>
</tr>
</tbody>
</table>
1. Problem statement

The considerable growth of energy consumption and the replacement of traditional band power (i.e. coal, gas and nuclear) with fluctuating renewable power (i.e. solar thermal, photovoltaic and wind) demands major extension of today’s transmission networks. In long distance energy transmission and connection of off-shore power, HVDC transmission is superior to HVAC. This is so because DC transmission losses are significantly lower for cables with a length exceeding 50 km and for overhead transmission lines with a length exceeding 700 km. Limitations in HVAC transmission length exist due to the massive consumption of reactive power in AC cables and transmission lines. Meshed, VSC based HVDC networks, overlayed to today’s HVAC grid, are on a good way to become the backbone in a future European wide Supergrid [BF13a, FW13, CIG25, CCE11].

Among several unsolved problems to enable such a Supergrid, grid protection in multi-terminal HVDC networks is one of the main obstacles [BF13a]. Arc based circuit breakers are standard measures for fault current interruption in HVAC. HVDC circuit breakers have been proposed and developed as passive resonance breakers, and hybrid breakers with limited interruptible currents (< 4 kA) [NNH+01, PMR+88, HJ11], but yet no breaker technology became established as circuit breaker, and performance increase would be desirable.

This thesis focuses on passive resonance HVDC circuit breakers and HVDC load switches. Breakers without semiconductor switches but only metal contacts have significantly lower losses in the closed state and tend to be significantly cheaper. The motivation of this thesis is to show ways to increase interruptible currents, decrease the time required for current zero creation and reduce the component
costs of such passive resonance breakers. By adapting the breaker chamber and nozzle geometry, the switching arc characteristic can be positively influenced. In this way an improved dynamic arc-network interaction and more efficient creation of current zero crossings is achievable. Precondition for this is an accurate knowledge of the switching arc characteristic, which is the main subject of this thesis.
2. Background and motivation

In this chapter, the candidate technologies for HVDC circuit breakers are set in relation to the expected fault currents in future meshed multi-terminal HVDC networks. Section 2.1 illustrates the necessity of transmission capacity extension as a consequence of the increasing share of renewables in the power mix. The available technologies for multi-terminal HVDC transmission and their advantages over HVAC are summarized in Section 2.2. Section 2.3 illustrates the expected fault currents, which an HVDC circuit breaker must be capable to interrupt. Candidate technologies for HVDC circuit breakers and their limitations are discussed in Section 2.4. Strategies for fault handling in HVDC grids, arising from the expected breaker performance and the expected fault currents, are discussed in Section 2.5.

2.1. Evolution of today’s transmission grid

On the way towards a low carbon, low nuclear society in Europe, a trend towards concentrated renewable power production is observable. These are mainly off-shore and on shore wind parks [KXLS02, LO03, MHPD07] and solar thermal power stations [Des13]. In contrast to fossil and nuclear power plants, the location of such power stations is less freely selectable. Wind power stations require continuous and stable wind conditions, which are found mainly off-shore along the coast in North Europe [HG06]. Solar thermal power stations require locations with high number of solar hours and mainly direct radiation and are thus placed preferentially in South Europe and North Africa. Such trends are not limited to Europe but arise world wide. Therefore, also intercontinental power transmission has
been envisioned (e.g. Pan-European Supergrid [FW13], Desertec Project [Des13], China-Europe transmission highway [ZoC26]). The following challenges arise in the future for the transmission network:

- Energy must be transmitted from the infeeding plants towards the large load centers in central Europe over long distance. (on-shore > 1000 km, off-shore > 50 km).

- Fossil and nuclear band energy is substituted with fluctuating wind and solar power. As a consequence, continuously large amounts of power must be transmitted across the network to compensate a local lack or excess of energy.

- The emerging Europe wide market for production and storage of energy, as a measure to boost renewables, leads to significantly more energy flow across country borders. (From the market perspective, Europe would preferably be a so called "copper plate" with unlimited transmission capacity.)

- Electrification of fossil consumers, as in housing and mobility, is expected to increase the consumption of electric energy in the future.

In today’s network, sufficient transmission capacity is not yet provided to meet these requirements [BF13a], and major infrastructure extensions must go hand in hand with the shift towards renewable energy. A strong transmission grid will be required to compensate stochastics in generation and to break peaks in the load. Wind energy from North Europe could be balanced with wind from Spain or solar thermal energy from the Sahara desert [HG06]. Strong East-West connections would allow balancing out loads from different time zones [HG06]. Recent trends for grid extension include:

- AC transmission at UHV voltage levels > 1 MV (cf. India [RRU07], China [ZoC26]).
• Small VSC-HVDC networks with a few terminals to connect off-shore wind power to an existing on-shore AC network [KXLS02, LO03, MHPD07].

• A Europe-wide large scale multi-terminal HVDC network is under discussion to form the future back-bone of Europe’s transmission grid [FW13, Des13].

The increase of AC voltage level is capable to reduce transmission losses and to enable AC transmission over longer distances. However, limits in the achievable voltage levels are given by the transmission tower size, as a consequence of the required insulation distance, and due to the limited availability of UHV AC components. Long distance AC power transmission requires reactive power compensation at regular intervals along the lines. In particular, in undersea cables this is difficult to provide. Long distance HVDC transmission has even at moderate voltage levels (≤ 550 kV) substantially smaller losses than in HVAC and does not require reactive power compensation. For long transmission distances, the reduced losses in HVDC clearly outweigh the 1–2% additional conversion losses in the AC/DC- terminals [HG06, Fra11]. The break even point, where the higher investment costs for AC/DC conversion outbalance accumulated costs of transmission losses, lies for overhead lines at ~ 800 km, and for cables at ~ 40 km [HG06] (cf. figure 2.1). The shorter distances for cables result from their higher capacitance in comparison to overheadlines. The reason for that is firstly because the conductors are closer together [BWPF12, Fra11] and secondly because the inter-line capacitance is much higher due to higher permittivity of the dielectric compared to air.

2.2. Meshed multi-terminal HVDC networks

HVDC connections have already been in operation for over 50 years. Applications are: a) back-to-back couplings of asynchronous AC
Figure 2.1.: Cost break-down for AC and DC systems with line length (copied from [HG06])
2.2 Meshed multi-terminal HVDC networks

Figure 2.2.: Reduction of required terminals for multi-terminal HVDC: Four-terminal point-to-point network (left) and meshed network (right). White squares are AC/DC terminals, black squares are HVDC circuit breakers. (copied from [Fra11])

networks, b) long distance point-to-point connections and c) undersea connections [HG06]. Multi-terminal HVDC grids have been proposed, but so far only three-terminal networks have been put into operation [BTAM13, MMP+94]. Meshed multi-terminal HVDC grids, in comparison to point-to-point connections, would bring major advantages:

- Power transmission can continue even if one interconnecting line is out of service [CJ97].

- AC/DC converter stations are a major contribution to the costs of HVDC connections. Figure 2.2 illustrates that in a meshed network with 4 nodes, the number of required converter terminals can be reduced by a factor of 3 in comparison to an identical point-to-point network. In the meshed network, fault protection is carried out by HVDC circuit breakers.

Two different technologies for HVDC transmission with different protection schemes exist. These are the thyristor based CSC (Current Sourced Converter) systems and IGBT based VSC (Voltage
Sourced Converter) systems [AXW02, CSC11]. CSC technology has been available for several decades. Therefore, most back-to-back and point-to-point connections have been implemented with CSC terminals [Fra11]. VSC systems became popular to connect off-shore wind farms in the last decade [BWPF12]. The technology has been enabled only recently by significant progress in solid state components (IGBTs).

The performance of VSC-HVDC and CSC-HVDC systems has been compared with the following results [Fra11]:

**Availability:** Today, CSC systems up to 6400 MW (800 kV, 4 kA) exist [CSC11]. VSC based HVDC systems have been realized with a rating of 150 kV, 400 MW, and a 300 kV, 800 MW system is in the planning phase. "The VSC-technology is, in principle, available for higher powers today, limited mainly by the voltage constraints of XLPE DC cables" [Fra11].

**Losses:** Today, VSC converters have 1.7% loss per station. CSC converters have 0.7 – 0.8% loss per station, which is only half the loss of VSC converters [HG06]. Substantial reduction of losses is expected in future multi-level VSC terminals.

**Harmonics:** CSC have intense low-order harmonics because thyristors are turn-on-only devices. With the IGBTs in VSC, much weaker high frequency components are achievable and smaller filters are required.

**Reactive Power:** CSC consume high amounts of reactive power. In contrast to that, VSC terminals have controllable consumption or production of reactive power. They may even support the stability of a surrounding AC network by infeeding reactive power.

**Current reversal:** In CSC networks, power flow reversal requires a change of polarity. In VSC networks, this is achieved simply by adapting the PWM sequence.

**Controllability:** In multi-terminal CSC networks, typically only one terminal is voltage controlled and sets the system voltage whereas all others are current controlled. In VSC-networks each terminal controls its own power flow individually. Thus, network control in
2.2 Meshed multi-terminal HVDC networks

VSC networks is superior to that in CSC networks.

Island Networks: In contrast to CSC converters, VSC converters have black start capability. Therefore, they are also suitable for connection of weak island grids and off-shore wind parks [HG06].

Different strategies in fault protection result from the principal differences of CSC and VSC converters: CSC systems are typically equipped with large DC side reactors [CSC11]. In the case of a fault they limit the maximum fault current gradient and lead to relatively moderate fault currents. Therefore, CSC systems could be operated without dedicated HVDC circuit breakers [Fra11]. The strategy of fault handling involves a complete discharge of the DC network in combination with either a blocking of all CSC terminals, or opening of the AC side circuit breakers closest to each terminal [HG06, CJ97]. The fault interruption process, including the subsequent restart of the DC system, typically requires > 200 ms [Fra11]. CSC terminals stop firing the thyristors in the fault case and prevent thereby that additional energy is fed from the AC network into the DC fault.

VSC terminals require capacitive filters on the DC side and are equipped with freewheeling diodes [AXW02]. Two negative consequences arise from that: Firstly, in the fault case these capacitances are discharged into the fault and contribute significantly to the amplitude and rate of rise of the initial inrush fault current [BWPF12]. Secondly, the freewheeling diodes conduct AC power uncontrollably into the DC fault, once the DC filter capacitor voltage has decreased below a critical level [Fra11]. Such an AC infeed could theoretically be prevented by operating the circuit breakers on the AC side [BWPF12].

There is agreement in the community that in meshed multi-terminal systems, a complete discharge of the DC network in the fault case is not acceptable, if the system exceeds a number of 3 – 5 terminals [HG06, Fra11]. An outage of several gigawatts for times of more than 100 ms from the whole DC network would destabilize even a strong surrounding AC network significantly. Even more critical for the AC grid could be the lack of stabilizing reactive power for the
AC grid, provided by the VSC terminals.

As a consequence, a large scale multi-terminal HVDC network, in particular in VSC technology, cannot be operated without additional protective elements on the DC side. Despite that, a ”Supergrid” will much more likely be based on VSC rather than on CSC, mainly due to the better power flow controllability [HG06, Fra11, JHL+11]. HVDC circuit breakers are therefore often named as a key enabling technology for multi-terminal HVDC [Fra11, HJ11].

2.3. Short circuit development in VSC-HVDC networks

Two types of faults are considered to be relevant for dimensioning of HVDC protection equipment; pole-to-pole faults and pole-to-ground faults. Pole-to-pole faults are considered to be more severe than pole-to-ground faults, but, the latter are expected to occur more frequently [YFO12, BWPF12].

Pole-to-pole faults have been modeled in the scope of the TWENTIES project for three different case studies with meshed HVDC grids of four and five VSC terminals. The simulations in Matlab and EMTP predicted fault currents for faults at three different grid locations [JHL+11]:

**Peripheral line fault:** The fault occurred on a line connecting a single peripheral terminal to a larger grid. A peak fault current of 5 kA after 15 ms resulted.

**Bus fault (weakly meshed):** The fault occurred on a line near a bus with two lines connected. A peak of 50 kA after 10 ms was predicted.

**Bus fault (strongly meshed):** The fault occurred on a line near a bus with three connected lines. A peak of 65 kA after 2 ms was predicted.

Pole-to-ground faults in overhead line grids and cable grids have been investigated for a three terminal HVDC grid by simulations with the program PSCAD [BF13a, BWPF12]. The identical grid
Figure 2.3.: Comparison of pole-to-ground fault current in overheadlines (a) and cables (b) and d)) for steady state conditions and for the initial 10 ms: Fault current contributions: (A) DC capacitor, (B) adjacent 300 km feeder cable/line, (C) AC infeed from 300 km distant terminal, (D) AC infeed from close terminal, solid line: total CB current (adapted from [BF13b])
structure was modeled with lines and with cables. One terminal is connected directly to a common bus, two terminals are connected via a 300 km line/cable to the same bus. A fault was assumed close to the bus, on one of the connecting lines/cables. Figure 2.3 shows the resulting fault current in the system, including a zoom of the first $\sim 10$ ms. The total fault current is separated into its shares that originate from different grid components. Critical components that dominate the fault current are thereby identified and this serves to improve the understanding of how fault currents evolve in VSC-HVDC grids [BF13a, BWPF12]:

- The steady state fault current is dominated by the AC infeed over the freewheeling diodes of blocked VSC terminals. It increases with the number of terminals connected to a bus, and decreases with the length of the line/cable over which the terminals are connected to the bus.

- In the system with overhead lines, the initial slope was very high and the first peak of the fault current occurred already at a time $< 2$ ms. It is dominated by the discharge of the filter capacitor. The current increases with the filter capacitor size and with the number of terminals that are directly connected to the bus [BF13a].

- In the system with cable connections, the cable capacitance discharge contributes significantly to the fault current peak, as well. However, its share reaches the circuit breaker location typically with a small delay (eg. $1 - 2$ ms), caused by the traveling time of the voltage wavefronts along the line. Thereby, a second peak may result that exceeds the amplitude of the first one.

The simulation results have the following consequences for protection equipment in future HVDC grids: the time to interruption or the time to current limitation will be a crucial parameter for the choice of circuit breakers in overhead line and cable networks. In case
of cable networks, the peak fault current may already be reached at a time $< 2\text{ ms}$. In case of overhead line networks, a slow breaker that requires $10\text{ ms}$ for interruption, may need to interrupt twice the current amplitude of a fast breaker operating within $\sim 5\text{ ms}$.

### 2.4. HVDC circuit breakers

#### 2.4.1. Current interruption at high voltage levels

Breakers in today’s AC networks are classified according to the stresses they must withstand during closing and opening operation.

- Disconnector switches can interrupt only small currents, or currents at negligible voltage differences, such as remaining capacitive charging currents of open lines of commutation currents in substations.

- Load switches can interrupt all currents occurring in normal operating conditions.

- Circuit breakers can interrupt any currents that occur as a consequence of a fault in the network. Their interruption is sufficiently fast to protect the surrounding network components from overcurrents exceeding their rating.

An optimal circuit breaker would provide the following performance:

- **in closed state:** Conduct nominal currents including possible transient overcurrents with minimum ohmic losses

- **in open state:** Withstand any occurring stationary voltage and transient overvoltages

- **opening operation:** Interrupt all possible fault currents as fast as possible but without creating excessive transient overvoltages, which would exceed the insulation limits of surrounding network components

- **closing operation:** Establish a low resistive connection as fast as possible and bear possible inrush currents, if a voltage difference is present between the breaker contacts at the instant of closing.
HVAC circuit breakers are available for nominal voltages exceeding 1000 kV and can interrupt several ten kilo-amperes. They exist as self-blast breakers, puffer breakers and air blast breakers [Duf09, Bla43, BKM+70, Küc05]. At mechanical separation of two contacts inside a gas chamber, they draw an arc. For extinction of this arc, the breakers create a supersonic axial gas flow guided by a nozzle, which cools the arc by continuously removing power $P_{\text{cool}}$. In such breakers, arc extinction always occurs at the natural current zero crossing of the sinusoidal current. This is because only at low currents, the heating power supplied electrically $P_{\text{heat}} = u \cdot i$ decreases below the cooling power $P_{\text{cool}}$. An arc can also be extinguished if the breaker produces a forward voltage higher than the driving source voltage. But this option is restricted to low voltage applications, because sufficiently high arc voltages are not achievable.

A current zero crossing, mandatory for arc extinction, is missing in DC. Therefore, HVDC interruption is considerably more challenging than HVAC interruption. For HVDC current interruption a breaker must fulfill the following three duties [Fra11]: a) current zero creation to interrupt the current, b) dissipate the inductive energy stored in the system, c) withstand the transient recovery voltage after current interruption.

In today’s HVDC systems, the following breakers exist: neutral bus switch (NBS), neutral bus ground switch (NBGS), metal return transfer breaker (MRTB) and high-speed bypass switch (HSBS) for parallel line switching [Fra11]. Circuit breakers have been realized for HVDC only in very limited ratings (250 kV, 8 kA [TAYY85] or 500 kV, 4 kA [PMR+88]). They do not exist with interruption performance sufficient for VSC systems.

### 2.4.2. Candidate HVDC circuit breaker technologies

In the following, candidate technologies for HVDC circuit breakers are compared and discussed. Selected breakers are compared in detail in Table 2.1 with respect to their total interruption time, on-state losses, and state of development.
Proposed breakers can be classified as *mechanical breakers*, *solid state breakers* and *hybrid breakers*, combining both technologies. It is further distinguished between *hybrid mechanical breakers*, where the main path is implemented with mechanical contacts, and *hybrid solid state breakers* that include solid state elements in the main path.

**Full solid state CB**

Topologies for full solid state breakers are typically based on a certain number of GCTs, GTOs or IGBTs connected in series [MSDD04], [TO02]. Their reaction times are extremely short, which makes them ideal as DC circuit breakers. Drawbacks are mainly the substantial on-state losses (especially for IGBTs) and the high component costs. This inhibits full solid state breakers from being utilized in large numbers. So far, only applications in AC-networks and medium voltage DC applications have been proposed [MSDD04, BM07]. HVDC multi-terminal network applications, where the time to interruption or the time to current limitation is a crucial factor, should be considered. Advances in semiconductor device technology, such as higher blocking voltages, lower forward losses or even new materials may be a key promoter for the full solid state CBs.

**Hybrid solid state CB**

A recently developed hybrid solid-state CB comprises a current interruption and a current conduction path (cf. figure 2.4e)). It consists of a fast, but small solid state switch in series with a fast metal contact disconnector in the main path [HJ11]. The actual breaker is located in a parallel path and consists of a number of series connected solid state switches. The small IGBT in the main path needs only to create a sufficiently high voltage for the commutation of the current to the parallel full IGBT breaker. The main path requires, therefore, fewer modules in series and, thus, features a smaller forward voltage and lower on-state losses compared to the full IGBT breaker. The
Figure 2.4.: HVDC circuit breaker candidates: a) Passive resonance breaker [BMRL85], b) active resonance breaker [BKM+70], c) fast mechanical switch with IGBT commutation path, d) vacuum circuit breaker with thyristor current injection unit [Mar11], e) hybrid IGBT and disconnector breaker with IGBT commutation path [HJ11], f) IGBT fault current reducing unit with mechanical resonance breaker [OPH+12], g) full solid state breaker
### Table 2.1.: Performance comparison of HVDC circuit breaker candidates

<table>
<thead>
<tr>
<th></th>
<th>Full solid state CB (2.4 g)</th>
<th>Hybrid solid state CB (2.4 e)</th>
<th>Hybrid mechanical CB (2.4 c, 2.4 d)</th>
<th>Mechanical passive or active resonance CB (2.4 a, 2.4 b)</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Expected total interruption time</strong></td>
<td>&lt; 1 ms</td>
<td>&lt; 2 ms</td>
<td>5 – 30 ms</td>
<td>&lt; 60 ms</td>
</tr>
<tr>
<td><strong>Required times for commutation / energy absorption</strong></td>
<td>commutation &lt; 0.1 ms energy absorption ~ 1 ms</td>
<td>commutation &lt; 0.2 ms disconnector opening &lt; 1 ms energy absorption ~ 1 ms</td>
<td>contact separation conventional AC breakers ~ 20 ms contact separation magnetically driven UFS (Ultra-Fast-Switch) with opening speed &gt; 20 m/s; ~ 1.5 ms</td>
<td>contact separation ~ 20 ms current zero creation passive resonance ~ 30 ms current zero creation active resonance ~ 2 ms</td>
</tr>
<tr>
<td><strong>Current state of development</strong></td>
<td>- not yet built for HVDC - development of VSC and CSC boosts technology as components are alike</td>
<td>- working principle proven - type test and interruption test with downscaled breaker passed</td>
<td>- not yet available - slow AC breakers available - UFS not yet available</td>
<td>- applied in CSC HVDC also used as MRTB (Metal-Return-Transfer-Breaker)</td>
</tr>
<tr>
<td><strong>Maximum rated voltage $U_n$</strong></td>
<td>&lt; 800 kV (same as the voltage level)</td>
<td>120 kV verified by test (up to 320 kV achievable)</td>
<td>- AC circuit breakers &gt; 500 kV - UFS &lt; 12 kV</td>
<td>&lt; 550 kV available</td>
</tr>
<tr>
<td><strong>Max. DC breaking current</strong></td>
<td>&lt; 5 kA expected</td>
<td>9 kA experimentally proven (up to 16 kA expected)</td>
<td>~ 6 – 12 kA (estimated)</td>
<td>- passive: up to 4 kA in operation active: up to 8 kA possible -able to survive transient overcurrents</td>
</tr>
<tr>
<td><strong>Expected power loss in comparison to a VSC converter station</strong></td>
<td>30% (high forward voltage due to serial connection of solid state devices)</td>
<td>&lt; 1% (only few IGBTs in series in the main path)</td>
<td>&lt; 0.01% (metal contacts)</td>
<td>&lt; 0.01% (metal contacts)</td>
</tr>
<tr>
<td><strong>Further development steps</strong></td>
<td>- development in solid state device technology to reduce on-state forward voltage or number of modules in series</td>
<td>- field experience with prototype in test grid - reduction of IGBT costs</td>
<td>-development of ultra-fast mechanical drives to reduce commutation time</td>
<td>- optimizing DC arc chamber for passive resonance to achieve higher current rating, minimize capacitor size and minimize time for current zero creation</td>
</tr>
</tbody>
</table>
disadvantage of this arrangement is the increased interruption time due to the required opening time of the mechanical disconnector. To gain sufficient time, the inventors proposed to install a 100 mH coil in series to the breaker that limits the fault current rise rate to $< 3.5 \text{kA}/\text{ms}$ [HJ11]. The concept is very attractive, but the costs of the IGBT modules remains the same.

An alternative concept, the "IGBT fault current reducing unit with mechanical resonance breaker", has been proposed recently [OPH+12]. It consists of a resonance breaker, an IGBT voltage limiter and an inductor in series (cf. figure 2.3f). The IGBT unit reduces the fault current but its purpose is not to interrupt. This allows the use of fewer IGBT elements and thereby drastically reduces the forward losses. The residual current (expected to be $< 4 \text{kA}$) is interrupted by a passive resonance breaker. The fault current limiting unit provides sufficient time for the resonance breaker to operate, because it brings back the current to interruptible current amplitudes. In comparison to the above discussed hybrid breaker, no full IGBT commutation path is required.

Hybrid mechanical CB

Hybrid mechanical breakers combine the low forward losses of a pure (fast) mechanical breaker and the fast performance of a solid state breaker in the commutation path [MSDD04]. They are faster than common mechanical breakers, as the arc chamber must only create sufficient voltage for commutation, but no artificial current zero crossing (cf. figure 2.4c)). They will only be superior to hybrid solid state breakers, if the contact separation speed and, thus, the build-up of arc voltage for commutation can be significantly increased or if measures in the grid allow interruption times $> 20 \text{ms}$. So far, ultrafast switches have been designed and tested only for MV levels [SFHK03, HF02].

A recently proposed concept is the "Hybrid mechanical breaker with solid state current injection" (cf. figure 2.4d)) [Mar11]. It combines two mirror-like arranged hybrid breakers in series with a
solid state current injector unit in between. Each hybrid breaker can interrupt only one current direction and consists of a vacuum interruption chamber with a parallel diode. First, the vacuum breakers are activated. When they are in a state able to interrupt, the thyristor in the injection path is operated. At closing of the thyristor, the current commutates into the current injection unit, via ground and a diode path back to the conductor. It creates a current zero in the vacuum breaker, at which the arc extinguishes. A capacitor in parallel to the thyristor and an inductor in series to the latter form an active resonance circuit. It interrupts the ground connection, once the vacuum breaker has interrupted. The energy absorption is performed in the diode path between ground and conductor. In addition to the diode, it includes a resistor in series plus an overvoltage limiter.

The breaker has minor on-state losses, as no solid state elements are placed in the main path. No details have been published on interruption time or on component size. However, the breaker is expected to achieve interruption times in the order of active resonance HVDC circuit breakers. In comparison to the latter, repetitive operations are also expected to be possible, because no separate charging unit is required, as the remaining fault current recharges the capacitor automatically.

**Mechanical passive or active resonance CB**

Mechanical passive or active resonance breakers have been developed for CSC HVDC systems and are based on AC gas circuit breakers. An additional LC-commutation circuit is placed in parallel to the CB. This enables a current oscillation between the two parallel paths and may create an artificial current zero crossing in the main path at which the CB can interrupt \cite{PMR88,TAYY85,Fra11}. The oscillation can be achieved by an active current injection from a pre-charged capacitor or excited passively by the arc. Yet, no technical solution has been found to increase the maximum interruptible current for passive resonance breakers to the desire level. This is a
consequence of the stationary arc characteristic, in which at high currents the voltage increases with an increasing current.

Active resonance breakers create the current zero with pre-charged capacitors and are, therefore, not bound to this limit. However, considerable capacitor size (especially at high voltage levels) results, an additional charging unit is required and no open-close-open switching operations are possible.

The low costs and low on-state losses of mechanical breakers would allow them to be installed in large numbers. Due to their long interruption times, they are only effective in combination with fault limiting devices or in combination with faster breakers at critical locations. They should certainly be considered as DC load break switches, because interruption time is there of minor importance.

2.5. Options for fault clearing in multi-terminal HVDC grids

So far, no standard has been established on rates of rise and peak currents for which the breakers must be designed [HG06, CCE11]. The fault current propagation is varying significantly with the chosen network topology, fault location, VSC filter size, number and length of lines connected to a bus, and whether cables or overhead lines are used. As a consequence of propagation and reflection of waves traveling along the line, even minor changes in the network may have significant effect on the fault current. Thus, requirements for HVDC circuit breakers will vary with network topology and breaker location.

In cable systems, the discharge of distributed cable capacitances and lumped VSC filter capacitances creates high inrush currents, in particular in the first 2 ms after fault occurrence. Besides a full solid state breaker, no technology is expected to interrupt sufficiently fast to handle such a fault without additional measures. Measures to ease circuit breaker requirements by grid adoptions have been demonstrated [BWPF12]:
• Reduction of the filter capacitor size (ideally below $7 \mu F$ [BF13a]).

• Choice of different converter technology. Full bridge converters (e.g., Modular-Multi-level Converters [MGM+10] or two-level full bridge converters) have been proposed, which require smaller DC side filter capacitors and do not conduct uncontrolled AC power into a DC fault [BWPF12].

• Pole reactors in series to each breaker. An inductor of 100 mH is sufficient to reduce the fault current slope in a 300 kV system to below 3.5 kA/ms, so that a hybrid breaker has sufficient time for commutation [HJ11].

• Use of DC side fault current limiters. This may give slower breakers sufficient time for interruption.

• Selective placing of high performance CBs at strategic grid locations. Slower breakers may be sufficient at peripheral locations.

• Splitting the network into sub-networks so that the faulty sub-network can be de-energized by converter control or operation of AC side circuit breakers.

In overhead line systems the line capacitance is much smaller than with cables. Therefore, fault currents do not rise instantaneously but gradually with rates of rise $< 2$ kA/ms if filter capacitors are kept small. In such systems, arc based circuit breakers could also be an option if their interruption time can be reduced significantly and their interruptible currents can be increased.

The author expects that the first generation of breakers will be hybrid ones, in particular for off-shore grids, because they are sufficiently fast and have acceptable on-state losses. It has become accepted that the costs of such breakers will be considerably higher than comparable components in AC systems [JHL+11, Fra11]. Additional support by fault current limiters will be needed. However, if circuit breakers will be required at both ends of a line and for each
line, as in AC, the breaker costs will play an important role when choosing the technology. In particular for overhead line systems, fast high performance breakers may be placed only selectively at strategic locations. Such locations could be busses, where sub-grids or several lines are interconnected [BWPF12]. Arc based breakers could be placed in a large number at less critical locations. In addition to that, resonance based breakers can also serve as load switches in such networks or they could be an integral part of hybrid breakers (cf. figure 2.4f).

2.6. Conclusion

Long distance overhead line transmission and off-shore cable transmission for integration of fluctuating renewable power is preferentially implemented in HVDC due to its low transmission losses. HVDC circuit breakers have been identified as key enabling components for a future large-scale multi-terminal HVDC network. Among the different candidate technologies for HVDC circuit breakers, so far none meets all requirements for fault protection in such a network. Passive and active resonance breakers are advantageous, as they are a comparably low priced technology that can be installed in large numbers in the grid. Breakers of this technology in operation consist of a traditional AC circuit breaker with an LC-commutation path in series and a surge arrester for overvoltage limitation. Presently, they are limited in maximum interruptible current (< 4 kA), need long times (> 30 ms) for current interruption and have comparably large capacitors that contribute significantly to the breaker costs.
3. Aim of this work

The objective of this work is a systematic and accurate determination of switching arc parameters in passive resonance HVDC circuit breakers with respect to different design parameters. The motivation of this goal is to enable an optimized design of an arc chamber for efficient creation of current zero crossings in the arc current, excited by passive resonance. The mechanisms of arc extinction at current zero are well known from decades of AC circuit breaker research and are not in the focus of this thesis. The chosen approach divides the goal into three main research problems:

- By black-box modeling of a passive resonant HVDC-MRTB, the optimal shape of the arc characteristic (power loss $P(g)$ and time of thermal inertia $\tau(g)$) in relation to the resonance circuit parameters are identified.

- A novel, efficient and precise arc parameter determination method, based on a novel arbitrary current source, is developed to enable accurate comparison of different arc chamber designs.

- In a large experimental project, the effect of physical arc chamber modifications (nozzle geometry, blow gas pressure, type of gas and nozzle material) on the arc characteristics (i.e. black box parameters) are determined.

Black-box parameters are an accurate measure to predict how efficiently an arc chamber interacts with the resonant path and creates a current zero crossing. A systematic arc characterization provides the understanding, how specific modifications of the arc chamber influence the arc characteristic. Thereby, a method is provided to
rate how efficiently an arc chamber modification supports passive resonance.

The understanding gained of the influence of the arc chamber design on the arc characteristic can be applied to improve an existing MRTB in two ways: a) in a way that the maximum current is increased under which an unstable resonance is excited, b) in a way that a current zero crossing is reached faster for a given maximum current or c) in a way the current zero crossing for a given maximum current is reached with a minimum size of external components (L and C). Directions a) and b) would lead to HVDC circuit breakers. For them short interruption times and large interruption currents are critical. Direction c) would lead to MRTBs or HVAC load switches. It could drastically reduce the costs of such breakers.
4. Switching arcs

This chapter gives a theoretical introduction to the present knowledge in characterizing switching arcs. An introduction to cooling mechanisms and chamber-arc interaction is given in section 4.1. Simple mathematical models for nozzle-constricted arcs under forced convection are introduced in section 4.2. In section 4.3 phenomenological black-box models are discussed. Section 4.4 reviews classical methods for experimental determination of black-box arc characteristics.

4.1. Introduction

4.1.1. Classes of arc models

Processes affecting an arc are very complex and can vary strongly in their behavior as a consequence of the interaction with the environment. Electric, magnetic, chemic, thermodynamic and fluidodynamic processes simultaneously affect the plasma column. From the application perspective for HVAC and HVDC circuit breakers, mainly the externally measurable parameters current $i$, voltage $u$ and conductance $g$ are of interest. The transient interaction of the arc with a surrounding network during the high current phase and the extinction process as a consequence of intensive cooling at low currents must be understood sufficiently to predict the breaker performance.

An arc can be completely described by seven coupled differential equations. These are [Rie67]: The coupling between voltage and electric field, the Poisson equation, the charge carrier balance, the electron current density, the ion current density, the energy balance
and the Saha equation. The coupled state variables voltage $u$, electric field $E$, number of electrons $N_e$, number of ions $N_i$, electron current density $j_e$, ion current density $j_i$ and temperature $T$ vary in time and space. For known start conditions, exactly defined geometry and known material parameters as functions of temperature, the problem is theoretically solvable [Rie67]. However, under realistic conditions a closed solution is almost never found. Limitations result of mathematical reasons from of complex boundary conditions, imprecisely known material coefficients and unknown start conditions. Therefore, in almost half a century of research on arcs, a large number of strategies and models to predict the behavior of specific arcs have evolved. The following three model classes are commonly used:

- **Simple mathematical models** limit the arc description to a certain type of arc under specific conditions [LL75, Low79, Low84, SNSD06]. Often a single, or only a few processes can be identified that dominate the arc behavior. These processes are precisely calculated, all others are neglected without significant loss of accuracy. This approach requires only moderate calculation effort.

- **Phenomenological models**, also named black-box models describe the arc by externally measurable parameters only. Typically, a simple differential equation is used whose parameters have to be experimentally determined for each arc chamber [May43, Cas39, Sch72, SK00]. By parameter fitting, black-box models achieve excellent transient accuracy and reflect correctly the dynamic interaction between an arc and its surrounding network by a two-port. In this case, the actual physical processes, which are represented via the experimentally determined arc parameter functions, are not of interest.

- **Computational Fluid Dynamics (CFD)** simulations have emerged strongly in the last decade. They solve the governing equations
numerically for a given complex geometry and include the thermodynamic flows of the cold gas. Certain processes are also neglected here, or the simulation is restricted to a thermodynamic equilibrium [SNSD06, IF08]. In such simulations the effect of geometrical variations on different processes in the arc can be visualized and thereby they can be studied easier.

### 4.1.2. Energy transfer mechanisms

In AC and DC circuit breakers, the energy required for creating and maintaining the arc plasma is provided by Joule heating from the current flow. A current density $j$ in a material with conductivity $\sigma$ creates ohmic losses $P_{\text{joule}}$ per volume $V$

$$\frac{P_{\text{joule}}}{V} = \frac{j^2}{\sigma}.$$  \hspace{1cm} (4.1)

At low currents, this heating energy has its minimum. Therefore, in HVAC circuit breakers the arc is interrupted always near current zero. HVDC circuit breakers must create such a current zero artificially.

Several cooling mechanisms are present in the arc. These include: radiation, laminar and turbulent convection and conduction. Coupled via the energy balance equation they are balanced out by the ohmic heating. At a sudden increase of current amplitude, the ohmic heating becomes larger than the arc cooling, so that an overshoot in energy results. This overshoot typically increases the plasma temperature and it may lead to an increased arc cross-section. The increased temperature compensates for the ohmic heating by increasing the plasma conductivity. This effect is dominant at low current amplitudes. The increased cross-section compensates for the ohmic heating by reducing the total arc resistance. The effect is dominant at high current amplitudes. Both effects also influence the various cooling mechanisms, so that typically a new steady state is found at increased arc temperature and arc cross-section.
Emission and absorption of radiation are an important energy transfer mechanisms inside the arc plasma [LL75]. In cylindrical arc columns, the bulk part of emitted radiation is reabsorbed by cooler outer layers of the plasma column and thus flattens the radial temperature profile [LL75]. In free-burning arcs, a large share of radiation is lost into the surrounding space. In wall stabilized arcs, the radiation is absorbed by the wall material and causes ablation.

**Forced convection**

Convective heat transfer is an important mechanism to remove enthalpy out of a nozzle. Natural convection occurs in vertical and horizontal free-burning arcs by a natural drift of heated gas. A convective flow also occurs in narrow nozzles caused by a pressure gradient created by wall material ablation. Forced convection by an axial flow of cold gas is used in AC and DC circuit breakers to significantly increase arc cooling. The involved mechanisms for turbulent and laminar energy transfer between the plasma and its surrounding cold gas are very complex and not yet fully understood. Nozzle constricted arcs under forced convection are further discussed in section 4.2.

**Ablation**

Ablation results from the interaction of an arc with its surrounding wall. Material evaporates from the nozzle surface and heats up by absorbtion of radiation. Thereby, it contaminates or even displaces the surrounding low temperature ($< 500\, K$) gas [STCA06]. In self-blast HVAC circuit breakers ablation arcs are commonly used to build up pressure during the high current phase [OY10]. This intensifies blowing in the subsequent low current phase and supports arc extinction.

The energy transfer from the arc column to the nozzle wall is radiation dominated. Turbulent and conductive energy transfer are also present, however, they occur mainly at the arc boundary and are
negligible in comparison to the radiation [GTR+00, STCA06]. This is so because the ablated gas covers the nozzle surface and prevents direct heat conduction from the high temperature (> 15 000 K) arc column [OY10].

Godin [GTR+00] names pyrolysis and photodegradation to be the physical processes governing the ablation and describes the processes as follows: Pyrolysis occurs due to progressive heating by low-energy radiation. In PTFE nozzles it breaks down the chemical bonds and releases evaporated C2F4 at a temperature of ∼ 1000 K. Exothermally, this vapor reacts further to CF4 and to graphite smoke. Thereby, it increases its temperature to ∼ 2500 K. Photodegradation breaks carbon bonds directly by photochemical reaction and releases CF2 radicals into the vapor. PTFE typically evaporates at a temperature of 1000 K and the vapor heats up to 3500 K by absorption. Above this temperature, PTFE vapor becomes transparent for arc radiation.

Experimental observations by Seeger [STCA06] support this distinction between a low temperature and a high temperature process. In figure 4.1 a constant specific ablation of 8 mg/kJ is observed at current densities \( j < 50 \, \text{A/mm}^2 \). A sharp increase of specific ablation is observable above this current density, which saturates at around 100 – 150 A/mm\(^2\) at a specific ablation of 20 – 30 mg/kJ. As a consequence, ablation occurs primarily in the nozzle throat (narrowest section of the nozzle) and becomes dominant for small diameter nozzles at high current amplitudes.

Osawa [OY10] investigated the ablated material mass during the interruption of sinusoidal 60 Hz currents. He observed a linear increase of this mass with arcing time and an increase to the power of 2.2 with the RMS current amplitude.

The choice of nozzle material is critical for the performance of HVAC circuit breakers, and the material must fulfill various requirements:

- The nozzle material must have a high dielectric strength [MKC+86].
Figure 4.1.: Specific ablation increase with current density $j$. Constant ablation at low $j$, drastic increase above 50 A/mm$^2$ and saturation at high current densities. (copied from [STCA06])
• It should be mechanically stable to withstand transient high pressures > 50 bar at narrow wall-thickness.

• An optimum ablation rate exists with respect to the interruption performance and the breaker life cycle. Strong ablation is required to create an overpressure in a high current phase. However, a too strongly ablating nozzle material leads to a degrading performance for a large amount of switching operations because the nozzle widens and deforms [YMQB08].

• The resulting chemical decomposition products must not affect the dielectric performance of the blow gas or be erosive for the breaker components.

Standard nozzle material in SF$_6$ insulated HVAC breakers is polytetrafluoroethylene (PTFE). The expert knowledge among material suppliers and breaker manufacturers refers to which inorganic filler materials they add to the pure PTFE to optimize its radiation absorption performance [YMQB08]. Among the different fillers, the most popular are Al$_2$O$_3$, TiO$_2$, BN and MoS$_2$.

Research was performed also on alternative nozzle materials (PMMA, PA6-6, PETP, POM and PE) [And97, MKC+86]. Among them transparent PMMA is well suited for research purposes, as it allows high-speed imaging of the arc [WLB11, WKF13].

4.2. Nozzle constricted arcs under forced convection

In HVAC circuit breakers, the arc is constricted by a nozzle to cool the arc with an axial supersonic cold gas flow [BMRL85, FS06]. This flow is either created from an overpressure reservoir or by wall ablation. Air blast breakers release gas from a pre-pressurized volume to create this gas flow [BKM+70]. Puffer circuit breakers compress a gas volume at contact opening [NNH+01]. In self-blast HVAC circuit breakers, wall ablation from a nozzle is used to build up pressure in
a volume during the high current phase, which is used to blow the arc during the low current phase towards current zero [FS06, CM97].

Three cooling mechanisms are considered to be relevant in the energy balance equation to compensate the ohmic heating under stationary conditions [LL75, TL75]:

1. the convective losses, caused by a plasma flow along an axial temperature gradient $\partial T/\partial z$ with sonic velocity $v_z$, specific heat $C_p$ and plasma density $\rho_p$.

2. The net emission of the radiation power $U_N$, not reabsorbed in the plasma column. Part of this radiation is absorbed in the cold gas vapor zone. The rest is absorbed by the nozzle wall and causes mass ablation.

3. The radial conduction (including turbulent energy transfer) due to a radial temperature gradient $\partial T/\partial r$ with a thermal conductivity $k$.

Even though all cooling mechanisms influence the arc simultaneously, simple models have been developed that achieve acceptable prediction accuracy by focusing only on one or two dominating processes. Depending on the current level, three different types of arcs may result in a single experimental arrangement. These are nozzle constricted arcs stabilized by forced convection at low currents and at high currents and ablative arcs at very high currents. The exact current amplitudes at which a certain arc type appears depend strongly on nozzle geometry, blow gas parameters and nozzle material.

4.2.1. Convection stabilized low current arcs

At low currents, the arcs may have a small cross-section $A \ll A_N$ in comparison to the nozzle throat cross-section $A_N$. In such arcs, the conductive-turbulent energy transfer dominates over the axial plasma convection and the radial radiation losses [TL75]. At low
4.2 Nozzle constricted arcs under forced convection

currents, the arc plasma is not fully ionized and the electrical conductivity $\sigma$ increases approximately exponentially with temperature $T$. It has been shown by two different analytical approaches that such arcs are characterized by a decreasing voltage with increasing current (falling characteristic). Two authors [Sha06, Jen93] assumed an axially uniform arc with radius $x = r/r_w$, normalized to the wall radius $r_w$. They used the Elenbaas-Heller equation to predict a radial temperature profile.

$$0 = E^2 r_w^2 \sigma(\xi) + \frac{1}{x} \frac{\partial}{\partial x} \left( x \frac{\partial \xi}{\partial x} \right).$$ (4.2)

$\xi = \int_0^T \lambda \partial T$ is the heat-flux potential, $\sigma(\xi)$ is the electric conductivity, $\lambda$ is the thermal conductivity and $E$ is the electric field. The equation results in an arc voltage $U \propto I^{-1}$ approximately inversely proportional to the current [Sha06].

Tuma and Lowke [LL75, TL75] assumed a parabolic temperature profile and can therefore represent the conduction losses by the following term: $P_{\text{cond}} = (4\pi\lambda T_p)/A$. The plasma temperature $T_p$ is uniform with radius. $T_p$ and the arc-cross section $A$ vary with the axial distance $z$ measured from the upstream electrode. The model includes convective and radiative losses so that it is applicable also for higher currents [LL75, TL75]:

$$0 = \underbrace{I^2/(\sigma A^2)}_{\text{ohmic heating}} - \underbrace{\rho_p C_p v_z (\partial T_p/\partial z)}_{\text{axial convection}} - \underbrace{U_N}_{\text{radiation}} - \underbrace{(4\pi\lambda T_p)/A}_{\text{radial conduction}}$$ (4.3)

At low currents, the thermal conduction of this model causes the arc voltage to decrease with increasing current. The phenomena would be intensified by turbulence losses, which have been neglected in the model [TL75].

Decreasing voltages at increasing currents are typical for free burning arcs at currents $< 100$ A [SO91]. But if sufficient blow pressure is applied, they may prevail up to several kA, as shown in measurements of a puffer circuit breaker [NNH+01] and an air blast
4.2.2. Convection stabilized high current arcs

At high currents, the arcs are characterized by a radially flat temperature profile with an abrupt decrease towards the cold gas vapor zone [LL75]. This is, because a large fraction of the radiation, emitted from the plasma core, is reabsorbed at the edge of the plasma. In a simple mathematical model, an arc constricted by a nozzle throat with cross-section $A_N$ is assumed to have two distinct regions. They vary in axial direction $z$, measured from the upstream electrode. A conducting core with cross-section $A(z)$ and temperature $T(z)$ and a nonconducting cold gas vapor zone enclosing the core with cross-section $A_N(z) - A(z)$ are assumed [LL75, TL75]. The cooling effect of the cold gas results from turbulent mixing of cold gas into the plasma along the axially increasing cross-section downstream.

In such arcs, a quite constant voltage with increasing current results, because the cross-section increases linearly with the current, while conductivity remains constant. The linear cross-section increase with current can be explained with the enthalpy flow balance [LL75]

$$\rho_p h_p v_z \frac{\partial A}{\partial z} = \frac{I^2(1 - f)}{\sigma A}.$$  \hspace{1cm} (4.4)

For this to apply, several assumptions must be made. Firstly, the arc cross-section is assumed large enough, so the convection losses outweigh the conduction losses. Still, $A$ must be small relative to $A_N$, to allow free radial expansion (e.g. $j < 10 \text{ A/mm}^2$). This is given, as long as the ablated mass does not affect the pressure profile in the nozzle. Secondly, the arc is assumed to be fully ionized with saturated conduction $\sigma \approx 100 \text{ S/cm}$ [LL75]. Further, a linearly pressure dependent volume enthalpy $\rho_p h_p/p = \text{const.}$, constant fraction $f$ of power lost by radiation, and axial independence of $f$, $\rho_p h_p v_z$, and $\sigma$ is assumed. By integrating equation (4.4) for a cylindrical nozzle,
the axial cross-section can be found:

\[
A_T = \frac{i}{\sqrt{p}} \sqrt{z} \sqrt{\frac{2(1-f)}{\sigma (\rho_p h_p) v_z}}. \tag{4.5}
\]

\(A_T\), hereafter referred to as theoretical arc cross-section, increases proportionally with current \(i\) and decreases with the square root of blow pressure \(p\). By axial integration of this cross-section for a throat length \(L_T\), a prediction of the current independent arc voltage \(U\) can be found using the definition of resistance:

\[
U = \sqrt{\frac{2 \rho_p h_p v_z}{(1-f) \sigma}} \cdot \sqrt{L_T \cdot p}. \tag{4.6}
\]

A linearly increasing cross-section with current is in agreement with several experimental studies from the 1970s [LL75] and has been experimentally confirmed in section 9.2. Constant arc voltages at increasing current have been observed at several kA in circuit breakers with small puffer diameter [NNH+01].

### 4.2.3. Ablative arcs

Arcs of high current densities \(j > 50\, \text{A/mm}^2\) have cross-sections \(A\) close to the throat cross-section \(A_N\). In such arcs, the radiative energy transfer from the plasma to the nozzle creates massive ablation of wall material. The ablated mass builds up an overpressure in the nozzle and creates an axial convection towards both ends of the nozzle [CM97, Mul93]. (Without externally applied blowing, a stagnation point would result in the axial throat center. An external pressure difference shifts this point towards the nozzle inlet.) The stagnation point pressure \(p_s\) rises approximately with the square of the current and may reach several 10 bar [Mul93, Nie87]:

\[
p_s \approx \frac{v_s}{\sigma \dot{h}} \frac{L_T}{2} \left(\frac{I}{A}\right)^2. \tag{4.7}
\]
\( v_s \) is the sonic velocity of the ablated vapor, \( \hat{h} \) is the required enthalpy to evaporate the mass and accelerate it to sonic velocity. Unlike in convection stabilized arcs, the cross-section is constricted by the increasing pressure at increasing current, thus a linear increase \( A \propto I \) is no more possible. However, as the radiation \( U_T \propto T^4 \) increases approximately with the power of four to the temperature [CM97, Nie87, Low84], only a small increase of \( T \) or \( A \) is required to compensate increasing ohmic heating at increased current by intensified radiation. As a consequence, a positive differential resistance results \( dU/dI > 0 \) [Nie87, TL75, Mul93]. Exact predictions are very difficult, because the ablative pressure interacts with the forced convection, vaporized nozzle material intrudes partly into the arc and influences also the radiation absorption performance in the cold gas vapor zone [IF08, SNSD06].

Increasing voltages with increasing current have been observed with current densities > 50 A/mm² in simple tube experiments [Mul93, Nie87, Low84, STCA06] (without forced convection) and also in HVAC self-blast circuit breakers [STCA06].

4.2.4. Characteristic arc cross-sections

In cylindrical two and three zone models, the arc cross-section is, besides the temperature profile, an important parameter to characterize an arc. It has to be emphasized that various different cross-sections exist. The following three have been investigated experimentally in this thesis and shall be theoretically introduced (cf. figure 4.2):

**Electric arc cross-section**

The electric arc cross-section is defined as the plasma cross-section contributing substantially to the total current flow. An axially averaged electrical arc cross-section \( A_E \) along the nozzle throat of length
4.2 Nozzle constricted arcs under forced convection

Figure 4.2.: Characteristic arc cross-sections in a zone arc model

$L_T$ can be determined from voltage and current measurements as

$$A_E = \frac{i L_T}{u \sigma} \quad (4.8)$$

Two assumptions must be made. Firstly, a fully ionized plasma of $T = 20 \cdot 10^3 \text{K}$ and $\sigma = 100 \text{S/cm}$ is assumed [Rie90]. This is valid for high current arcs only. Secondly, the major voltage drop $u_{throat} \approx u$ is assumed to occur in the cylindrical nozzle throat $L_T$ with minimum cross-section.

Optical arc cross-section

The visible radiation emitted from air plasma varies by several orders of magnitude for a small change in temperature. This was shown for arcs with diameters in the range $d = 0.1 – 1 \text{cm}$ [CGR11]. As a consequence, a significant decrease of brightness is observable in a convection stabilized arc (CSA) between the hot plasma and the surrounding cold gas. $A_V$ is defined as the light emitting arc cross-
Fluid dynamic arc cross-section

In a nozzle with a CSA, nearly all mass flow is carried by the cold gas [Low79]. In comparison, the hot plasma contributes only minimally to the gas flow and acts as an obstacle, partly blocking the nozzle outlet. This is due to the low density of the plasma in comparison to the cold gas. The subsequent mass flow reduction can be rated by the difference between the known effective nozzle cross-section $A_{Neff}$ (without arc) and the effective cross-section $A_{eff}$ (with arc). A fluid dynamic arc cross-section $A_P$ is defined as:

$$A_P = A_{Neff} - A_{eff}$$  \hspace{1cm} (4.9)

For air, a sonic flow occurs if the upstream to downstream pressure ratio exceeds the critical value $p_{up}/p_{dn} > 1.89$ [Whi99]. Under sonic conditions (cf. equation (4.10)), an adiabatic flow rate $\dot{m}_S^*$ in a converging-diverging nozzle is dependent only on the upstream pressure $p_{up}$ with the effective nozzle cross-section $A_{eff}$, the specific heat ratio for air $\gamma = 1.4$, the gas temperature $T$ and the ideal gas constant $R$ [Whi99]. Under subsonic conditions, the downstream pressure $p_{dn}$ affects also the flow rate $\dot{m}_{sub}^*$ (cf. equation (4.11)).

$$\dot{m}_S^* = A_{eff} p_{up} \sqrt{\frac{\gamma}{RT}} \sqrt{\left(\frac{2}{\gamma + 1}\right)^\frac{\gamma+1}{\gamma-1}}$$  \hspace{1cm} (4.10)

$$\dot{m}_{sub}^* = A_{eff} \cdot k \sqrt{\frac{\gamma}{RT}}$$  \hspace{1cm} (4.11)

with

$$k = p_{dn} \sqrt{\left(\frac{2}{\gamma - 1}\right) \left(\frac{p_{dn}}{p_{up}}\right)^{\frac{2}{\gamma}}} \left[1 - \left(\frac{p_{dn}}{p_{up}}\right)^{\frac{\gamma-1}{\gamma}}\right]$$  \hspace{1cm} (4.12)
4.3 Black-box arc modeling

The adiabatic mass flow can be determined from a pressure measurement $p_H$ in a circuit breaker pressure volume $V_H$. A change in $p_H$ results from an imbalance of inflowing mass from a storage volume $\dot{m}_{in}$ and outflowing mass through a nozzle $\dot{m}_{out}$ (cf. equation (4.13)). Hereby, the temperature of the heating chamber $T_V$ varies due to gas expansion from start conditions $T_0$ and $p_0$ (cf. equation (4.14)).

\[
\gamma R T_{in} - \gamma R T_H \dot{m}_{out} = V_H \cdot \frac{dp_H}{dt} \tag{4.13}
\]

\[
T_H(t) = T_0 \left( \frac{p_H(t)}{p_0} \right)^{\left( \frac{\gamma - 1}{\gamma} \right)} \tag{4.14}
\]

4.3. Black-box arc modeling

4.3.1. Introduction

Despite of the significant progress in physical arc modeling [ZAM+02, IF08, SVKS03, KCP09], black-box models are still used to simulate dynamic arc-network interactions due to their low calculation effort [CS05, SK00]. However, the validity and applicability of these models is often challenged for multiple reasons. Firstly, their accuracy strongly depends on the exact description of the arc parameter functions, which are very difficult to determine. Secondly, it is not straightforward to transfer a measured set of parameters to other arc conditions. This requires to determine the parameters for each arc configuration from new. Moreover, there have been doubts that the arc may not be accurately described with a single ordinary differential equation having exclusively the conductance $g$ as state variable [Bis54].

Physical arc models often describe only the stationary arc behavior or, if dynamic behavior is included, they are applicable only to arcs under special conditions [SNSD06]. Further, such models usually fail to model the dynamic behavior because of mathematical
difficulties. However, in many applications the correct modeling of the high frequency interaction between the arc and its surrounding network is important. Passive resonance HVDC circuit breakers, for example, create an artificial current-zero crossing by exciting a high-frequency oscillation current between the arc-chamber and a parallel LC-path \([\text{NNH}^+01, \text{Fra11}]\). In this example, high frequencies (> 5 kHz) and high current gradients (> 10 kA/ms) cause that dynamic processes dominate in the arc. Black-box models have already been successfully applied to describe and predict passive resonance arc behavior \([\text{NNH}^+01, \text{Leu01}]\).

The author agrees with the above concerns about black-box models but believes that many of their difficulties originate from imprecise arc parameter determination by experiments and the inability to validate the chosen black-box modeling equation for the arc under investigation. The choice of a more complex black-box model is only possible if its parameters can still be determined experimentally. Classically, arcs are investigated using sinusoidal test currents which have coupled current amplitude \(I\) and gradient \(\dot{I}\). This current shape is not ideal for parameter evaluation, it would be preferred to control \(I\) and \(\dot{I}\) independently of each other.

### 4.3.2. Generalized Mayr’s equation (Mayr-Schwarz equation)

The majority of today’s black-box models are modifications or extensions of the well known models by Mayr and Cassie and can be reduced to the equation (4.15) \([\text{May43, Cas39, Sch72, SK00, CW93}]\). This equation, also referred to as energy balance equation, describes the change of the stored energy content \(Q\) in the arc column resulting from the imbalance between ohmic heating \(P_{\text{heat}}\) and cooling \(P_{\text{cool}}\):

\[
\frac{dQ}{dt} = P_{\text{heat}} - P_{\text{cool}}.
\]  (4.15)
Under the assumption that the cooling power $P(Q)$ and the conductance $g(Q)$ are arbitrary functions of $Q$, the general form of the dynamic arc equation [CW93, Kap11] can be formulated:

$$
\dot{g} = \frac{1}{\partial Q\partial g} \left( \frac{i^2}{g} - P \right).
$$

(4.16)

With the further assumption of $Q = Q_0 \cdot \ln(g/G_0)$, where $Q_0$ and $G_0$ are constants describing the arc, plus the definition of a thermal time constant $\tau := Q_0/P$, equation (4.16) equates into the Mayr equation:

$$
\frac{\dot{g}}{g} P \tau = \frac{i^2}{g} - P.
$$

(4.17)

"Rejecting the hypothesis that in Mayr’s model of a dynamic arc, represented by equation (4.17), the time constant $\tau$ and cooling power $P$ are constants, leads to a generalized Mayr’s equation or the Mayr-Schwarz arc equation” [Kap11], where $P(g)$ and $\tau(g)$ are free functions of the arc conductance $g$

$$
\dot{g} = \frac{g}{\tau(g)} \left( \frac{u_i}{P(g)} - 1 \right).
$$

(4.18)

### 4.3.3. Physical interpretation of black-box models

Cao and Stokes [CS91] identified three physical processes involved in the transient interaction of arc and arc chamber that act on different time scales $\theta$. These are a) the radiative energy exchange inside the arc associated with a change of arc temperature ($\theta_1 \approx 1.8-5.8\mu$s), b) the readjustment of the gas flow due to a changed arc cross-section ($\theta_2 \approx 10-50\mu$s) and c) the adjustment of ablation rates due to intensified radiation, absorbed by reaching the nozzle, when the arc increases its temperature or approaches the wall ($\theta_3 \approx 100-200\mu$s) [CS91].

In black-box models, a single first order differential equation (typically the twice modified Mayr equation equation (4.18) [Sch72]) is
used to describe parallel influences of different physical processes that change a measurable state $g$ due to an imbalance between ohmic heating $UI$ and arc cooling $P$ with a thermal inertia $\tau$.

At low currents, the change of $g$ is explained by thermoionization and a subsequent increase of the plasma conductivity [May43]; at high currents, the conductance change is explained by a change of arc cross-section [Cas39]. Within this explanation it is obvious that $P$ and $\tau$ are no physical constants but do vary with the arc state $g$ [Sch72] and do also vary with the design of the arc chamber [NNH+01].

### 4.3.4. Transformation of stationary characteristic

Pietsch [PRT75] introduced a commonly used approximation

$$P(g) = 1 \text{ W} \cdot a_{pg} \left( \frac{g}{1 \text{ S}} \right)^{b_{pg}} \quad (4.19)$$

$$\tau(g) = c \left( \frac{g}{1 \text{ S}} \right)^d \quad (4.20)$$

of the stationary and dynamic arc characteristic. It correctly describes only certain types of arcs in a limited conductance range, but is often used for characterization of switching arc chambers [CW93, NNH+01]

The stationary characteristic is often plotted as $U(I)$ or $P(I)$ instead of $P(g)$ [CW93]. All descriptions are equivalent under stationary conditions and can be transformed into each other using $P = UI$ and $g = I/U$. Many authors fitted the stationary characteristic as potential function of either $I$ or $g$. A conversion Table 4.1 has been included to relate the most common descriptions: $P(g) = 1 \text{ W} \cdot a_{pg} \cdot \left( \frac{g}{1 \text{ S}} \right)^{b_{pg}}$, $U(I) = 1 \text{ V} \cdot a_{ui} \cdot (I/(1 \text{ A}))^{b_{ui}}$ and $P(I) = 1 \text{ W} \cdot a_{pi} \cdot (I/(1 \text{ A}))^{b_{pi}}$. It has to be emphasized that dynamically the descriptions are not equivalent anymore. A stationary description has to be converted to $P(g)$ to represent the cooling power in the twice modified Mayr equation (4.18).
4.4. Black-box parameter determination

4.4.1. Intrinsic assumptions

The choice of black-box model equations and the algorithm to determine their parameters typically requires to make certain assumptions about the arc behavior. These assumptions often remain unverified when an arc is characterized experimentally. Firstly, one has to be aware that a process including different physical processes is described by a simple first order differential equation, which may not describe the arc completely. In addition to that some parameter determination methods introduce an analytical shape how the parameters depend on the arc state, e.g. an analytical expression of the arc cooling power $P$ as a function of the arc conductance $g$: $P = P(g)$ [PRT75, CW93]. The terminology direct and indirect are introduced to classify the methods. A determination method is classified as direct if it does not require a mathematical description of the chosen arc parameters, but extracts them as individual measurement points at different arc states. Indirect methods, in contrast, require that the shape of the arc parameters is described, e.g. by

Table 4.1.: Conversion table for stationary arc characteristics:

<table>
<thead>
<tr>
<th>to \ from</th>
<th>$b_{ui}$</th>
<th>$b_{pi}$</th>
<th>$b_{pg}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$b_{ui}$</td>
<td>1 · $b_{ui}$</td>
<td>$b_{pi} - 1$</td>
<td>$\frac{b_{pg} - 1}{b_{pg} + 1}$</td>
</tr>
<tr>
<td>$b_{pi}$</td>
<td>$b_{ui} + 1$</td>
<td>1 · $b_{pi}$</td>
<td>$\frac{2b_{gp}}{1 + b_{gp}}$</td>
</tr>
<tr>
<td>$b_{pg}$</td>
<td>$\frac{1 + b_{ui}}{1 - b_{ui}}$</td>
<td>$\frac{b_{pi}}{2 - b_{pi}}$</td>
<td>1 · $b_{gp}$</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>to \ from</th>
<th>$a_{ui}$</th>
<th>$a_{pi}$</th>
<th>$a_{gp}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$a_{ui}$</td>
<td>1 · $a_{ui}$</td>
<td>1 · $a_{pi}$</td>
<td>$(a_{gp})^{\frac{1}{1 + b_{gp}}}$</td>
</tr>
<tr>
<td>$a_{pi}$</td>
<td>1 · $a_{ui}$</td>
<td>1 · $a_{pi}$</td>
<td>$(a_{gp})^{\frac{1}{1 + b_{gp}}}$</td>
</tr>
<tr>
<td>$a_{pg}$</td>
<td>$(a_{ui})^{\frac{1}{1 - b_{ui}}}$</td>
<td>$(a_{pi})^{\frac{2}{2 - b_{pi}}}$</td>
<td>1 · $a_{gp}$</td>
</tr>
</tbody>
</table>
the often used power functions $P(g) = 1\, W \, a_{pg} \cdot (g/(1 \, S))^{b_{pg}}$.

Generally, the determination of an arc characteristic is performed in two or three steps, in which the second step is optional:

1. Choice of a black-box equation to link the internally stored energy $Q$ to externally measurable parameters $u$ and $i$. This includes specification of arc parameters, which can be arbitrary functions of the arc state, e.g. $P(g)$ and $\tau(g)$ [Sch72].

2. Optionally, the chosen arc parameters are assumed to follow a specific analytic form, e.g. $P := 1\, W \, a_{pg}(g/(1 \, S))^{b_{pg}}$ and $\tau := c \cdot (g/(1 \, S))^{d}$ [PRT75, Sch72]. According to the classification above, determination methods that require such an expression would be called *indirect*.

3. A method is chosen to determine the specified arc model parameters of the chosen modeling-equation from measured $u$ and $i$ oscillograms. Thereby, *direct* determination requires a large measurement effort because the parameters must be measured and determined individually (with a reasonable discretization) for each possible arc state $g$. *Indirect* determination requires significantly less measurement and calculation effort because not the arc parameters but only their coefficients are determined, like $a, b, c, d$ in the example above.

### 4.4.2. Stationary and transient arc characteristic

The influence of different current gradients on the transient arc voltage and its consequences for $P$ and $\tau$ determination, which result shall be briefly discussed. For this purpose, in figure 4.3 results of a simple black-box simulation of a wall-stabilized arc are presented with roughly estimated constant arc parameters ($P \approx 50\, kW$, $\tau \approx 10\, \mu s$). A constant current of $I_{DC} = 100\, A$ with a subsequent rise of $\Delta I = 150\, A$ with various slopes is assumed, and the transient arc voltage is simulated with the twice modified Mayr equation as a reaction to the impressed current.
4.4 Black-box parameter determination

At low current slopes $0.1 \text{kA/} \text{ms}$, the transient voltage is dominated by the stationary characteristic $P(g)$, because the current rise time $T_R = 3 \text{ ms}$ is much larger than $\tau$. Under moderate current slopes of $10 \text{kA/} \text{ms}$, $P$ and $\tau$ affect the transient voltage simultaneously, because $T_R = 30 \mu\text{s}$ is in the same order of magnitude as $\tau$. The transient voltage response during a high current slope of $100 \text{kA/} \text{ms}$ is dominated by the stationary state prior to the current increase. Because of the small rise time $T_R = 3 \mu\text{s}$, the arc does not strongly change its conductance during the current slope, but mainly afterwards. This leads to a linear increase of the arc voltage with current during the slope.

A transient arc parameter can only be determined from those sections of the current-voltage oscillogram where the transient voltage is significantly affected. The comparison of transient voltage waveforms with $\tau = 10 \mu\text{s}$ and $\tau = 20 \mu\text{s}$ reveals that $\tau$ shows the greatest effect around a $10 \text{kA/} \text{ms}$ slope and has no or only a minor effect at slopes $0.1 \text{kA/} \text{ms}$ and $100 \text{kA/} \text{ms}$ (cf. figure 4.3). An exception are
low gradient slopes or constant currents created subsequently to a high gradient slope. Here, the rate at which the voltage returns to a stationary point is dominated by $\tau$. At low current slope without previous high gradient slope, the transient arc voltage is closest to its stationary value. Thus, $P$ has more effect on the transient voltage, the lower the current gradient is.

In summary, black-box parameters that describe the stationary arc characteristic are best derived from measurements with small current gradients. Those parameters describing the transient arc characteristic are best derived from measurements with very fast current change followed by (quasi-) constant currents or current slopes where the relative changes in current $i/(di/dt)$ are in the order of the characteristic arc time constants.

4.4.3. Classical parameter determination methods

The results of chapter 5 will point out that passive resonance is much more sensitive to the arc parameters than to the circuit parameters. Therefore, a very precise knowledge of $P(g)$ and $\tau(g)$ is central for stability considerations. The large number of published methods [Sch72, PRT75, Rij75, Ams77, Rup79, DRWZ83, Leu01] shows how challenging it is to determine the arc parameters correctly from $u,i$ oscillograms. In the publications, three main approaches can be identified: A) the iterative approach, B) the parameter separation approach and C) the multiple gradient approach in C1)-C3) an analytical version and C4) in a graphical version.

A: Iterative Method: Schwarz and Pietsch [Sch72, PRT75] determined the parameter functions iteratively by minimizing a least square error between the measured voltage $u(t)$ and its re-simulation $u^*(t)$ with the arc equation (4.18) (cf. Table 4.2A)). This requires the assumption of an analytical function for $P$ and $\tau$ (typically $P = 1 \, \text{W} \cdot a_{pg} (g/(1 \, \text{S}))^b_{pg}$ and $\tau = c \cdot (g/(1 \, \text{S}))^d$ is used).

B: Parameter Separation Method: Rijanto [Rij75] determined
4.4 Black-box parameter determination

$P$ and $\tau$ independently from each other from a single $u,i$-oscillogram by superimposing a 10 kHz oscillation of sufficient amplitude to a 50 Hz short circuit current (cf. Table 4.2B). He identified instants $t_1$ in time without a change in conductance $\dot{g}(t_1) = 0$ for $P$ extraction and instants $t_2$ without ohmic heating at current zero crossing $i(t_2) = 0$ for $\tau$ extraction. In these instants, the arc equation (4.18) equates to:

$$P(t_1) = i(t_1) \cdot u(t_1) \quad \text{for} \quad \dot{g}(t_1) = 0,$$

(4.21)

$$\tau(t_2) = -\frac{g(t_2)}{\dot{g}(t_2)} \quad \text{for} \quad i(t_2) = 0.$$  

(4.22)

To determine the parameter curves in a wide range of conductances, a current superposition is performed at different start values of arc conductance $g$.

**C1-C3: Analytical Multiple Gradient Methods:** With the assumption of conductance dependent arc energy $Q(g)$ and cooling power $P(g)$, equation (4.18) can be solved for the instantaneous inertia $\tau(t_1) = \tau(t_2)$ and power loss $P(t_1) = P(t_2)$ if the electrical parameters are known for two points in time $t_1$ and $t_2$ with identical conductance $g_1 = g_2 = g$ but different conductance gradients $\dot{g}_1 \neq \dot{g}_2$.

$$P = \frac{i_1^2 \dot{g}_2 - i_2^2 \dot{g}_1}{g(\dot{g}_2 - \dot{g}_1)}$$  

(4.23)

$$\tau = \frac{g(i_2^2 - i_1^2)}{i_1^2 \dot{g}_2 - i_2^2 \dot{g}_1}$$  

(4.24)

Amsinck [Ams77] applied this method for the first time to a single $u,i$ measurement of an extinguished arc with thermal reignition (cf. Table 4.2C1)). There, the assumption is made that the arc is identical before and after current zero. Ruppe [Rup79] applied the method to identical conductance values before and after the peak
### Table 4.2.: Classical parameter determination methods.

<table>
<thead>
<tr>
<th>Method</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>A) Schwarz</strong></td>
<td>- one experiment&lt;br&gt;- iterative recalculation of voltage waveform</td>
</tr>
<tr>
<td><strong>B) Rijanto</strong></td>
<td>- high frequency current superimposed on a 50 Hz oscillation&lt;br&gt;- extraction only at ( \dot{g} = 0 ) and at ( i = 0 )</td>
</tr>
<tr>
<td><strong>C1) Amsinck</strong></td>
<td>- one experiment with thermal reignition&lt;br&gt;- two different gradients at the same conductance</td>
</tr>
<tr>
<td><strong>C2) Ruppe 1</strong></td>
<td>- one experiment with current peak&lt;br&gt;- two different gradients at the same conductance</td>
</tr>
<tr>
<td><strong>C3) Ruppe 2</strong></td>
<td>- two similar experiments&lt;br&gt;- two different gradients at the same conductance</td>
</tr>
<tr>
<td><strong>C4) Drebenstedt / Leu</strong></td>
<td>- arc under passive resonance&lt;br&gt;- multiple different gradients with the same conductance</td>
</tr>
</tbody>
</table>
4.4 Black-box parameter determination

of a current half cycle (cf. Table 4.2C2)). Ruppe also applied this parameter determination method successfully to two or multiple similar experiments with varied current amplitudes or current slopes (cf. Table 4.2C3)).

**C4: Graphical Multiple Gradient Method:** On the suggestion of Rother, Drebenstedt et. al. [DRWZ83] implemented the calculative procedure of Amsinck-Ruppe in a graphical form and applied it to pressurized gas and SF$_6$ arc chambers. To do so, equation (4.18) can be written as

$$ui = (P\tau)\frac{\dot{g}}{g} + P. \quad (4.25)$$

This is a straight line equation of the form $y = mx + n$ with the independent variable $x = \dot{g}/g$, the dependent variable $y = ui$ and the slope $m = P\tau$. Given deterministic arc behaviour, all points of identical conductance values would lie on a straight line (cf. Table 4.2C4). The power loss $P$ results from the intersection with the abscissa $\dot{g}/g = 0$, and the ordinate intersection $u \cdot i = 0$ is used to determine $\tau = -g/\dot{g}$. This treatment significantly improved the recalculation accuracy of high current switching arcs by averaging procedures. Leu [Leu01] applied this procedure to determine parameter functions of passive resonance currents and in particular for current chopping oscillation at the switching of low currents. It was shown that the arc interacts with the effective capacitance in parallel to the arc chamber. Thereby, the interaction causes a passive resonance waveform that is well suited for parameter determination.

4.4.4. Comparison

In the parameter separation method B and the multiple gradient method C, a specific current waveform must be created. In the iterative method A, theoretically any waveform is suitable as long as it contains sufficient high current slopes, because only the four coefficients $a, b, c$ and $d$ must be determined. Method C4 application
by Leu has the advantage that the required current is shaped by
the arc itself. However, the required oscillating current can also be
created artificially with a setup to superimpose a high frequency
current. Wrong results can occur due to physical reasons if external
parameters change the arc during the evaluated section or between
the points of identical gradients (e.g. after a current zero crossing
or if multiple experiments are combined). They can also result from
numerical problems and measurement: The methods must deal with
stochastic arc fluctuations that are always present but not described
by the energy balance equation. A phase shift between current and
voltage measurement or a stray inductance between spatially dis-
tributed measurement probes can result in unphysical or even neg-
ative g values. Also analytical reasons could be a source of errors,
in case that the arc under investigation is not described sufficiently
by the simple black-box equation or if in method A the assumed
parameter function is not valid. A distinction between the three
implementations of method C is made to emphasize parameter de-
termination for different applications. In the experiment, a time as
short as possible should lie between the points of same conductance
to minimize a possible change of the arc during the time of mea-
surement. A thermal reignition used by Amsinck (C1) provides this
optimally at low currents and is therefore suitable to reflect interrup-
tion phenomena. A current peak used by Rijanto (C2) determines
the parameters more accurately at high currents because no current
zero with reignition lies between the used points of identical conduc-
tance. Thereby, interruption phenomena are reflected less accurately
because low g values are difficult to measure. The combination of
multiple experiments (C3) allows using similar sinusoidal currents of
different peak amplitude, which are very easy to create. However, a
large number of experiments should be performed when using this
method to compensate not only for the stochastic nature of the arc
but also a possible scatter between different experiments. Several
adaptations or combinations of the discussed methods with respect
to the available current waveform are imaginable:
• Determination of $P(g)$ becomes trivial if constant DC currents at different levels can be created.

• Several methods can be applied and their results combined. $P(g)$ can be evaluated from an experiment with slowly changing current (eg. B and C1) and $\tau(g)$ calculated from an experiment with rapidly changing current.

• The methods can be applied independently to subsequent sections of the measured waveforms to monitor a possible change of a physical parameter such as contact distance or blow pressure.

In all methods, a test current must be shaped specifically to produce different conductance gradients over a wide range of conductance. For this purpose, mostly sinusoidal currents are used for arc parameter determination because they are easy to create [Rij75, Rot80]. Their drawback is the coupling of current amplitude and current gradient $\dot{I} = \omega I \sin(\omega t)$. As a consequence, only a few $P$ and $\tau$ values result per experiment. The number of experiments can be significantly reduced if a high frequent current is superimposed to a low frequency oscillation. Such are: high frequency sinusoidal current superimposed on a 50 Hz current, high frequency oscillation superimposed on an exponentially decreasing positive current (CR-discharge) [WF11] or a damped oscillation around current zero (LRC-circuit). Also passive resonance currents were used for parameter determination [Leu01]. Of these waveforms, none contains constant current sections, and they do not permit varying $I$ and $\dot{I}$ independently from each other.
5. Optimal arc characteristic for improved designs of passive resonance HVDC-circuit breakers

In this chapter, it is intended to clarify the process of passive resonance and to optimize it theoretically. By systematic modification of the arc characteristic and resonance circuit components a better breaker performance is aimed for. Section 5.1 explains the process of passive resonance current interruption. In section 5.2, the Li-japonov stability theory is used to predict the system stability in a local operating point. The results of a sensitivity analysis for passive resonance by simulations are shown in section 5.3 and discussed in 5.4. In section 5.5, a strategy for experimental arc chamber optimization is formulated based on the findings in section 5.4. This strategy is applied in 5.6 using a pressure dependence of the arc characteristic from literature.

5.1. Passive resonance

A passive resonance circuit breaker consists of an AC circuit breaker in the main path, an LC commutation path with an optional making switch and a path with an energy absorbing element (cf. figure 5.1a)). The arc characteristic and circuit parameters of an MRTB in operation are published in [NNH⁺01] and summarized in Table 5.1. A typical interruption process with this breaker has been modeled in MATBLAB Simulink. The predicted current and voltage
Table 5.1: HVDC MRTB reference parameters for passive resonance circuit stability calculation: adapted from [NNH+01]

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fault current $i_F = I_{Fa} + t \cdot I_{Fb}$</td>
<td>$I_{Fa} = 3.5,\text{kA}$, $\dot{I}_{Fb\text{ref}} = 2,\text{A/}\text{ms}$</td>
</tr>
</tbody>
</table>

- **Arc parameters**
  - $P = (1\,\text{W}) a_{pi} (\frac{I}{I_1})^{b_{pi}}$
  - $\tau(g) = c \cdot (\frac{g}{I_1})^d$
  - $a_{pi} = 0.135 \cdot 10^6$, $b_{pi} = 0.5$
  - $c = 30\,\mu\text{s}$, $d = 0.5$

- **Circuit parameters**
  - $L_0 = 130\,\mu\text{H}$
  - $R_1 = 1\,\text{m}\Omega$
  - $C = 28\,\mu\text{F}$
  - $R_2 = 1\,\text{m}\Omega$

**Figure 5.1:** a) Passive resonance breaker, b) Electric equivalent circuit for passive resonance creation of a current zero crossing
Figure 5.2.: Passive resonance current interruption: a) the fault current ($i_F$) commutates from the main path ($i_1$) to the resonance path ($i_2$) and further to the energy absorber ($i_{abs}$), b) charging of capacitor $C$, c) zoom of arc conductance oscillation, d) zoom of arc voltage ($u$) and capacitor voltage ($u_c$) oscillation
waveforms are displayed in figure 5.2. The interruption process of a 4 kA fault current is discussed. The igniting itself has not been precisely modeled and the arc is assumed to be described with an unchanged arc characteristic of Table 5.1. In reality, the arc characteristic may change during contact opening and if the blow pressure varies. The interruption process includes the following steps:

**Open contact $S_1$ (here at $t = 1 \text{ ms}$):** At contact separation an arc is created. This arc is axially blown by compression of a puffer volume coupled with the opening mechanism. Contact separation typically requires several ms. The arc voltage would rise approximately linearly with distance (not included here) and stabilizes at full distance at a constant voltage (here $U_{\text{arc}} = 2 \text{kV}$).

**Close making switch $S_2$ of commutation circuit:** (here at $t = 2 \text{ ms}$) At the instant of closing $S_2$, the capacitor $C$ is uncharged but the arc voltage is at 2 kV. Thus, an initial current oscillation of $i_2 \approx 1.3 \text{kA}$ is excited between the arc and the LC circuit.

**Passive resonance:** The arc forms in combination with the LC path an unstable resonance circuit. Therefore, the oscillating currents $i_1$ and $i_2$ increase in amplitude.

**Arc extinction:** When the oscillation amplitude of $i_2$ becomes larger than the fault current $i_F$, the superposition of both currents creates a current zero crossing in the arc current $i_1$ (here at $t = 6.5 \text{ ms}$). If the forced cooling is sufficient, the arc extinguishes at current zero crossing. At extinction, the full fault current commutates to the resonance path $i_2 = i_F$.

**Capacitor charging:** Subsequently, the resonance capacitor is charged to the critical voltage of the surge arresters (here $U_c = 210 \text{kV}$), which is typically 1.5 pu of the grid system voltage.

**Voltage limitation:** Once the capacitor voltage exceeds the break through voltage of the surge arresters (here at $t = 8 \text{ ms}$), the fault current commutates from the resonance path into the surge arresters.

**Energy absorption:** The remaining inductive energy, stored in the surrounding network is absorbed by the surge arrester, this forces
the fault current $i_F$ to decrease to zero.

This research focuses on the passive resonance current zero creation, but not on the arc extinction and energy absorption. Therefore, in the equivalent circuit of the breaker, stray resistances $R_1$ and $R_2$ are included, the surge arresters neglected and an impressed fault current $I_F$ assumed (cf. figure 5.1b)). The dynamic arc-commutation-circuit interaction can be modeled using a black-box energy balance equation (5.2) [Sch72] and two differential equations describing the commutation circuit:

$$\frac{d}{dt}i_2 = i_2 \left( -\frac{1}{Lg} - \frac{1}{R_1} - \frac{1}{R_2} \right) + u_c \frac{1}{L} + \frac{i_F}{Lg}$$

(5.1)

$$\frac{d}{dt}u_c = \frac{1}{L}i_2$$

(5.2)

$$\frac{dg}{dt} = \frac{1}{\tau} \left( \frac{(i_F - i_2)^2}{P} - g \right)$$

(5.3)

The state variables are: resonance current $i_2$, capacitor voltage $u_C$ and arc conductance $g$. Typically, black-box parameters are described as functions of the conductance $P = 1 \text{ W} \cdot a_{pg}(g/(1\text{ S}))^{b_{pg}}$ and $\tau = c \cdot (g/(1\text{ S}))^d$ [CW93].

5.2. Stability prediction

For an estimation of the resonance circuit instability, a system matrix $A$ (cf. equation (5.4)) is equated by linearizing eqs. (5.1)-(5.3) in a local operating point $i_2 = I_2$, $u_c = U_c$, $g = G$ and $i_F = I_F$. According to the Lyapunov theorem, a linear system is asymptotically stable if all eigenvalues of the system matrix $A$ have a negative real part. Therefore, at least one eigenvalue of $A$ must have a positive real part to let a resonance current $i_2$ grow from a local operating...
Optimal arc characteristic point

\[ \frac{\text{d}}{\text{d}t} \begin{bmatrix} i_2 \\ u_c \\ g \end{bmatrix} = A \begin{bmatrix} I_2, U_c, G, I_F \end{bmatrix} \cdot \begin{bmatrix} i_2 \\ u_c \\ g \end{bmatrix} \]

(5.4)

with

\[ A = \begin{bmatrix} -\frac{R_1}{L} - \frac{R_2}{L} - \frac{1}{LG} & -\frac{1}{L} & -\frac{I_F}{LG} + \frac{I_2}{LG^2} \\ 0 & 0 & 0 \\ \frac{-2(I_F - I_2)}{ap_gCG^{bpg+d}} & 0 & \frac{(bpg+d)(I_F - I_0)^2}{ap_gCG^{bpg+d+1}} - \frac{1-d}{cG^d} \end{bmatrix} \]

(5.5)

A necessary condition for resonance circuit instability is a negative gradient of the stationary arc characteristic

\[ \frac{\text{d}U}{\text{d}I} \bigg|_{I=I_1} < -(R_1 + R_2) \]

(5.6)

that overweighs the stray resistances [NNH+]01. The condition, however, is not sufficient, because transient effects of \( \tau \) also play an important role.

5.3. Simulation results

By numerical integration of eqs. (5.1)-(5.3), the passive resonance during fault current interruption of the MRTB breaker has been simulated for the following cases (cf. figure 5.3):

- **Excited**: Constant fault current \( I_{Fa} \) and \( S_2 \) closed after opening \( S_1 \), so that initially a resonance is excited by the voltage difference of the arc and the capacitor.

- **Academic**: Constant fault current \( i_F = I_{Fa} \) but capacitor \( C \) charged to \( u_c = R_1 \cdot I_1 + I_1/g \) at the instant of closing \( S_2 \), so that no initial voltage difference exists to excite the resonance.

To avoid that numerical parameters (eg. step width) influence
the excitation, a high frequency 0.1 A current ripple is added to $i_F$ to excite the resonance.

- Rising: Identical to case 2, but the fault current starts to rise with a gradient $\dot{I}_{Fb}$ at closing of $S_2$.

The closing strategy in case 1 is optimal for an installed breaker because the delayed closing of $S_2$ shortens the interruption (cf. figure 5.3). The academic case 2 is optimal to study stability, because the resonance current grows from zero at the local operation point.

Systematically for the academic case 2, all parameters have been varied to study their sensitivity on the time $T$ between the closing of $S_2$ and the first current zero crossing of $i_1$. Figures 5.4a-b) relate the relative increase of $T$ compared to case 2 with a relative change of the circuit and arc parameters. Cases without current zero crossing during the simulation time of $T_{\text{sim}} = 1$ s were plotted as $T = T_{\text{sim}}$.

A reduction of $T$ is achieved for increasing $C$, but the effect saturates for large values of $C$. An optimum value exists for the inductor $L$ that lies two times lower than that of the investigated MRTB. The present stray resistances $R_1$ and $R_2$ have negligible effect. They would have undesirable effect only from 32 mΩ. Just a minor increase of current amplitude $I_{Fa}$ increases $T$ sufficiently. Positive slopes in the fault current $\dot{I}_{Fb} < 0.1$ kA/ms are uncritical. Larger slope values lead to a 2 – 3 times higher interruption time. An increase of stationary arc voltage $U_0$ and its negative gradient $dU_0$ in the operating point both reduce the interruption time strongly. Also the parameters $c$ and $d$ of the function $\tau(g) = c \cdot (g/(1\ S))^d$ have major effect. Their increase strongly increases $T$. At a doubling of either $c$ or $d$ no current zero can be created.

For all simulated parameter combinations, the system instability has also been calculated with equation (5.5). Stable cases are indicated with black circles in figure 5.4. For all stable parameter configurations, also no current zero crossing occurred within the simulation time of 1 s. However, instable cases do not guarantee that a current zero crossing results. Cases exist, where an oscillation
Figure 5.3.: Passive resonance current with excitation, without excitation (academic) and with rising fault current: a) arc current \( i(t) \), b) arc voltage \( u \) vs. arc current \( i_1 \)
grows only up to a certain amplitude and keeps on oscillating with a constant amplitude. The discrepancy between the calculation and the simulation results from the stability calculation having been performed only for the work point onwards. The stability calculation is therefore only a criterion to decide whether an oscillation starts to grow.

5.4. Discussion

Asymptotically stable parameter combinations have only negative eigenvalues of A (cf. equation (5.4)) and are indicated by black circles in figure 5.4a)-b). They correspond, correctly, to the cases where no current zero crossing occurred during $T_{\text{sim}}$.

**Fault Current:** The fault current influences $T$ in two ways. Firstly, at high fault current amplitudes $I_{Fa}$, the stationary characteristic $U(I)$ of a blown arc is typically flat and the thermal inertia is large [BMRL85, BKM+$^{70}$, NNH+$^{01}$]. Hence, the system is typically stable for high currents and instable for low ones. Secondly, a linearly rising fault current $i_F$ stands in competition with the exponential resonance current growth. Consequently, a rising fault current slope delays the current zero creation.

**Stationary arc characteristic:** Large stationary arc voltage $U(I)|_{I=I_1}$ and strongly falling characteristic $\frac{dU}{dI_1}|_{I=I_1}$ in the operation point boost passive resonance.

**Thermal inertia:** Large thermal arc inertia $\tau$, resulting from large parameters $c$ and $d$, delays the conductance change of the arc. By that, a falling stationary characteristic $\frac{\partial U}{\partial I_1}|_{I=I_1}$ results in a flatter or even rising transient characteristic $\frac{\partial u}{\partial i_1}$. For infinite $\tau$ or infinite current gradient $\frac{di_1}{dt}$, the transient arc voltage $u = i_1/G$ would resistively follow the arc current. Consequently, the lowest possible values of factors $c$ and $d$ are desirable.

**Resonance frequency:** Large current gradients add damping to the system, because they increase the lagging of the transient arc voltage. The high oscillation frequency in passive resonance creates
Figure 5.4.: Reduction or increase of time to current zero $T$ relative to the academic reference case, a) sensitivity of $T$ on circuit parameters, b) sensitivity of $T$ on arc parameters.
such high current gradients $\frac{di}{dt} \simeq \omega I_{Fa}$ [NNH$^+$01, BMRL85]. This can be avoided by choosing a large capacitor $C$ and inductor $L$. However, a large inductor is not beneficial because the minimum energy $E_{\text{min}} = \frac{1}{2}(L/I_{Fa})^2$ required to create a reverse current of equal size to the fault current $I_{Fa}$ rises linearly with $L$. Therefore, the capacitor has to be charged by passive resonance to a higher voltage and the current zero creation requires more time. Consequently, an optimum value of $L$ exists (cf. figure 5.4a). A large capacitance and a high charging voltage are unfavorable, as the capacitor costs contribute significantly to the breaker costs.

### 5.5. Criterion for desirable arc cooling mechanisms

A stationary arc characteristic is assumed to follow section-wise a function $U = 1 \text{V} \cdot a_{ui}(I/1\text{A})^{b_{ui}}$. In this description, a parameter $a_{ui} > 0$ and a parameter $b_{ui} < 0$ are required so that a decreasing voltage at increasing current (falling characteristic) and passive resonance instability can exist. Dynamic effects in the arc may only dampen instabilities in the resonance circuit but not create them, (i.e. independent of the shape of $\tau$, it is not possible to achieve passive resonance instability if the arc characteristic is rising $\frac{\partial U}{\partial I} > 0$). Therefore, it is appropriate to optimize the stationary cooling power independently from the dynamic behavior for a large negative stationary gradient $\frac{\partial U}{\partial I} < 0$.

Transferred to the description $P = (1\text{W}) \cdot a_{pg}(g/1\text{S})^{b_{pg}}$ using Table 4.1, a parameter $b_{ui} < 0$ corresponds to a parameter $-1 < b_{pg} < 1$. Transferred to the description $P = 1\text{W} \cdot a_{pi}(I/1\text{A})^{b_{pi}}$, it corresponds to a parameter $b_{pi} < 1$.

The assumption is made that in thermodynamic equilibrium the total power loss $P$ is a superposition of different positive cooling terms $P_{gi}$ and $P_{ji}$ and that the different cooling terms can be separated in such a way that they are potence functions either of the
current or the conductance:

\[ P = \sum_j P_{ji} + \sum_j P_{jg} \quad (5.7) \]

\[ = \sum_j (1 \text{ W}) \cdot a_{jpi} \cdot \left( \frac{I}{1 \text{ A}} \right)^{b_{jpi}} + \sum_j (1 \text{ W}) \cdot a_{jg} \left( \frac{g}{1 \text{ S}} \right)^{b_{jg}} \quad (5.8) \]

It is further assumed that no negative cooling components exist, so that \( a_{jg} \) and \( a_{jpi} \) may only be positive. The cooling terms can now be brought to a \( U(I) \) form, using Table 4.1, and equate to:

\[ U(I) = \sum_j \left( a_{jpi} I^{(b_{jpi}-1)} \right) + \sum_j \left( a_{jg} \left( \frac{1}{1 + b_{jg}} \right) I^{(b_{jg}-1)} \right) \quad (5.9) \]

The differentiation of equation (5.9) with respect to changes in current results in:

\[ \frac{\partial U(I)}{\partial I} = \sum_j a_{jpi} (b_{jpi} - 1) I^{(b_{jpi}-2)} \]

\[ + \sum_j a_{jg} (b_{jg} - 1) I^{(b_{jg}-2)} \left( a_{jg} I^{(b_{jg}-1)} \right) \left( \frac{-b_{jg}}{1 + b_{jg}} \right) \quad (5.10) \]

According to equation (5.9) and (5.10), a positive cooling power component \( P_{jg} \) of \( P_{jI} \) always contributes a positive component to the arc voltage but it may contribute a positive or a negative component to its derivative with respect to the current. It is therefore possible to extend the globally formulated requirements for a falling characteristic \((-1 < b_{pg} < 1 \text{ and } b_{pi} < 1)\) to the individual loss components \((-1 < b_{jg} < 1 \text{ and } b_{jpi} < 1)\). Thus, three categories of loss components result:

**Undesired losses:** Loss components with \( b_{pg} > 1 \) or \( b_{pi} > 1 \) contribute a positive term to the gradient \( \partial U/\partial I \), so that a less
5.6 Blow pressure effect

steeply falling or even a rising $U(I)$ characteristic results.

**Desired losses:** Loss components with $0 < b_{pg} < 1$ or $0 < b_{pi} < 1$ contribute a negative term to the gradient $\partial U/\partial I$, so that a less steeply rising or a falling $U(I)$ characteristic results.

**Irrelevant losses:** Loss components with $b_{pg} = 1$ or $b_{pi} = 1$ add a constant voltage to the $U(I)$ characteristic and do not affect its gradient. Therefore, such losses can not excite passive resonance in a stable system, but they intensify the instability in a system, which is already instable. Thereby, the time required to create a current zero crossing is reduced.

**5.6. Blow pressure effect - an example of desirable losses**

The effect of a physical arc chamber parameter on the passive resonant current creation can be shown for the example of the parameter blow pressure $p_H$. The stationary $UI$-arc characteristic of an air blast breaker has been measured for 10 bar (red crosses), 30 bar (green circles) and 50 bar (blue squares) blow pressure \cite{BKM}. In figure 5.5a) and b) this characteristic is plotted as a $U(I)$ and a $P(g)$ diagram. The measurement points were fitted to the function

$$P = (1 \text{ W} \cdot a_{pg}) \left( \frac{p}{1 \text{ bar}} \right) \left( \frac{g}{1 \text{ S}} \right)^{b_{pg}}$$

with $a_{pg} = 0.393 \times 10^6$ and $b_{pg} = 0.25$. The fitted curves (solid lines in figure 5.5 a) and b)) describe the measurement data accurately so that the effect of blow pressure on the arc characteristic is well described by equation (5.11).

In the above 10 bar example the stationary characteristic is

$$P(10 \text{ bar}) = 3.93 \text{ MW} \left( \frac{g}{1 \text{ S}} \right)^{0.25}.$$  

(5.12)
At an increase of blow pressure to 30 bar, the additional losses are

\[ P(30 \text{ bar}) - P(10 \text{ bar}) = 7.86 \text{ MW} \left( \frac{g}{1 \text{ S}} \right)^{0.25}. \]  

(5.13)

These additional losses rise with an exponent \( b_{pg} \) which is < 1. In other words, the additional losses created by an increase of blow pressure in this configuration rise less than linearly with conductance. According to the concept introduced in section 5.5 such losses are desired losses as they increase the negative slope gradient of the stationary arc characteristic. By that they improve the passive resonant current zero creation.

To illustrate the reduction of time \( T \) required for current zero creation at increased blow pressure, the simulations of section 5.3 have been repeated for an increased and a reduced blow pressure. In addition, a stability calculation was performed with equation (5.4).
5.6 Blow pressure effect

Figure 5.6.: Reduction time to current zero $T$ at increasing blow pressure $p_H$

In the simulations and stability calculation, only the effect of $p_H$ on $P(g)$ was included and a possible effect on $\tau(g)$ neglected.

The results are shown in figure 5.6. They show a major decrease of $T$ for increased blow pressure $p_H$. Black circles indicate asymptotically stable conditions with low blow pressure, where no passive resonance is possible. (In figure 5.6 only relative changes with pressure are given, because the absolute pressure of the MRTB breaker from section 5.3 is not known. Under the assumption that the MRTB arc characteristic behaves similar to the air blast breaker characteristics in [BKM+70] the reference case $p_{H\text{ref}}$ would correspond to a blow pressure of approximately 15 bar.)

It is the goal of this thesis to confirm the reported influence of blow gas pressure on the arc characteristic and to investigate systematically all other possible external influencing parameters like type of blow gas, nozzle geometry, and nozzle material.
5.7. Conclusion

Simulations of a passive resonance MRTB revealed that for its satisfactory operation with a given arc chamber resonance circuit parameters are suitable in a limited range of values only and can add only relatively small enhancement of performance to the breaker. In comparison, small changes of the arc characteristics $P(g)$ and $\tau(g)$ have a significant effect on the interruption time. Hence, an optimally designed arc chamber allows the interruption of higher currents or the choice of a smaller capacitor.

In order to create a falling characteristic even at high currents, it must be investigated, how strongly different loss components (ie. conduction, convection, turbulence and radiation) scale with the current or with the conductance. In a second step, technical measures must be found to modify the arc chamber in a way that undesired loss components are reduced or that desired loss components are enhanced. Irrelevant loss components are, in principle, desirable as well. They may not influence if passive resonance occurs, but they may speed up the passive resonance current increase for a system which is already instable. Further, they may support arc extinction once a current zero crossing is reached. The increase of blow pressure was identified as an example measure to increase desirable losses.

A systematic search for arc configurations with strongly falling voltage-current characteristic and lowest possible arc inertia is advisable to improve the performance of HVDC circuit breakers. This research requires an accurate and efficient determination of arc characteristics. An improved method for accurate characterization will be discussed in chapter 7.
6. Experimental methods

In this chapter, the experiment setup and experimental methods are discussed. Section 6.1 describes the arbitrary current source. Its development is an integral part of this thesis. An LC current source used for sinusoidal current measurements is briefly discussed in section 6.2. Section 6.3 characterizes the model circuit breaker used for the systematic study with blown and wall stabilized arcs. Section 6.4 briefly specifies a spark gap used for free burning arc experiments. An overview on diagnostic measurement equipment is given in section 6.5. Three methods for the determination of arc cross-sections are introduced in section 6.6.

6.1. Arbitrary current source

In the following the arbitrary current source is only described briefly. A detailed description of the components, the performance scope, and the control strategy can be found in Appendix A.

6.1.1. Functionality

The novel arbitrary current source is used to create complex current waveforms with repetitive constant current sections interspersed by fast current slopes (up to 150 kA/ms) in a single experiment [WF10]. The source consists of three parallel interleaved modules with a capability to provide a voltage of 3 kV and a current of 1 kA each and is equipped with a common IGBT controller. The load current $i_{\text{load}}$ is the superposition of all three module currents $i_n$. The functional principle of the modules is similar to that of a buck converter. An
Figure 6.1: Electrical equivalent circuit of arbitrary current source

equivalent circuit of the current source is shown in figure 6.1, a picture of the three modules is provided in figure 6.2.

Each module $n$ consists of a pre-charged capacitor $C_n$, a fuse $F_n$, an IGBT $S_n$ with antiparallel diode, an inductor $L_n$ and a freewheeling diode $D_n$. For closed IGBT $S_n$, a positive voltage difference $u_C - u_{\text{load}}$ is applied across the inductor $L_n$ and if $u_C > u_{\text{load}}$ a positive current gradient results:

$$\frac{di_n}{dt}|_{\text{on}} = \frac{u_C - u_{\text{load}}}{L_n}.$$  \hfill (6.1)

At opened switch $S_n$, the current commutates to the freewheeling diode $D_n$, and the arc voltage $u_{\text{load}}$ causes the current to decrease with a negative gradient

$$\frac{di_n}{dt}|_{\text{off}} = -\frac{u_{\text{load}}}{L_n}.$$  \hfill (6.2)
Figure 6.2.: Arbitrary current source: The picture was taken in the source room. The three parallel modules are shown.
During a PWM-cycle of frequency $f$ and duty cycle $d$, the following current step results:

$$\Delta i_n = \frac{u_C \cdot d - u_{\text{load}}}{L_n \cdot f}. \quad (6.3)$$

Prior to each experiment, the inductance values of $L_n$ and the charging voltage $u_C$ are selected to achieve three significantly different module current slopes $\dot{I}_n$ ($0.1 \text{ kA/ms} < \dot{I}_n < 50 \text{ kA/ms}$) based on prediction $\tilde{u}_{\text{load}}$ of the arc voltage $u_{\text{load}}$:

$$L_n = \frac{(u_c - \tilde{u}_{\text{load}})}{\dot{I}_n}. \quad (6.4)$$

Each inductor $L_n$ is equipped with 10 mechanical tabs, which are distributed logarithmically along the windings. This allows to vary the number of windings and with it the inductance with small steps in a wide range ($40 \mu\text{H}-4.5 \text{ mH}$). Complex current waveforms are achieved by combined or opposed operation of several modules with different current slopes. An ideally constant current period is achieved by two modules with an inductance ratio

$$\frac{L_2}{L_1} = \frac{\tilde{u}_{\text{load}}}{u_c - \tilde{u}_{\text{load}}}, \quad (6.5)$$

so that the rising slope of one module in on-state compensates the falling slope of the other module in the off-state.

A simple but robust PWM control strategy has been chosen: All switches $S_n$ are turned on period-synchronously to a frequency $f$ with a selectable phase shift $\varphi_n$. An IGBT is turned off instantaneously if the instantaneous module current $i_n$ exceeds the instantaneous module set current $i_{S_n}$, and it remains open until the start of the next control cycle. By that, repetitive experiments become very comparable, no overshoot occurs after steep slopes, and no parameter tuning is required when the value of a module inductor is changed.
The current source can create nearly arbitrary current waveforms up to 3 kA at a voltage of $\leq 3$ kV. Therein, rising slopes are selectable up to 150 kA/ms, falling current slopes result as a consequence of consumed energy in the load. In particular, the source can create constant currents, staircases, sawtooths, constant gradient slopes and various superpositions of the shapes mentioned above.

6.1.2. Creation of selected current waveforms

The arbitrary current source is capable to create a large variety of different current waveforms. In the following it is explained how those current waveforms are created that have been used in the scope of this thesis.

Long direct current with small ripple

A small ripple $< 50$ A is achievable by the choice of a high module inductor $L_1 = L_2 = L_3 = 1.5$ mH and by interleaved operation of the modules at $\varphi_1 = 0^\circ$, $\varphi_2 = 120^\circ$, $\varphi_3 = 240^\circ$ (cf. figure 6.3a)). The interleaving virtually triples the switching frequency. Further flattening is achieved by increasing the switching frequency, adding $3 - 6$ mH per module and decreasing the capacitor voltage. However, all of these measures reduce either the maximum achievable experiment time of the maximum achievable current amplitude. A switching frequency increase limits the maximum duration by increased switching losses (cf. figure A.5), a decreased capacitor voltage by a factor of $k$ decreases the available energy by a factor of $k^2$. An increased inductance is problematic because a large fraction of the capacitive energy is used to charge the inductors, which causes a decreasing of capacitor voltage. As a combined consequence of all limitations the achievable ripple typically increases slightly with increasing current amplitude, if long experiment durations are chosen.
Figure 6.3.: Selected current waveforms created by the arbitrary current source: a) long constant current with small ripple, b) short constant current without ripple, c) current slope, d) current slope, e) staircase-like waveform, f) sawtooth superimposed to increasing slope, g) stair current with high gradient slopes and overshoot, h) slope and constant current with current spikes superimposed.
Current slopes

A rising slope with constant gradient

\[
\dot{i} = \frac{U_c - U_{\text{load}}}{L_n + L_B}
\]  \hspace{1cm} (6.6)

results if the set current is higher than the instantaneous current (cf. figure 6.3c)). The module inductor size \(L_n\) and the capacitor voltage \(U_c\) can be chosen to select three significantly different current slopes per experiment by using three modules. The busbar and the bushing to connect the model circuit breaker have a self inductance of \(L_B \approx 40 \mu H\). To avoid interaction of the different modules a minimum module inductor of \(L_n = 20 \mu H\) must be selected. Thus, at maximum charging voltage of \(U_c = 3\) kV, the 60 \(\mu H\) inductance limits the achievable slopes to 50 kA/ms. In principle, 150 kA/ms are achievable by superposition of the three module currents, given certain pre-conditions:

- At applications requiring an 80 ms low pre-current for contact separation, at least one module must be equipped with a large inductor to create this pre-current. Thus, the maximum achievable gradient is reduced to 100 kA/ms.

- A total 6 \(\mu s\) turn-off delay results from the optical transmission of the current measurement and the IGBT gate driver. At 60 \(\mu H\) total inductance, the maximum module set current must be chosen below 700 A to ensure a turn-off at a maximum current of 1000 A.

- At interleaved operation, interactions of modules with different inductor size or different switching times may occur. Such phenomena occur in particular at arc extinction. Thus, at extreme current slopes a synchronous operation of all three modules is advisable.

The falling current slope \(\dot{I}_N = -u_{\text{load}}/L_n\) per module results from the energy consumption of the load. It can therefore only be selected
iteratively or pre-estimated, if the load behavior is predictable.

**Current slope with superimposed spikes**

A current slope with superimposed spikes can be created by interaction of two or three modules (cf. figure 6.3h)). A first module with large inductor remains turned on (high set current) or turned off (set current zero) until minimum or maximum current is reached. This creates a linear slope. A second module with small inductor is switched on and off repetitively during the current slope. This creates short triangular current spikes superimposed to the slope. The spike duration results from the selected set current amplitude and the selected module inductance. As no negative module currents are allowed, only positive spikes can be created. The maximum slope current is limited to 2 kA, as one module is required to create the spikes.

**Stair currents**

Stair currents with large slope and flat constant section can be created by a complex interaction of the source and the load (cf. figure 6.3e)). Precondition for this is that either the load voltage is much smaller than the capacitor voltage or that the load voltage remains approximately constant at increasing current. The latter condition is quite well fulfilled for free burning arcs, unblown SF$_6$ arcs and weakly blown arcs with large nozzle throats at high current amplitudes.

For creation of the 80 ms pre-current, typically one module with large inductor $L_1 = 1-4$ mH is required. At creation of the staircase, this module may either be turned on (high set current) or off (set current zero) during the time, the staircase is created. If so, the current slope of the first module is falling or rising with a constant low gradient. The staircase shape is created by mutual operation of two further modules: One module with typically $L_2 = 0.5 - 1.5$ mH creates a rising slope (high set current). The duration of
the staircase waveform is limited by the time at which this module reaches its maximum current, which is typically the case after 1 – 2 ms. The remaining module is configured with an inductance of 

\[ L_3 = \frac{u_{\text{load}}}{(U_c - u_{\text{load}})}L_2. \]

Its set current is chosen slightly below the desired step size, the switching frequency is chosen such that the module current \( I_3 \) never reaches zero. If so, the third module creates a sawtooth waveform. At opened IGBT \( S_3 \) and closed IGBT \( S_2 \), the decreasing slope of the sawtooth of module 3 compensates the increasing slope of module 2. Thus, a constant current of typical duration 50 – 300 \( \mu s \) results. At the start of the next control cycle, module 3 is turned on and module 2 remains on. By that, the rising slope of module 2 is superimposed to the rising slope of the sawtooth of the module 3. This results in a steeply increasing current. At repetitive toggling of module 3, a staircase-like increasing current results with one step per control cycle.

**Sawtooth**

To create a sawtooth waveform, all three modules should be operated synchronously with the same set current. Their inductance \( L_n = \frac{(U_c - u_{\text{load}})}{(3f \Delta I_n)} \) must be configured to achieve the desired ripple size \( \Delta I \) for the chosen frequency \( f \) and capacitor voltage \( U_c \), based on a prediction of the load voltage \( u_{\text{load}} \). If a symmetric sawtooth is desired, the capacitor voltage amplitude should be chosen to be twice the expected load voltage amplitude.

**6.2. LC current source**

An LC resonance circuit with pre-charged capacitor is used to generate a damped sinusoidal current. This is used to be able to compare the new arc characterization method (cf. section 7.2) using complex current shapes to the classical methods based on sinusoidal currents (cf. section 4.4.3). No additional damping resistor is added, instead the current amplitude decrease resultes from the energy consump-
tion of the arc and the resistance of the test circuit. Inductances and capacitances in the range of $4\,\mu\text{H} - 10\,\text{mH}$ and $3.5 - 14\,\mu\text{F}$ are used. The resulting oscillating frequency of the test current is in the range of $0.5 - 17\,\text{kHz}$.

6.3. Model circuit breaker

In the following the model circuit breaker is described briefly. A detailed description of the components and an equivalent circuit of the blow arrangement can be found in Appendix B. All experiments are performed with the same model circuit breaker (cf. figure B.2) but with a systematic variation of single arc chamber parameters. The design of the model circuit breaker has been optimized for high flexibility in blow pressure, nozzle shape, opening speed and blow gas. It has been constructed as a single flow arrangement and without encapsulation. The breaker is therefore suitable only for gases that can be released freely into atmosphere, such as air, nitrogen and CO$_2$. Therefore all experiments have been performed in dry air instead of SF$_6$.

6.3.1. Functionality

In the model circuit breaker an arc is burning vertically in a nozzle arrangement with top-down current direction, the forced axial blowing is bottom-up along the arc. The breaker is equipped with two vertical copper-tungsten electrodes: a fixed $18\,\text{mm}$ diameter electrode at the bottom and a pneumatically movable $5\,\text{mm}$ diameter upper electrode. The lower electrode is mounted inside a closed pressure volume of $1\,\text{liter}$, into which gas from eight pressurized bottles of $1.5\,\text{liter}$ each can be released instantaneously by parallel opening of $8$ valves. A vertical nozzle is mounted on top of the pressure chamber, so that an arc between the two electrodes is axially blown in a single flow arrangement. The geometry of valves, pipes and the pressure volume is arranged in a way that the minimum cross-section
is inside the nozzle throat. This ensures that sonic conditions if once they are reached, always occur in the nozzle throat. If not specified otherwise, dry air is used as blow gas.

In AC circuit breakers, the contact separation is typically achieved by pre-tensioned springs. Such mechanism is very complex and has the disadvantage that contact velocity and distance are fixed. Instead, a pneumatically movable upper electrode has been used. It achieves a variable opening speed up to \(3.4 \text{ m/s}\) and a variable electrode gap distance up to \(200 \text{ mm}\). The arc is ignited by applying a \(100 \text{ A}\) pre-current during the contact separation time of typically \(80 \text{ ms}\).

6.3.2. Nozzle geometry

All nozzles consists of an inlet section with a length \(L_{\text{in}} = 10 \text{ mm}\) and opening angle \(\varphi_{\text{out}} = 45^\circ\), a cylindrical throat of a diameter \(d_T\) and length \(L_T\), and an outlet of length \(L_{\text{out}} = 10 \text{ mm}\) and opening angle \(\varphi_{\text{out}} = 22.5^\circ\) (cf. figure 6.4). The upstream electrode tip to throat inlet distance \(X_{\text{in}}\) is set to \(10 \text{ mm}\). The throat outlet to downstream electrode tip distance \(X_{\text{out}}\) varies in the range of \(20 - 30 \text{ mm}\). In this range, \(X_{\text{out}}\) has negligible effect on the measured parameters. In most nozzles, all inside edges are rounded with \(1 \text{ mm}\) radius. For the experiment series to investigate different blow pressures, edges are rounded with a 1 centimeter radius instead.

The nozzles are manufactured from PMMA. During the experiments, especially the narrow nozzles changed their geometry due to wall ablation. A given tolerance of the throat diameter \(d_T\) is determined from a measurement before and after an experiment series.

6.4. Free burning arc arrangement

Free burning arcs are used for comparison of the different arc parameter determination methods. The methods should be tested under worst case conditions concerning the signal to noise ratio between the
current-gradient-excited transient voltage overshoot and the stochastic fluctuations. With respect to that, a free-burning horizontal arc is optimal because it features a relatively low voltage but strong fluctuations and thus has a low signal to noise ratio [SO91]. In comparison to free burning arcs, wall stabilized arcs and blown arcs have higher arc voltages and exhibit lower fluctuations.

Spherical copper electrodes of 50 mm radius and a 4 mm spacing gap are used. The arc is ignited by means of a spark plug for the experiments in the LC-resonance test circuit. For the measurements in the new arbitrary current source, a copper igniting wire with 50 µm diameter is used. To minimize the effect of the igniting wire on the arc characteristic, a constant current of 75 A is applied for at least 100 ms prior to the evaluated current interval.
6.5. Diagnostic tools

6.5.1. Current measurement

The current $i$ is measured by an ABB-ES2000 Hall-sensor with uncertainty $< \pm 0.5\%$. It follows current slopes up to 100 kA/ms correctly with a delay $< 1\,\mu s$ and has a 1 dB bandwidth of 100 kHz. The sensor is suitable for the current slopes created in the experiment series. If the current source is operated at higher current slopes, a different sensor must be chosen.

A 1 : 1000 Pearson current-transformer of $< 1\%$ uncertainty has been used to measure the sinusoidal currents.

6.5.2. Voltage measurement

The arc voltage has been measured from the signal difference of two identical LeCroy 1 : 1000 voltage probes with $< 1\%$ error, connectes to a differential amplifier. Geometric constraints make it impossible to place the voltage probes directly at the electrode tips. This caused a stray inductance in series to the arc. Its value is determined from a short circuit measurement to $L = 0.45\,\mu H$. The voltage measurements are mathematically compensated for this stray inductance using the gradient of the measured current. The series resistance between the two voltage probes is negligible.

6.5.3. Pressure measurement

A Kistler RAG piezo-resistive pressure sensor with low frequency resolution (3 dB barrier frequency $> 3\,kHz$) is mounted in the neck of one bottle and measures $p_B$. The symmetrical design of the blow apparatus and the simultaneous operation of all valves justify assuming identical pressure in all bottles. A Kistler 4005B piezo-resistive pressure sensor with high frequency resolution (3 dB barrier frequency $> 100\,kHz$) is mounted inside the pressure chamber in the center between two flow inlets and measures $p_H$. For safety reasons it is not mounted directly at the nozzle throat, but $\approx 10\,cm$ from the
nozzle inlet. The sonic cold gas traveling time reduces the dynamic resolution to \( t = 0.3 \text{ ms} \).

Pressures \( p_H \) and \( p_B \) are not measured as absolute values but as overpressures relative to atmospheric pressure and temperature \( p_R = 1013 \text{ mbar}, T_R = 293 \text{ K} \).

6.5.4. Optical measurement

For the video recording, a Videal MotionPro10000 high-speed-camera with 10000 frames per second and a telescopic lens are used. The camera is positioned such that the whole inner nozzle throat is visible. A brightness reducing filter is attached to the lens. A strong increase of the arc brightness with current and pressure, requires adapting the filter for each measurement. Hence, comparisons of absolute brightness between different experiments have to be interpreted with care. The nozzle is manufactured from transparent PMMA to enable image recording of the arc column. PMMA has been chosen due to its moderate ablation [And97], high transparency for visible light and relatively low toxicity of gaseous decomposition products.

6.6. Arc cross-section determination

In arcs stabilized by forced convection, a current-conducting cross-section \( A_E \), an optical cross-section \( A_V \) and a fluid dynamical cross-section \( A_P \) exist (cf. section 4.2.4). The axially averaged electric cross-section \( A_E \) can be calculated by equation (4.8) from a voltage and current oscillogram. The methods to determine \( A_V \) and \( A_P \) are described in the following two sections.

6.6.1. \( A_V \) determination from arc images

A single frame from the high speed video and its extraction of arc diameter is shown in figure 6.5. The frame shows a convection stabilized vertical arc with gas flow in bottom-up direction and current
in top-down direction. At the bottom of the frame, the cathode spot is observable. It is slightly enlarged by the converging shape of the nozzle. Light refraction at the cylindrical outer rim of the nozzle causes optical distortion of the image. However, if the arc burns in the nozzle center, the ratio between nozzle diameter and arc diameter remains unaffected. The black dot at the bottom is a local saturation of the camera sensor. In the diverging part, the arc is not clearly visible due to the strongly blackened nozzle. A $V_d$ determination is performed from a $(m \times n) = (86 \times 25)$ pixels rectangle at the nozzle throat, indicated in figure 6.5b). From the resolution, a zoom ratio of $r = 0.275 \text{mm/px}$ results. This corresponds to a window of the size of $2.37 \times 0.69 \text{cm}$ at a distance $z = 2.47 - 3.16 \text{cm}$ measured in downstream direction from the upstream electrode. Arc identification is defined in a relative way, taking into account the maximum brightness $h_{\text{max}}$ and the mean brightness $h_{\text{mean}}$ of a horizontal line of pixels. In each horizontal line, a pixel is considered to be plasma $y = 1$ if it exceeds the dynamic reference brightness $h_{\text{ref}} = h_{\text{mean}} + 0.7 \cdot (h_{\text{max}} - h_{\text{mean}})$ of its line and $y = 0$ otherwise. Hence, the vertically averaged arc diameter $d_{\text{load}}$ results as:

$$d_{\text{load}} = r \left( \frac{1}{n} \sum_{n} \left( \sum_{m} y \right) \right).$$  

(6.7)

### 6.6.2. $A_p$ determination from pressure signal

A reference pressure measurement is performed to determine the undisturbed gas flow in the nozzle without arc for a bottle filling pressure $p_B = 5 \text{ bar}$ (cf. figure 6.6). During the period of interest, a sonic flow $\dot{m}_{N_{\text{ref}}}^*$ is present at the nozzle, and a subsonic flow $\dot{m}_{V_{\text{ref}}}^*$ results in the valves. (They were determined by equations (4.13)-(4.14) from the measured pressure curves). In addition, the mass-flow at nozzle $\dot{m}_{N_{\text{ref}}}^*$ and valve $\dot{m}_{V_{\text{ref}}}^*$ is modeled by equations (4.10)-(4.12). It has been found that this flow deviates by a factor of proportionality from the flows $\dot{m}_{N_{\text{ref}}}$ and $\dot{m}_{V_{\text{ref}}}$. Several simplifications contribute to
Figure 6.5.: Arc image for cross-section determination: a) image of a convection stabilized arc, b) arc extraction (blue pixels) from narrowest section of the nozzle throat (red rectangle). The current flow direction is top-down, gas flow direction is from the bottom-up.
this deviation: the non-ideality of the gas, the neglected geometrical disturbances including corners and surface roughness, and the nozzle surface boundary layer that leads to an effective cross-section that is smaller than the real nozzle cross-section $A_N$. A proportional nozzle correction factor $C_N$ has been added to equation (4.9) and a valve factor $C_V$ to equation (4.10) to compensate for the mass flow deviations. $C_V = 0.52$ and $C_N = 1.2$ lead to good agreement between measured pressure curves and the recalculated ones by integration of the modelled mass flow $\dot{m}_{N\text{ref}}^*$ and $\dot{m}_{V\text{ref}}^*$ (cf. figure 6.6). Identical pressure conditions as in the reference case are applied for an experiment with arc. Here, the assumption is made that the cold gas temperature is not significantly affected by the arc and that also the geometrical factors $C_N$ and $C_V$ remain unchanged. The dynamically changing arc cross-section is determined from the mass flow reduction in comparison to the reference measurement. (A new reference measurement is performed for each nozzle shape and for each blow pressure applied.) For this, the pressure signal with arc is subdivided into intervals of $\Delta t = 8$ ms. Therein, the nozzle mass-flow $\dot{m}_N$ is calculated by equations (4.13)-(4.14) and the flow $\dot{m}_N^*$ is also modeled by equations (4.10)-(4.12). By a least square fit, the reduced effective cross-section $A_{\text{eff}}$ is determined iteratively. The fluid dynamical arc cross-section $A_P$ results from equation (4.9). This is done for each interval individually.

6.7. Typical experiment sequence

The sequence of a typical experiment with the arbitrary current source and the model circuit breaker is shown in figure 6.7. The specified times and amplitudes are typical values and vary with the chosen experiment parameters. After closing the safety circuit and calculation of the expected IGBT thermal losses, a configuration file is uploaded from the measurement computer to the IGBT controller. If the uploaded file is valid, the experiment computer advises the IGBT charger to charge the capacitors to the specified voltage. On
reaching the intended capacitor voltage, the selected pressure bottles are filled to the preset pressure. At manual confirmation, the experiment is started and the measurement devices are triggered. Firstly, a small current is applied so that an arc is created at contact separation. At fully opened contacts, the blow valves are opened. Once the blow pressure has stabilized, the desired current waveform is created during a burst of $10^{-20}$ ms. After arc extinction, the contacts re-close. The blow pressure is reduced to zero and at opening of the safety circuit. The remaining energy in the capacitors is discharged via a resistor and the capacitor is short circuited automatically after this. All measurement data are automatically imported into the measurement computer, displayed in a plot and stored.
Figure 6.7: Schematic time diagram of a typical experiment
7. Improved methods for direct black-box arc parameter determination and model validation

In this chapter, an improved method for arc parameter determination is proposed. In section 7.1, the limitations of classical methods are demonstrated by parameter recalculation from synthetic current and voltage waveforms. An improved method is proposed in section 7.2. In section 7.3 the improved method is validated and investigated with respect to its sensitivity to stochastic variations in the arc voltage.

7.1. Limitations of classical methods (A-C)

The different classical methods for arc parameter determination have been introduced in section 4.4.3. Before applying them to characterize a real arc, the methods have been tested with synthetically generated voltage and current waveforms. A hypothetical arc was assumed to have constant arc parameters $P_H = 10\,\text{kW}$ and $\tau_H = 10\,\mu\text{s}$ and to be governed by Mayr’s equation. For various kinds of damped sinusoidal waveforms $i$ the hypothetical voltage response $u(t)$ has been calculated. Damped sinusoidal currents with frequencies of 0.1, 1, 10 and 80 kHz have been simulated and are shown in figure 7.1. At 0.1 kHz, the arc voltage follows mostly its stationary characteristic. Thermal reignitions are observed around current zero crossings. At 1 and 10 kHz, the arc forms loops in the $ui$-diagram (cf. figure 7.2)
Figure 7.1.: Transient $u(t)$ and $i(t)$ waveforms of an ideal hypothetical arc ($P_H = 10\,\text{kHz}$ and $\tau_H = 10\,\mu\text{s}$) governed by the Mayr’s equation. At low frequencies (a-b) thermal extinction peaks are observable. At 80kHz (d) the arc voltage is nearly a sinusoidal waveform in phase with the current.
Figure 7.2.: Transient $u(i)$ and stationary $U(I)$ characteristic of an ideal hypothetical arc ($P_H = 10\, \text{kW}$ and $\tau_H = 10\, \mu\text{s}$) governed by the Mayr’s equation. At sinusoidal current oscillation with $0.1\, \text{kHz}$, the arc follows mostly its stationary characteristic (dashed black line), at higher frequencies, the transient effects become dominant.
because of transient effects. At 80 kHz the arc behaves mostly like a resistor and has a voltage which is in phase with the current.

The iterative method A of Schwarz [Sch72], the parameter separation method B of Rijanto [Rij75] and the graphical multiple gradient method C have been implemented in Matlab. Method C has been implemented in the two versions C1 and C4. In the max-min implementation C1, only the maximum and the minimum conductance gradients at a conductance \( g_0 \) were always used to calculate both parameters \((P(g_0), \tau(g_0))\). This corresponds to the method versions C1-C3 in Table 4.2. It has also been implemented in a graphically averaged form, corresponding to the version C4 in Table 4.2.

7.1.1. Validation

Hypothetical arcs with 0.1 and 10 kHz sinusoidal currents have been simulated. The simulations are identical to the ones in figure 7.1a) and c), but with weaker damping in the current waveform, so that 10–20 subsequent oscillations resulted. All methods were used to re-extract the arc parameters \( P_H^*(g) \) and \( \tau_H^*(g) \) from an ideal hypothetical arc without any noise in the voltage signal \( u_H \). A correct extraction would result in constant parameters for all conductance values. The extracted arc parameters by methods B, C1 and C4 are compared in figure 7.3a) and b). Black dashed lines indicate the correct parameters, red triangles (B), blue circles (C1) and green squares (C4) indicate the re-extracted points. The extracted parameters by methods B, C1 and C4 agree within a 1% band with the assumed parameters in the hypothetical arc. The largest deviations resulted at low conductance values. \( \tau(g) \) values could be extracted much better from the high frequency signal. At 0.1 kHz the determined values are correct, but could be evaluated for very low conductance values only. Further simulations and recalculations have also been performed for non-constant arc parameters (e.g. \( P_H = 1W \cdot a_{pg}(g/(1S))^{b_{pg}} \) and \( \tau_H = c \cdot (g/(1S))^{d} \)). In their case as well an error < 1% resulted.

The simulations confirm that all methods have been implemented correctly. All methods can be used to recalculate the parameters
7.1 Limitations of classical methods (A-C)

Method B: parameter separation
Method C1: multiple gradient (max−min)
Method C4: multiple gradient (averaged)

Figure 7.3.: Validation of classical parameter determination methods (Black dashed lines indicate the correct parameters, red triangles (B), blue circles (C1) and green squares (C4) indicate the re-extracted points): a),b) satisfying parameter extraction (error < 1%) from an ideal arc for a 0.1 kHz and 10 kHz sinusoidal current, c) failure to extract parameters from a realistic arc with 2% Gaussian noise superimposed to the voltage signal, d) and e) improved but still not satisfying extraction results by filtering of disturbed voltage signal before determination.
with an accuracy of 1% in the investigated band of current frequencies, given that the voltage and current signals are free of noise. They also revealed that τ extraction can only be performed successfully if a large current slope is present as in the 10 kHz current waveform.

A similar validation was performed for method A. Unlike with the other methods, in A, not parameters at discrete conductance value are extracted but the coefficients $a$, $b$, $c$ and $d$ of the function $P_H = 1 \text{ W} \cdot a_{pg} (g/(1 \text{ S}))^b_{pg}$ and $\tau_H = c \cdot (g/(1 \text{ S}))^d$ calculated by a least square fit. Method A recalculate all parameters correctly within a 1% band. It has to be emphasized that the method would fail if the assumed arc parameters cannot be described by the function above.

### 7.1.2. Sensitivity to noise

A real arc voltage signal is almost never free of noise but has always stochastic fluctuations superimposed [SO91]. In other words, the voltage signal contains non deterministic variations, which are not described by the black-box modeling equation. To take account of this in the hypothetical arc, a white Gaussian noise with vanishing mean value and standard deviation of $\pm 1 \text{ V}$ was added to the simulated arc voltage signal. This corresponds to $\approx 2\%$ of the maximum arc voltage, which is considered to be a conservative lower limit.

All methods have also been used to extract the parameters from the disturbed signals. Method A was once more able to derive the coefficients $a$, $b$, $c$ and $d$ correctly with minor error ($< 3\%$). The results of the other methods were much less satisfying. Methods B and C1 and C4 showed significant scatter ($> 20\%$) in $P(g)$ and returned unreasonably high or even negative values of $\tau(g)$ (cf. figure 7.3c)). Unlike method A, methods B and C1 and C4 use single points in time to calculate the arc parameters. Therefore, they are much more sensitive to disturbances of measurement values.

It was tried to improve the $\tau$ determination from methods B, C1 and C4 by numerically filtering $u_H(t)$ before the parameter extraction. The extracted parameters, improved by filtering are plotted in figure 7.3d)-e). A 5th order Butterworth filter with cut-off fre-
quency if 100 times the current oscillation frequency was able to reduce errors and scattering of method B below 10%. However, the results of $\tau(g)$ are still not satisfying. For methods C1 and C4, the filter reduced the error in $P(g)$ below 10%, but the error in $\tau(g)$ remained above 50%. The poor $\tau$ evaluation of method C1 and C4 is caused by the error amplification by the mathematical or graphically straight line fit. The version C4 is slightly superior to the version C1 in $P(g)$ determination. This is so, because in C4 not only two but several data points per conductance value are used to extract the parameters. This has an additional averaging effect.

7.1.3. Consequences

Simulations with hypothetical arcs revealed the sensitivity of methods B, C1 and C4 to arc fluctuations. In particular, $\tau$ values cannot be extracted with sufficient accuracy to compare different arc chambers by comparing their arc parameters. Method A is much less sensitive to noise, but is not considered an alternative, because it is restricted to arcs, whose parameter shape function can be specified before the measurement (e.g. $P = 1W \cdot a_{pg}(g/(1S))^{b_{pg}}$ and $\tau = c \cdot (g/(1S))^{d}$).

The performance of the classical methods is not sufficient for the planned schematic comparison of different arc chambers. It was, therefore, necessary to develop an improved parameter determination method, with the same robustness to noise as method A but without the requirement to pre-specify an analytical function, which the arc parameters are assumed to follow. The improved method presented in the next section tries to separately measure the stationary and transient arc characteristic and to simplify the parameter extraction by a choice of a more complex current waveform.
7.2. Improved methods (D)

7.2.1. Arc characterization for choice of appropriate model

A novel current source allows testing with nearly arbitrary choice of current waveforms including steps and slopes of varying steepness (cf. section 6.1). Based on this capability, a new arc parameter determination method was developed that uses a series of staircase-like current steps for arc characterization. Herein, the current gradient is chosen as large as possible but at least sufficient to create a significant overshoot in the arc voltage. The subsequent constant current is chosen sufficiently long for the arc voltage to reach a stable value. The step amplitude must be chosen small enough such that a linear transition in the stationary characteristic can be assumed between two successive constant current values.

The arc voltage response $u(t)$ to a single current step $i(t)$ is shown in figure 7.4. The arc shows a stationary characteristic during the
constant current intervals, but fluctuations are superimposed. Furthermore, a transient arc voltage spike occurs during the current step followed by a subsequent exponential decrease in the constant current phase towards the new stationary voltage. From the observed transients one can conclude that the free-burning arc under investigation is quite well described by a first order differential equation, i.e. the current steps are used to identify a suitable arc model.

It is now either necessary to characterize the arc model-independently, by using a large number of such experiments with different current gradients, or to choose a black-box modeling equation and to determine its parameters as a free function of an arc state. A model-independent arc characterization is discussed in section 7.2.2, an improved method for determination of arc parameters is proposed in section 7.2.3. In agreement with other authors equation (4.18) is chosen [Sch72] and the parameters \( P(g) \) and \( \tau(g) \) are determined as free functions of conductance \( g \).

### 7.2.2. Model-independent arc characterization

Instead of introducing a black-box model at this point, the arc could be characterized by a table with externally measurable parameters \( u, \partial u/\partial t \) as a response to several combinations of \( i, \partial i/\partial t \). To fill in such a table, a large number of similar staircases would have to be measured with different current slopes \( \partial i/\partial t \) of the rising slope. We would consider such a description model-independent, because it is not necessary to introduce a black-box equation. The disadvantage of such a description is the large measurement effort, so that it is very impractical to implement. Furthermore, it would require developing a methodology for relative comparison of different tables from different arc chambers.
7.2.3. Improved direct parameter determination method for staircase-like currents (D1)

It was found that current steps are also well suited to extract the arc parameters of a chosen model, as the stationary and transient arc characteristics are measured independently from each other. A similar approach has been proposed in 1954 [Bis54], but it could not be put experimentally into practice at this time.

The specific determination algorithm is shown here in the example of the generalized Mayr’s equation (4.18). However, it is not restricted to this model and can be used more generally.

1. The measured voltage $u(t)$ and current $i(t)$ are divided into sections $u_j(t)$, $i_j(t)$ each including a current step and the subsequent constant current.

2. In each section, the quasi-stationary voltage $U_j$ and current $I_j$ after the dynamic behavior vanished are taken. The stationary cooling power follows directly as $P_j = I_j \cdot U_j$ versus conductance $G_j = I_j/U_j$.

3. $P_j(G_j)$ is section wise linearly interpolated between two quasi-stationary points:

$$
\bar{p}_j = a_j \cdot g + b_j
$$

with $a_j = \frac{P_j - P_{j-1}}{G_j - G_{j-1}}$ and $b_j = P_j - a_j \cdot G_j$.

4. The conductance $g_j(t) = i_j/u_j$ and its gradient $\dot{g}_j(t)$ are calculated for each section. The chosen model equation (here equation (4.18)) is also solved for the current gradient using equation (7.1)

$$
\dot{g}_j^*(t) = \frac{1}{\tau_j} \left( \frac{i^2}{\bar{p}_j} - g_j \right)
$$

(7.2)
5. For each of the segments $j$ the least square error

$$\sum_t (\dot{g}_j - \dot{g}_j^*)^2$$  \hspace{1cm} (7.3)

is then minimized iteratively by modifying $\tau_j$ with a start guess, in our case $\tau_j \approx 10 \mu$s or $\tau_j = \tau_{j-1}$.

### 7.2.4. Improved direct parameter determination method for non-staircase-like currents (D2)

The model-independent method of section 7.2.3 requires that before and after a transient slope a constant current must exist. The following adaptation of the method overcomes this limitation. It determines one stationary point from the transient phase, so that a constant current is only required either before or after the slope.

1. A current is shaped such that a constant amplitude is applied for a time $t > 5\tau$ to ensure establishment of a stationary condition. This constant current is followed by a slope with large current gradient. A current waveform can consist of several similar sections at various current amplitudes that fulfill the former condition. If so, the method is applied independently for each section and delivers a single $P$ and $\tau$ value at a single conductance value for each section.

2. Only one stationary value $P_1(g_1)$ is determined from the constant current time-wise as close as possible to the subsequent current slope.

3. The least square error fit between equation (7.2) and equation (4.18) is performed for an unknown $\tau$ and for unknown stationary power loss $P_2(g_{\text{max}})$ at the maximum measured conductance during the slope. The point of maximum conductance is an extremum $\dot{g} = 0$ and is, therefore, a dynamically occurring stationary point according to Rijanto [Rij75].
7.3. Validation of improved parameter for determination methods (D)

The improved methods D1 and D2 have also been validated with synthethical current waveforms. A measured staircase-like current (cf. figure 7.5a)) and a measured falling slope with superimposed spikes (cf. figure 7.5b)) have been used for that. Two different synthetic arcs have been used to validate the methods: In the first simulation series, a hypothetic free burning arc with low voltage and constant parameters \( P = 10 \text{ kW}, \tau = 10 \mu\text{s} \) was postulated. The classical methods have been tested with an identical arc. In a second simulation series, a hypothetic blown arc with high voltage was assumed. Its parameters were taken from the MRTB, described in section 5.1: \( P = 1 \text{ W} \cdot a_{pg}(g/(1 \text{ S}))^{b_{pg}} \) with \( a_{pg} = 6.92 \cdot 10^6 \) and \( b_{pg} = 0.33 \), \( \tau(g) = c \cdot (g/(1 \text{ S}))^d \) with \( c = 30 \mu\text{s} \) and \( d = 0.5 \).

Synthetic voltage waveforms have been calculated for both arcs and for both current waveforms. Gaussian noise \( u_N(t) \) with mean value \( m = 0 \) and standard deviation \( \sigma(u_N(t)) \) have been added to the calculated voltage signals to test the limits of the determination methods. The low voltage arc has been interfered with \( \sigma(u_N) = 1 \text{ V} \) and \( \sigma(u_N) = 5 \text{ V} \) noise. The former corresponds to the interference the classical methods have been tested with, the latter noise is five times higher. The blown high voltage arc has been interfered with \( \sigma(u_N) = 100 \text{ V} \) and \( \sigma(u_N) = 500 \text{ V} \) noise so that approximately the same signal to noise ratio results as in the free burning arc. The improved method for steps (method D1) and for spikes (method D2) have been used to re-extract the arc parameters from the undisturbed and the disturbed signals.

The extracted \( P(g) \) characteristics are shown in figure 7.6, the corresponding \( \tau(g) \) characteristics in figure 7.7. For no added interference and 1 V added interference, the accuracy of \( P(g) \) is as good as the \( P(g) \) determination with the classical methods from a 0.1 kHz sinusoidal current. For 5 V interferences, where the classical methods mostly failed, the \( P(g) \) is still accurately recalculated with an
error $< \pm 5 \%$. The largest error in $P(g)$ is observable at very low conductance values. This is because there the spikes and staircases are large relative to the bending of the $P(g)$ characteristic, so that it is no longer sufficiently linear between two subsequent stationary points.

In contrast to the classical methods, the $\tau(g)$ determination is accurate, even at a very poor signal to noise ratio. The improved method tolerates in average five times lower signal to noise ratio ratio than the classical methods.

The differences of staircase-like current waveforms and spike currents are small for $1 \text{V}$ interference in the free burning arc and $100 \text{V}$ interference in the MRTB arc. However, at a $5 \text{V}$, respectively $500 \text{V}$ noise level, $\tau$ extraction from the staircase-like currents is more accurate.

The results further showed that it is easier to extract parameters from a high voltage arc than from a low voltage arc, when both signals are similarly disturbed. Free burning horizontal arcs can be considered as the worst case, in comparison to vertical, wall stabilized and blown arcs. Former have a very low arc voltage and provide no external stabilization measures, so that a very poor signal to noise ratio results.
Improved methods for direct arc parameter determination

Figure 7.5.: Validation of improved parameter determination methods

D1 and D2: Measured current waveforms and synthetic voltage waveforms: a,b) current waveform, c,d) synthetic free burning arc, e,f) synthetic MRTB arc
7.3 Validation determination methods (D)

Figure 7.6.: $P(g)$ noise sensitivity of improved parameter determination methods: a,c): extracted from staircase-like currents with method D1, b,d) extracted from spike currents with D2
Figure 7.7: $\tau(g)$ Noise sensitivity of improved parameter determination method: a,c): extracted from staircase-like currents with method D1, b,d) extracted from spike currents with D2
8. Experimental comparison of arc parameter determination methods

In this chapter, a free burning arc and a wall stabilized arc are used to experimentally validate and compare the different classical with the new arc parameter determination methods. In section 8.1 the novel improved method D1 proposed in section 7.2 is compared with the classical methods of section 4.4.3. In section 8.2, the current waveform is optimized for the improved determination methods D1 and D2.

8.1. Comparison for a horizontal free burning arc

The horizontal spark gap described in section 6.4 was used to compare the classical methods for arc parameter determination with the improved novel method D1. The LC current source, described in section 6.2, was used to test the classical methods A, B and C4 described in section 4.4. 13 different frequencies in the range of 0.5 – 17 kHz have been used. The arbitrary current source, described in section 6.1 was used to create the staircase-like current waveforms for method D1. The existing methods are all based on the generalized Mayr’s equation. Therefore, the comparison is performed for its parameter functions $P(g)$ and $\tau(g)$. As verified earlier, equation (4.18) characterizes the arc under investigation sufficiently accurately.
8.1.1. Classical methods (A-C)

Measurement results

Sinusoidal currents of 13 frequencies in the range of $0.5 - 17\text{kHz}$ were created and applied to the spark gap described in section 6.4. Of these, only three test currents and the corresponding voltages are shown in Figures 8.1-8.3. The voltage waveforms show re-ignition peaks shortly after current zero only for low current oscillation amplitude and for low frequency oscillations. Only those conditions show sufficiently small current gradients at current zero so that the arc reduces its conductance significantly. For increasing frequencies, the measurement gets closer to a sinusoidal arc voltage that is in phase with the current. For infinitely high frequency, the arc would show purely resistive behavior.

Parameter evaluation results

Methods B and C were applied separately to each measurement to extract the parameters $P(g)$ and $\tau(g)$. Method A was applied to all selected measurements in Figures 8.1a), 8.2a) and 8.3a) simultaneously. The resulting $P(g)$ are shown for three test current frequencies in Figures 8.1b), 8.2b) and 8.3b) and the resulting $\tau(g)$ in Figures 8.1c), 8.2c) and 8.3c).

Method A yields a linearly rising $P = 1\text{W} \cdot a_{pg}(g/(1\text{S}))^{b_{pg}}$ with $a_{pg} = 1560$ and $b_{pg} = 1$. Methods B and C extracted almost the same linearly rising power loss for all frequencies. Methods B and C show an increasing scatter in $P$ with increasing frequency. In method B this is caused by error propagation. The time the arc remains at $\dot{g} \approx 0$ is shorter for higher frequencies and thus scatter in measurement voltage and current can be averaged out less well. For method C, this is caused by the arc behavior. At low frequencies, the arc follows mainly its stationary curve. Consequently, lower current gradients, i.e. lower oscillation frequencies, lead to more accurate $P(g)$ results.
8.1 Comparison for a horizontal free burning arc

---

**Figure 8.1.** Experimental results of LC-currents at 0.84 kHz (classical methods): a) current and voltage waveforms, b) arc power loss $P(g)$, c) thermal inertia $\tau(g)$.
Figure 8.2.: Experimental results of LC-currents at 3.6 kHz (classical methods): a) current and voltage waveforms, b) arc power loss $P(g)$, c) thermal inertia $\tau(g)$.
Figure 8.3.: Experimental results of LC-currents at 8.3 kHz (classical methods): a) current and voltage waveforms, b) arc power loss $P(g)$, c) thermal inertia $\tau(g)$. 
Methods A–C all extracted arc thermal inertias of $0 - 20 \mu$s. However, the resulting $\tau$ strongly varies with method and current oscillation frequency: Method A results in a curve $\tau(g) = c \cdot (g/(1 \text{S}))^d$ with $c = 4.3 \mu$s and $d = 0.3$. A similar result was found for the 8.4 kHz measurement with method B and for the 3.5 kHz measurement with method C.

For a reliable evaluation also at high conductance values, method B requires high frequency oscillations as it uses current zero crossings for the $\tau$ evaluation. At low conductances, method B yielded unexpectedly high $\tau$ values.

Method C extracts $P$ and $\tau$ simultaneously and, consequently, works reliably only in a narrow frequency range of the arc current. At 840 Hz the voltage waveform is mainly stationary and $\tau$ cannot be evaluated at all. At 8.4 kHz the fluctuations in $P$ cause, as explained above, strongly scattered and rather low $\tau$ values. The 3.6 kHz test current provided reasonable results for $\tau$ in the range $3 \text{S} < g < 13 \text{S}$. At very low conductances, unexpectedly low $\tau$ values resulted, and at very high values a strong scatter is observable. The evaluation method C is very sensitive to the chosen test current frequency. Evaluation of $\tau$ from experiments with 1 kHz and 5.8 kHz show comparably poor results like those from 840 Hz and 8.4 kHz, respectively.

### 8.1.2. Staircase-like current method (D1)

**Measurement results**

Four identical experiments were performed with step-wise increasing current. A typical current waveform is plotted in figure 8.4a). Each current waveform features steep slopes with $\partial i/\partial t > 7 \text{kA/ms}$ to show deviations from the steady state curve. Each current step is followed by a current plateau lasting $\Delta t = 230 \mu$s with nearly constant current with slopes $\partial i/\partial t < 0.07 \text{kA/ms}$. The current step is $\Delta I \approx 50 - 100 \text{A}$.

The arc voltage measured is also plotted in figure 8.4a). For each
current step, a voltage peak with an approximately exponential decay of duration $t_{\text{fast}} = 10 - 15 \mu s$ is observable. A quasi-stationary arc voltage section follows. This gives a first rough estimation of the thermal inertia $\tau$. During the constant current phase, the arc voltage is not fully constant but fluctuations with time constants of $t_{\text{slow}} > 500 \mu s$ occur. This behavior is due to fluctuations of the arc geometry or in its temperature distribution and is not described by the black-box equation (4.18). Thus, even a perfect parameter evaluation method will show a certain error in predicting the quasi-stationary behavior. However, the large difference in the dynamics $t_{\text{slow}} \gg t_{\text{fast}}$ justifies a $\tau$ extraction with a minimum error.

**Parameter evaluation results**

The resulting arc cooling power during one experiment with staircase-like current is shown in figure 8.4b). The transient heating power $p_{m}$ is plotted with a solid blue line and the quasi-stationary values $P_{j}$ from the constant current phases are indicated by red circles. These values were extracted as the mean value of the arc voltage during the constant current sections starting 50 $\mu s$ after the current step up to the next step. This assured that the fast dynamics have already vanished and the slow dynamics are averaged out. The stationary arc characteristic is approximately described by $P_{\text{fit}} = 1 \text{ W} \cdot a_{pg}(g/(1 \text{ S}))^{b_{pg}}$ with $a_{pg} = 659$, $b_{pg} = 1.24$. From the shape of the $P_{j}(g_{j})$ curve (cf. figure 8.4b)) it is possible to judge if a linearization between the points is reasonable or if another measurement with smaller step size is required. Moreover, a large scatter in $P_{j}(g_{j})$ would indicate that the external physical conditions are not maintained sufficiently constant.

The extracted $\tau(g)$ values are displayed in figure 8.4c). A general increase with increasing conductance can be seen, and $\tau(g)$ can be approximately described by $\tau_{\text{fit}} = c \cdot (g/(1 \text{ S}))^{d}$ with $c = 6.7 \mu s$ and $d = 0.23$. The standard deviation is $\approx 4 \mu s$.

**Recalculation of the step current experiment with evaluated arc parameters:** As a consistency check, the arc voltage can
Figure 8.4.: Results with arbitrary current source: a) staircase-like test current and measured arc voltage, b) identification of steady-state points (red circles) and fit curve (black dashed line) from the dynamic signal (blue solid line), c) Evaluated $\tau$ values (red circles) with method D1 and fit curve (black solid line) with standard deviation (black dashed line)
Figure 8.5.: Parameter determination results with arbitrary current source: a) comparison of measured and recalculated voltage, b)-c) arc parameters extracted with method D1 from four identical experiments.
be recalculated with the black-box model equation (4.18) using the previously evaluated discrete values of $\tau_j$ and linear interpolations between $P_j$. In figure 8.5a), the recalculated arc voltage waveform (dashed line) is compared with the measured one (solid line). The dynamic behavior corresponds correctly to the measured arc voltage. The stationary behavior does correspond only on average, as the slow stationary dynamics were neglected as described above. This holds true for the general fluctuations, but also for the sudden voltage drop at 76.6 ms in figure 8.5a). The voltage drop is most probably caused by a movement of the arc column, which is not represented by the modeling equation.

In Figures 8.5b) and c), the evaluated parameters of four consecutive experiments with identical staircase-like currents are shown. In all experiments the dynamic arc behavior was similar, but the average arc voltages differed by approximately 5 V. This can be seen in the $P(g)$ curves of figure 8.5b). While all $P(g)$ curves are in themselves quite smooth, different $P(g)$ curves result from test to test, an indication of the different states of the arc in the different experiments. Interestingly, all four experiments result in fairly similar $\tau(g)$, as shown in figure 8.5c). Consequently, $P$ seems to vary more from experiment to experiment than $\tau$.

**Recalculation of LC-experiments with parameters evaluated from step-experiments:** The parameters extracted by method D1 from the step current measurements were also used to recalculate the arc voltage of the experiments in the LC-circuit described above. Therefore, the parameter curves were analytically interpolated by the functions shown in Figures 8.5a) and b) (solid lines).

$$P(g) = 1 \text{ W} \cdot a_{pg} \cdot \left( \frac{g}{1 \text{ S}} \right)^{b_{pg}} \quad \text{and} \quad \tau(g) = c \cdot \left( \frac{g}{1 \text{ S}} \right)^{d} \quad (8.1)$$

with $a_{pg} = 1718$, $b_{pg} = 1$, $c = 5.4 \mu$s, $d = 0.26$. Again equation (4.18) was used to model the arc.

The recalculated voltage waveforms (dashed lines) are compared with the measured ones (solid lines) in figure 8.6. The recalculation
result is very satisfying, deviations only occur directly after current-zero as the re-ignition is not covered in this black-box model.

8.1.3. Discussion of method comparison

To achieve an accurate measurement of dynamic arc current and voltage is very challenging, even with suitable measurement equipment. In addition, the arc may change its characteristic during a measurement due to elongation, arc root movement, as well as magnetically or thermally caused movement of the arc column. This results in continuous and stochastic arc voltage fluctuations. Hence, the quality of the determination algorithms mainly depends on the ability to extract from a signal with a low signal to noise ratio that part of the behavior that is governed by the chosen energy balance equation.

Method A is very easy to implement, not sensitive to noise in the measurement signal, requires no specific current waveform, and often provides very acceptable results [Sch72, NNH+01]. However, the selected parameter functions are fitted to the whole experiment and thus typically to a wide range of conductances $g$. Even if it is possible to find parameter functions for $P$ and $\tau$ that are valid over the whole range of $g$, it is not obvious that the evaluated parameters would be constant over the whole experiment. In our case, method A was only successful because it was fitted to all three measurements at different test current frequencies simultaneously. The best fit to the experiment at $f = 840\, \text{Hz}$ results in a poor recalculation of the arc voltage of the experiment at $f = 8.3\, \text{kHz}$.

Method B provides the benefit of independent $P$ and $\tau$ determination but requires a current waveform with sufficient number of current zero crossings and sufficient instances with $\dot{g} = 0$. An oscillating current waveform with decreasing amplitude and decreasing current gradient is thus ideally suited. Alternatively, multiple measurements may be combined to collect sufficient extraction points. Handling numerical challenges is critical in the implementation of method B: Firstly, fluctuations in the measurement signal lead to
Figure 8.6: Recalculation of the voltage waveforms for a sinusoidal current using the parameter functions extracted with the staircase-method.
instances with $\dot{g} = 0$ that are part of the arc dynamics but are easily misinterpreted as stationary points. Secondly, at current zero the values $g$ and $\dot{g}$ are not defined, due to division by zero, and interpolation methods must be used. Thirdly, any time shift between voltage and current zero crossing leads to unreasonable $\tau$ values. Such a shift occurs quite regularly and can be the consequence of a measurement offset or a delay from the measurement equipment and long cables. The cooling power is successfully evaluated only for low frequencies as the arc is truly stationary at the current peak only for these current shapes. For increasing test current frequency, the arc is no longer in the stationary state which leads to an increased scatter in the evaluation. The opposite is true for evaluation of $\tau$. From stationary arcs (low frequency oscillations), the thermal time constant cannot be evaluated. To obtain reliable data from method B, at least two measurements with significantly different oscillation frequencies need to be performed.

Method C relies strongly on the validity of equation (4.18) because it determines $P$ and $\tau$ simultaneously from the dynamic waveform. Consequently, only a narrow band of current frequencies allows an accurate parameter extraction. In our example, only the measurement at 3.6 kHz was successful. Experiments with slightly different test current frequencies have been performed (2.5 kHz and 5.5 kHz; not shown here). Their evaluation showed considerably less accurate results, similar to the ones shown for 840 Hz and 8.3 kHz. The graphical implementation C4 of method C produces a certain intrinsic averaging by the straight line fitting but has strong error propagation for $\tau$ and is thus very sensitive to arc fluctuations. Consequently, it requires considerable efforts in implementation to deal with all the numerical problems and a manual identification of poorly extracted points.

Reliable evaluation of $P$ and $\tau$ with low scatter can be done using the new method D1. From a single experiment, it is possible to determine the arc characteristic over of the entire conductance range. From figure 8.4a) it can be seen that the arc voltage is not
constant during the constant current phase. This corresponds to arc elongations, movements, or changes in the temperature distribution [SO91]. Thus only an average arc characteristic can be obtained from the evaluation. However, from figure 8.5b) it can be seen that it is even possible to distinguish these different states of the arc from repetitive experiments. Of the four different experiments, three different cooling powers have been determined, which indicates three different arc lengths at the instant of the measurement. The lowest value, which corresponds to the shortest arc length, is found in two experiments. These differences in the state of the arc originate from the period before the staircase-like test current is applied, as a low current arc is burning for 100 ms before the actual start of the experiment to minimize the effects of the evaporated ignition wire.

In addition to an improved accuracy in the arc parameter determination, method D1 is the only one that allows to judge if the chosen model equations are reasonable. From the exponential shape of arc voltage measurement in response to the current step of the presented experiments it can be confirmed that the behavior only depends on the deviation of the arc conductance from its stationary state value and that it can be described with a single time constant $\tau(g)$. The chosen power law relation for $P$ and $\tau$ with respect to $g$ holds true for the arc configuration presented, however, this may be different for other arcing conditions and always needs to be carefully checked. Of course, all the benefits described above come at the expense of a substantially more challenging task to create the necessary current waveforms of a specific shape.

8.1.4. Conclusion

The most important challenge in black-box modeling is the choice of a parameter function to describe the arc and the determination of the parameter values for all states of the arc. The limitations of conventional parameter determination methods from experiments with sinusoidal test currents have been demonstrated by the example of a free-burning arc. These are mainly three problems: a) the
sensitivity of the classical methods to arc fluctuations, b) the limited spread of \( \dot{I} \) in sinusoidal test currents and consequently the limited amount of information that can be extracted, and c) the necessity to make an a priori assumption of parameter function and model equation without the possibility of validation.

An improved direct method to determine the arc characteristic by using more complex current waveforms is proposed. It was demonstrated on the example of a free burning arc that it is possible to determine the arc characteristic with high accuracy. It was even possible to distinguish different states of the arc in consecutive identical experiments. In addition, it was possible to confirm that commonly used arc model equations represent suitable descriptions of the arc characteristics.

The improved parameter extraction, made possible by using complex current waveforms, is expected to become very helpful in future research on high current arcs. Other current waveforms besides the current steps may also be beneficial for more precise or more efficient arc parameter determination. Parameter extraction with different current waveforms will be compared in the following section.

8.2. Optimal current waveform for arc characterization

8.2.1. Results

A large number of experiments were performed for a wall-stabilized arc with identical setup but with different current waveforms. All current waveforms have been created by means of the arbitrary current source described in section 6.1. The model circuit breaker, described in section 6.3, has been used for the experiments, equipped with a large nozzle of 50 mm throat length.

After each measurement series, the nozzle diameter was measured. It widened after more than 150 experiments from 20 mm to 27 mm. The nozzle widening led to an arc voltage decrease by \( \sim 100 \) V. This
effect was also observable in the extracted parameter functions \( P(g) \) and \( \tau(g) \). The increasing nozzle diameter was associated with a decreasing \( P \) and a doubling of \( \tau \) from \( 12 \mu s \) to \( 22 \mu s \). The upper electrode length became shortened by approximately \( 0.5 \) mm per experiment and the electrode was replaced regularly. However, the resulting increase in gap length from \( 10 \) to \( 11 \) cm caused only negligible effects on the measurement results.

**Quasi-stationary arc characteristic**

The stationary arc characteristic was investigated for an arc in a 23 mm diameter nozzle. A slowly rising current gradient of \( \dot{I} = +0.35 \) A/ms followed by a falling gradient of \( \dot{I} = -0.1 \) kA/ms was applied to the arc (cf. figure 8.7a)). The current gradients are small enough that no transient effects are observable but the arc voltage follows closely its quasi-stationary characteristic. In the voltage signal, stochastic arc fluctuations of \( 20 - 50 \) V amplitude and \( 5 - 6 \) kHz frequency are observable. A clearly detectable minimum voltage exists to which the arc returns regularly after a fluctuation.

By statistical methods, the quasi-stationary arc characteristics \( U(I) \) were determined from the transient current and voltage waveforms of nine similar low current gradient experiments (cf. figure 8.7b)). The mean arc voltage shows the typical falling characteristic at currents below \( 0.3 \) kA and a moderately rising voltage at high currents. Due to arc fluctuations, and up to \( 25 \) V variations between different experiments, a band of up to \( 150 \) V between minimum and maximum voltage at a given current resulted. The standard deviation is approximately \( \pm 25 \) V. Therein, no hysteresis between rising and falling current slope was observed.

In figure 8.7c), short current spikes are superimposed to a \( 0.4 \) kA quasi stationary current. During the constant current interval the arc voltage varies within a band of \( 30 \) V. Thus, a steady state arc voltage exists only in average. During the current spikes, transient over-voltages outweigh the stochastic fluctuations so that a smooth voltage curve results. This is only the case because the spike current
8.2 Optimal current waveform for arc characterization

Figure 8.7.: Experimental results for current waveform variation: a) Quasi stationary arc voltage during a low gradient current slope, b) Statistically averaged voltage fluctuations in quasi-stationary U(I)-characteristics of 9 low gradient experiments, c) Quasi-constant current with current spikes superimposed
gradient and amplitude are sufficient high. Otherwise it would be difficult to distinguish between transient over voltage and arbitrary voltage fluctuations.

**Dynamic arc characteristic**

With the same measurement setup and a nozzle diameter of $24 - 25$ mm, the transient arc voltage during high gradient current spikes was investigated. Rising current slopes are of the order $\dot{I} = 2 - 36$ kA/ms, the falling slopes are a factor 3 smaller. A constant current $I_0$ was applied for a time of 0.9 ms before the spike to ensure quasi-stationary conditions. Despite of this measure, stochastic fluctuations still lead to variations in the start voltage before the current spikes. In the example of figure 8.7c), transient voltage variations in a band of $+100, -20$ V around the stationary voltage are excited by the current slopes. In comparison to that, stochastic fluctuations are of smaller amplitude.

With a large number of similar experiments, the effect of start current $I_0$ (cf. figure 8.8a)), current gradient $\dot{I}$ (cf. figure 8.8b)) and spike amplitude $\Delta I$ (cf. figure 8.8c)) on the transient $u(i)$ characteristic was measured. In the first few $\mu$s of all current spikes, the arc voltage increases linearly with the current $u(t) \sim i(t)/g_0$. This is so because the arc had no time to change its conductance. Start current $I_0$ affects the amplitude of transient over-voltage because of a different start voltage and because of a different steepness of the ohmic slope. By that, up to four times higher transient voltages are found at low currents in comparison to high start currents (cf. figure 8.8a)). An increase in current gradient $\dot{I}$ reduces the total conductance change during the spike and therefore brings the transient $u(i)$ closer to the formerly constant conductance slope. Up to the measured current gradient, this causes a widening of the enclosed area by the transient loop in the $u(i)$ diagram. An increase in spike amplitude increases the conductance change during the spike. This deforms the transient $u(i)$ shape because the parameters $P$ and $\tau$ are not constant for different conductance values. This effect was
8.2 Optimal current waveform for arc characterization

Figure 8.8.: Effect of current waveform on the transient \(ui\)-characteristics: a) Effect of start current amplitude \(I_0\) at \(\dot{I} = 36 \text{ kA/ms}\) and \(\Delta I = 0.5 \text{ kA}\), b) Effect of current gradient \(\dot{I}\) for a \(\Delta I = 0.27 \text{ kA}\) and \(I_0 = 0.4 \text{ kA}\), c) Effect of spike amplitude \(\Delta I\) for \(\dot{I} = 22 \text{ kA/ms}\) and \(I_0 = 0.4 \text{ kA}\)
observed much stronger in the *falling* region of the stationary *UI*-characteristics.

**Experiment results with staircase-like and spike currents**

For arc parameter determination, different current waveforms were applied to a wall-stabilized arc in a 21 mm diameter nozzle. Here, only staircase-like currents (cf. figure 8.10 a)) and spike currents superimposed to a moderately increasing current (cf. figure 8.10b)) are shown. With the current source, staircase-like currents with maximum $\dot{I} = 16$ kA/ms and a step size of 0.2 kA were imposed. The spikes have rising and falling gradients of 29 kA/ms and $-7$ kA/ms, respectively, and their amplitude $\Delta I$ grows with increasing stationary current.

Method D1 was used to extract the parameter functions $P(g)$ and $\tau(g)$ of the experiments with staircase-like current. The results of eight identical experiments have been combined. Method D2 was used to extract the parameters from the experiments with spike currents. The results of four identical experiments have been combined. Method D2 has also been used to extract the parameters from experiments with other current waveforms (not shown here). The extracted parameters $P(g)$ (shown in the form $U(I)$) and $\tau(g)$ are mostly identical for both current waveforms (cf. figure 8.10c)-e)). At $0.2 - 0.4$ kA, the stationary arc voltage of the staircase-like current exceeds the one of the spike current because therein, the staircase-shape could not be created perfectly. This excited undesired transients in this region. With the spike current, four times more extracted data points could be generated per experiment. The resulting $4 \mu$s stray band of $\tau$ for the spikes is a factor 2 smaller than that of the staircase-like current.
Figure 8.9.: Measured voltage and current during a staircase-like test current a) and during a moderately rising current with overlayed spikes b)
Figure 8.10.: Comparison of extracted parameters from staircase and spike experiments: a) stationary characteristics $P(g)$, b) stationary characteristics $U(I)$, c) thermal inertia $\tau(g)$
8.2 Optimal current waveform for arc characterization

Figure 8.11.: Parameter determination accuracy vs. current shape: a) Effect of spike amplitude $\Delta I$ on $\tau$ accuracy for $\dot{I} = 25$ kV/cm at falling and at rising $UI$-characteristic, b) Effect of spike gradient $\dot{I}$ on $\tau$ accuracy for $\Delta I = 0.2$ kA in falling region and $\Delta I = 0.25$ kA in rising region, c): Average error of recalculated transient voltage waveform in % relative to the difference of maximum and minimum transient voltage.
Effect of current gradient and spike amplitude on parameter accuracy

A large number of current spikes as in figure 8.7c) were applied to a 20 mm wall-stabilized arc to investigate the effect of spike shape on the standard deviation of \( \tau \). The influence of current gradient \( \dot{I} \) and spike amplitude \( \Delta I \) in the falling region \( I_0 = 0.15 \text{ kA} \) and in the region with rising characteristics \( I = 0.4 \text{ kA} \) was investigated (cf. figure 8.11). One data point corresponds to four similar experiments with 15–20 identical current spikes each. It became clear that wrong \( \tau \) values result for current gradients \(< 3 \text{ kA/ms} \), the stray band of \( \tau \) narrows significantly towards gradients of 25 kA/ms. A higher spike current amplitude in the rising region leads to a higher \( \tau \) value because the \( \tau \) value is measured, thereby, at a higher conductance. In the falling region of the arc characteristic as well as in the rising region, an optimal spike amplitude exists with the narrowest \( \tau \) stray band. The optimal amplitude \( \Delta I \) has approximately the same value as the quasi-stationary current \( I_0 \) before the spike.

With the parameters determined, the transient voltage was re-simulated by an integration of equation (4.18) individually for each spike. In figure 8.11c), the average RMS error between the measured voltage and the re-simulated one relative to the difference between maximum and minimum transient voltage occurring during a spike is plotted. A minimum error resulted for an amplitude \( \Delta I = 0.2 \text{ kA} \) in the falling region and an amplitude of \( \Delta I = 0.3 - 0.5 \text{ kA} \) in the rising region. In both regions, recalculations of the voltage waveforms showed the best results with respect to the RMS error for a gradient of \( \dot{I} = 15 \text{ kA/ms} \).

8.2.2 Discussion

Black-box modeling accuracy: For correctly determined arc parameters, the agreement between measured voltage waveform and re-calculation by integration of equation (4.18) was extremely good. Generally errors \(< 1 \% \) were achieved. This confirms that the en-
ergy balance equation used is suitable to predict transient arc volt-
ages. Larger errors resulted if stochastic fluctuations occurred either
in the transient phase or between the instant where the stationary
point was determined and the subsequent current slope. Because of
the inability to identify very small fluctuations, an average error of
$2 - 4\%$ for the transient voltage resulted.

*Arc fluctuations:* In nearly all constant current intervals and low
gradient slope intervals with duration $> 100\, \mu s$, stochastic arc fluc-
tuations were present. Up to $20\%$ of all current steps and spikes
showed voltage fluctuations also during the transient phase. In-
tervals of the voltage curve that contain such fluctuations are not
described correctly by equation (4.18) and can lead to wrong $P$ and
$\tau$ values. Typically, a too high time constant results. Therefore, the
arc must be treated as a stochastic process and the quality of a pa-
rameter determination method depends on how well it is possible to
exclude or average out such fluctuations. A large number of repet-
itive experiments usually results in a stray band of $P(g)$ and $\tau(g)$. This can be combined with an algorithm to identify large fluctua-
tions and exclude such sections from parameter determination. In
the present implementation this was firstly achieved by re-simulation
of the voltage waveform and secondly by identification of unexpected
maxima and minima in the arc voltage during the constant gradient
current slope.

*Momentary quasi-stationary point:* Transient voltages created by
current slopes are superimposed to the instantaneous quasi-stationary
state and not to any averaged stationary curve. This stands in agree-
ment with the observations of [MSS80]. Unless a fluctuation occurs
during a current spike, a higher quasi-stationary start voltage leads
to a constant voltage offset during the whole transient phase. For
this reason, $\tau$ must be determined with respect to the instantaneous
stationary characteristic. This is the case with higher probability,
the closer the quasi-stationary point is determined to the subse-
quent current slope, from which $\tau$ is extracted. For this reason, Ri-
janto’s [Rij75] approach to merge different gradient slopes from two
experiments for parameter determination is likely to cause higher stray bands of $\tau$.

**Optimum step/spike amplitude:** A minimum current increase is required at a given current gradient so that the transient over-voltage significantly exceeds the band of stationary arc fluctuations. An upper limit of current step amplitude results from the chosen method. The assumption of constant $\tau$ and linearly increasing $P$ during a transient current does not hold true anymore, if the conductance changes too much during transient currents. To overcome this limitation, the determination algorithm was also implemented in more complex forms including linearly rising $\tau$, but too frequently showed convergence problems. The conductance variation within a current step is typically larger than the one of a spike because the spike reaches its maximum conductance value not at current peak but during the falling slope. Therefore, step amplitudes should be chosen smaller than spike amplitudes.

**Optimum current gradient:** The experiments have shown that for spikes and steps, $\tau$ is determined with best accuracy for $\dot{I} = 15\, \text{kA/ms}$. At higher gradient spikes the effect of $\tau$ on the transient voltage decreases because the rising current slope is too short for the conductance to change sufficiently. Also, at higher gradients, the dynamically determined stationary point at $\max(g)$ has a higher stray band. For steps with very high current gradient, $\tau$ can also be determined from the transient voltage at constant current right after current rise. The second stationary point would then be determined from the same constant current but at a time $> 5\tau$ after the step. This strategy allows using significantly higher current gradients and would probably lead to even better accuracy of $\tau$. However, technical constraints of the current source limited the achievable current gradient to $\dot{I} = 15\, \text{kA/ms}$ if subsequently a constant current has to be created lasting $100\, \mu\text{s}$.

**Optimal current waveform:** The above considerations clearly identify the staircase-like currents to be the theoretically optimal current waveform for parameter determination because it completely
decouples $P$ and $\tau$ determination. However, due to the limitation in current gradient, better results were achieved with the spikes superimposed on a slowly rising current slope. The fact that transient and stationary behavior of the arc should be determined time-wise close to each other demands current waveforms that combine low current gradient sections and high gradient sections in a short interval. Apart from the proposed waveforms, several other current pulse shapes would fulfill this condition. Among them sawtooth currents and high gradient staircase-like currents with significant overshoot have been created for arc characterization as well. They showed only slightly wider stray bands for $\tau$ than in the measurements presented.

With the available current source, the optimal current waveforms are spikes because a large number of spikes can easily be created at different amplitudes in a single experiment. For experiments with arcs at several 10 kA, such spikes could also be superimposed to a 50 Hz sine waveform, probably with comparable results.

8.2.3. Conclusion

The novel arbitrary current source has made it possible to create complex current waveforms including staircase-like and spike currents. By that, the arc parameters can be determined with a smaller stray band of a lower number of experiments required. The arc power loss $P(g)$ is ideally determined from constant current sections. The arc thermal inertia $\tau(g)$ can be determined either from moderate current slopes or at constant currents subsequent to a very large slope. Therefore, an optimum current waveform combines repetitive constant current sections and high current gradients very closely together. This clearly favors a series of small current steps with as large as possible current gradient and constant current sections $> 5\tau$ for arc parameter determination. In the experiments, such waveforms were technically limited to a gradient of 15 kA/ms. At this gradient, current spikes superimposed on a current slope with gradient $< 1$ kA/ms achieved much smaller scatter in $P$ and $\tau$. $\tau(g)$ could be determined at 20 different conductance values with a stray band of
< 5 µs from a single experiment. Best results were achieved for spike amplitudes of the magnitude of the previous quasi-stationary current. The accurate transient voltage recalculation with error 2–4% for correctly determined parameters confirmed the validity of the energy balance equation for the wall-stabilized arc investigated.

The improved determination accuracy now allows a better relative comparison of similar arc chamber configurations using their black-box parameters. By that, nozzle shape optimization of AC and DC circuit breakers for dynamic processes, such as passive resonance or self blast pressure build-up, is possible more easily.
9. Arc chamber characterization

In this chapter, arcs with different chamber parameters are systematically characterized via their black-box arc parameters. In section 9.1 the comparison is performed for varying blow pressure, nozzle diameter, throat length, blow gas and nozzle material. The arc cross-section variation at varied current amplitude and varied blow pressure is investigated in section 9.2.

9.1. Effect of arc chamber parameters on black-box characteristic

9.1.1. Experiment configuration

For the experiments, the arbitrary current source (cf. section 6.1) and the flexible model circuit breaker (cf. section 6.3) have been used. In section 8.2, it was shown that a slowly rising or falling slope (\(|\text{d}i/\text{d}t| < 1 \text{kA/ms}\)) with superimposed triangular current spikes (\(|\text{d}i/\text{d}t| \sim 15 - 30 \text{kA/ms}\)) enables to determine \(P(g)\) and \(\tau(g)\) with sufficient accuracy and to recalculate the measured transient voltage waveform from the extracted black-box parameters with < 2% error. The current waveforms created consist of low gradient current slopes with high gradient spikes superimposed. A large number of spikes and quasi-constant currents have been generated with various current amplitudes with different conductance \(g\). Usually four identical experiments were performed per configuration with 20 – 30 spikes each, to compensate for stochastic phenomena influencing the
results. The improved parameter determination method D2 of section 7.2.3 has been used for arc parameter extraction.

**Blow pressure:** An experiment series with a wide throat nozzle $d_T = 25 - 26$ mm diameter was performed for various blow pressures of $p_H = 0.9 - 15$ bar. The large nozzle cross-section results in current densities $< 3$ A/mm$^2$, where ablation is small [STCA06].

**Nozzle Diameter:** An experiment series was performed with cylindrical nozzles with a throat length $L_T = 50$ mm for different nozzle throat diameters of $d_T = 6 - 25$ mm at a low and a high blow pressure of $p_H = 1.5$ bar and $p_H = 7.7$ bar, respectively. The filling pressure of the bottles was adjusted for each experiment, so that the desired chamber pressure resulted at the end of the low current phase right before the rising slope with spikes. The measured current range $0.1 - 2$ kA in combination with different throat diameters resulted in current densities of $j = 1 - 70$ A/mm$^2$.

**Throat length:** An experiment series with throat length varied in the range of $L_T = 10 - 75$ mm was performed for a) a narrow nozzle $d_T = 7 - 8$ mm without forced convection, b) a narrow nozzle $d_T = 6 - 7$ mm with moderate blow pressure $p_H = 3$ bar and c) a wide nozzle $d_T = 25$ mm with strong blow pressure $p_H = 9$ bar. In the experiments, the electrode gap width was adapted to maintain the throat outlet to downstream electrode distance at $x_{out} = 20 - 30$ mm. Unlike case c) with $j < 4$ A/mm$^2$, in cases a) and b) the current densities exceeded $40$ A/mm$^2$ at high currents and major nozzle diameter change from wall ablation was observed.

**Blow gas:** An experiment series with cylindrical nozzles of diameter $d_T = 12 - 13$ mm and throat length $L_T = 50$ mm was performed for three different blow gases: dry air (air), carbon dioxide (CO$_2$) and helium (He). The absence of an encapsulation restricted the candidate gases to those that could be freely released into the open air. He was chosen because it has three times larger speed of sound $c_S$ and ten times lower density $\rho$ at a given pressure than CO$_2$ and air. CO$_2$ was chosen because it has a specific heat $C_P$, $20\%$ lower than air. The parameters of air, CO$_2$ and He in Table 9.1 for normal
conditions are used to set convective and conductive coefficients in equation (4.3) in relation to each other. However, the relationships may be different at plasma temperature and varied pressures.

For the air and CO\textsubscript{2} case, the blow pressure decreased during the experiment from 5.4 bar to 5.1 bar. In the He experiment, the pressure decreased from 5.9 bar to 5.1 bar, due to the higher flow velocity of He. In this way, the blow pressures of He were always higher than the ones of air and CO\textsubscript{2}.

Table 9.1.: Gas parameters [Too]

<table>
<thead>
<tr>
<th></th>
<th>normal conditions 1013 mbar, 293° K</th>
<th>air</th>
<th>CO\textsubscript{2}</th>
<th>He</th>
</tr>
</thead>
<tbody>
<tr>
<td>thermal conductivity ( k ) in W/K</td>
<td>26.2</td>
<td>16.8</td>
<td>156.7</td>
<td></td>
</tr>
<tr>
<td>density ( \rho ) in kg/m\textsuperscript{3}</td>
<td>1.2</td>
<td>1.8</td>
<td>0.16</td>
<td></td>
</tr>
<tr>
<td>speed of sound ( v_s ) in m/s</td>
<td>331</td>
<td>259</td>
<td>965</td>
<td></td>
</tr>
<tr>
<td>specific heat ( C ) in kJ/(kgK)</td>
<td>1.01</td>
<td>0.84</td>
<td>5.19</td>
<td></td>
</tr>
<tr>
<td>conduction coef. ( X_1 := \frac{k}{k_{\text{air}}} )</td>
<td>100%</td>
<td>64%</td>
<td>600%</td>
<td></td>
</tr>
<tr>
<td>convection coef. ( X_2 := \frac{(\rho C v_s)}{(\rho C v_{s})_{\text{air}}} )</td>
<td>100%</td>
<td>99%</td>
<td>200%</td>
<td></td>
</tr>
</tbody>
</table>

**Nozzle material:** An experiment series with nozzles of identical geometry but varied material was performed. The nozzles had a cylindrical throat \( d_T = 6 \) mm of \( L_T = 50 \) mm and a cylindrical inlet and outlet section of diameter \( d_{\text{in}} = d_{\text{out}} = 30 \) mm and length \( L_{\text{in}} = L_{\text{out}} = 5 \) mm. Only the material of the core has been varied. The material of the inlet and outlet section was always PMMA. The narrow nozzle ensured high current densities \( > 50 \) A/mm\textsuperscript{2} in the throat, where ablation was expected. The current density in the inlet and outlet section was below 3 A/mm\textsuperscript{3}. Therefore, it can be assumed that the PMMA sections did not contribute to the ablation and thus their material is not affecting the experiment. A low blow pressure of \( p_{H} = 1.7 \) bar was chosen, so that ablation is present already at low currents. The materials PMMA, PTFE (Lubriflon391), Macor (ceramic) and quartz glass (GE124) were used. PMMA was used as a reference material, as it was also used in the other investigations. It has a thermal conductivity of \( 0.17 - 0.9 \) W/(K m).
PTFE is the standard nozzle material in HVAC arc chambers. Maccor is a very temperature resistant glass-ceramic, which has already been gased out in the process of manufacturing and has nine times higher thermal conductivity than PMMA. It does not release gases below 800°. Quartz glass has a softening temperature of 1683°C and is therefore expected not to ablate.

9.1.2. Typical experiment

The current and voltage waveforms of two typical experiments with a narrow nozzle $d_T = 6\,\text{mm}$ and a throat length $l_T = 50\,\text{mm}$ are shown in figure 9.1. The plot shows a high pressure $p_H = 9\,\text{bar}$ experiment (cf. figure 9.1a)), a low pressure $p_H = 1.6\,\text{bar}$ experiment (cf. figure 9.1b)) and a zoom of the high pressure experiment (cf. figure 9.1c)). In both experiments, similar current waveforms were applied with a rising current gradient $0.2\,\text{kA/ms}$ and a falling current gradient of $0.05 - 0.10\,\text{kA/ms}$. The superimposed spikes have a rising gradient of $\dot{I} = 8 - 20\,\text{kA/ms}$ and a falling gradient of $\dot{I} = 6 - 8\,\text{kA/ms}$.

At high blow pressure, a pressure decrease of $\Delta p_H = 0.2\,\text{bar}$ is observed during the time of measurement, due to the escaping cooling gas. In the low pressure experiment, a significant interaction of the arc and the chamber pressure ("backheating") is observed. $p_H$ is increased by the arc from 1.7 to 3 bar. Even the bottle pressure is increased from 1.7 to 2 bar as a consequence of the ablated gas and the flow reversal. In consequence, the peak pressure in the pressure chamber is reached $\sim 1.5\,\text{ms}$ after the current maximum. To avoid that the rising current branch affects the falling one, the peak current is maintained for 3 ms before the falling current slope is applied. Despite that, a small difference results between the rising and falling arc $UI$- characteristics because of non identical blow pressure.
Figure 9.1.: Experimental results with a narrow nozzle ($d_T = 6\, \text{mm}$, $d_L = 50\, \text{mm}$) a) high pressure ($p_H = 9\, \text{bar}$) experiment with strong forced convection, b) low pressure ($p_H = 1.7\, \text{bar}$) experiment with strong backheating, c) zoom of plot a)
Figure 9.2.: Effect of blow pressure in a wide nozzle ($d_T = 25\text{ mm}, L_T = 50\text{ mm}$): a) stationary characteristics $U(I)$ and parameterizations $P = 1\text{ W} \cdot a_{pg}(p/(1\text{ bar})) \cdot (g/(1\text{ S}))^{b_{pg}}$ (dashed curves), b) thermal inertia $\tau(g)$.
9.1.3. Effect of blow pressure

In a first set of experiments, the effect of the blow pressure on the arc characteristic has been investigated for currents up to 1.2 kA. A large throat diameter ($d_T = 25$ mm) was used to avoid ablation effects. At high blow pressures, only falling $UI$-characteristics are observable. At lower blow pressures, also a flat section with constant voltage was measured. An increase of the blow pressure increases the arc voltage, shifts the current amplitude $I_T$ with minimal arc voltage to higher amplitudes and increases the steepness of the falling characteristics (cf. figure 9.2a). The thermal inertia is only slightly increased with increasing blow pressure ($\approx 1 - 2 \mu s$) and rises moderately with conductance.

The falling characteristic can be accurately described by the function:

$$P = 1 \text{ W} \cdot a_{pg} \left( \frac{p}{1 \text{ bar}} \right) \left( \frac{g}{1 \text{ S}} \right)^{b_{pg}}$$

with $b_{pg} = 0.5$ and $a_{pg} \approx 67 \cdot 10^3$. The fits are indicated by dashed curves in figure 9.2a). The fit is only valid for the falling characteristics but not for the flat and rising characteristics, because it cannot describe a voltage minimum.

In figure 9.2b) the thermal inertia is shown for an arc blown with 0.9 bar, 1.9 bar, 15.5 bar. The results show a very small effect of blow pressure $p_H$ and of conductance $g$. The mean value of the evaluated $\tau$ curves increases $1 - 2 \mu s$ for a blow pressure increase by a factor of sixteen. An increase of conductance from $1 \text{ S}$ to $3 \text{ S}$ leads to an increased mean value of $\tau$ by only $2 - 3 \mu s$. In both cases, the error bars widely overlap.

9.1.4. Effect of nozzle diameter

For the measured currents $< 2 \text{ kA}$, the effect of nozzle diameter on the arc characteristics at high (7.7 bar) and low (1.5 bar) blow pressure is shown in figure 9.3. As can be seen, the effect of the
Figure 9.3.: Effect of nozzle diameter ($L_T = 50$ mm): a) stationary characteristics $U(I)$, b) thermal inertia $\tau(g)$
diameter is significantly different at low pressure compared to high pressure. At high blow pressure $p_H = 7.7$ bar, a diameter variation between 6 and 30 mm has a minor effect on the $U(I)$ characteristics and on $\tau(g)$. Both experiments showed nearly identical falling characteristics (cf. figure 9.3a)). At low blow pressures $p_H = 1.5$ bar, $\tau$ increases significantly with increasing diameter and the shape of the $UI$-characteristics change drastically. While for the current region of the measurements no distinct rising characteristics are observed for the 11 mm nozzle at 1.5 bar, strongly rising characteristics result for the 6 and 8 mm experiment already from 500 A and 750 A, respectively. In the small and medium nozzle diameter experiment, the slope in the rising region is largely identical, it does, however, set in at lower currents for the 6 mm nozzle.

Thermal inertias $\tau$ of experiments without rising characteristic showed an approximately square root increase of $\tau$ with conductance that saturates between $10-14 \mu$s. Experiments where the current was sufficiently high or the diameter sufficiently small to create a rising characteristic, do show a maximum of $\tau$ at a conductance value corresponding to the transition current $I_T$. For larger conductances, $\tau$ decreases to values of $3-5 \mu$s (cf. figure 9.3b)).

The observation was made that a larger nozzle widening from test to test occurred for narrow nozzles with low blow pressure. The 6 mm nozzle at 1.5 bar blow pressure widened up to 0.25 mm per test. For this reason only two tests were evaluated for this configuration instead of four in all other experiments. In the high pressure experiments and the wide nozzle experiments the nozzles widened much less.

9.1.5. Effect of throat length

For currents $< 2$ kA, the effect of throat length at an experiment without forced convection, with moderate, and with high blow pressure is shown in figure 9.4. With increasing throat length, the arc voltage and the steepness of the rising characteristic increases, and the transition current $I_T$ reduces. In the $p_H = 0$ bar experiment (cf.
Figure 9.4.: Effect of throat length on stationary characteristics and thermal inertia: a-b) no forced convection ($p_H = 0 \text{ bar } d_T = 7 - 8 \text{ mm}$), c-d) weak forced convection ($p_H = 3.1 \text{ bar } d_T = 6 - 7 \text{ mm}$), e-f) strong forced convection ($p_H = 9 \text{ bar } d_T = 25 \text{ mm}$)
9.1 Black-box characteristic

Figure 9.4a-b)) the characteristics is *rising* from around 200 A. Their steepness is ten times intensified by increasing $L_T$ from 10 to 75 mm. Significant ablation and nozzle widening is observed from test to test. Interestingly, short nozzles widened more strongly, compared to such with long $L_T$.

In the 3.1 bar experiment, forced convection and ablative convection is present. A pressure increase in the heating chamber occurred in the 75 mm and 50 mm experiment but not in the 25 mm and 10 mm experiment. The two experiments where it occurred showed a distinctly *rising* characteristic above 500 A (cf. figure 9.4c)). In the experiments with shorter throat, no *rising* characteristics were observed up to the maximum applied current.

In the high pressure experiment (cf. figure 9.4e)), no flow reversal occurred and no *rising* characteristics were observable up to the maximum current. However, the 75 mm nozzle showed flat characteristics above 1.5 kA. Despite the voltage increase with throat length, the steepness of the *falling* characteristic is not affected by $L_T$. In comparison to the low pressure experiments, much smaller nozzle widening was observed at high pressure.

Also the $\tau(g)$ curves are mainly unaffected by $L_T$ at low conductance. Deviations for different $L_T$ are observable in the non blown and weakly blown case (cf. Figures 9.4b) and 9.4d)). Experiments with including a *rising* section of the $UI$-characteristic showed a decreasing $\tau$ at high conductance. In experiments without a *rising* characteristic, $\tau$ rises also at high conductance. The increased voltage at strong forced convection caused that smaller conductance values were observed with identical current waveforms.

### 9.1.6. Effect of blow gas type

A $1 - 2\mu s$ higher $\tau$ value of air results for $g > 1.25$ S compared to CO$_2$ and He, but the scatter bands overlap.

The type of blow gas, and with it the conductive and convective parameters have been varied for a strongly blown arc (cf. figure 9.5). Despite the large differences in thermal conductivity, speed of sound
Figure 9.5.: Effect of blow gas ($p_H = 5.2$ bar, $d_T = 12$ mm): a) stationary arc characteristics $U(I)$, b) thermal inertia $\tau(g)$
and gas density, the gas type only shows a small influence on $U(I)$ and $\tau$. At $I > 1\, \text{kA}$, where flat $UI$-characteristics are present, the arc voltages of air, CO$_2$ and He are identical. At low currents, CO$_2$ has the highest voltage and lies 300 V above that of He, which is lowest.

9.1.7. Effect of nozzle material

The effect of nozzle throat material at low blow pressure ($p_H = 1.7\, \text{bar}$) on the arc characteristic has been investigated for a PMMA, a PTFE, a Macor and a Quartz glass nozzle (cf. figure 9.7). The PMMA and Macor nozzles widened 1 mm in 6 experiments. The PTFE nozzle widened 0.5 mm in 4 experiments and the widening of the quartz glass nozzle in 4 experiments is below the measurement tolerance of 0.25 mm. In all experiments, an increase of chamber pressure $p_H$ by $1 - 1.5\, \text{bar}$ resulted and also the bottle pressure $p_B$ was increased by $0.2 - 0.3\, \text{bar}$ (cf. Figures 9.6a and 9.6b)). The highest increase was observed for Macor, the lowest increase resulted for quartz glass, even though a 250 A higher current was created for the quartz experiment.

Despite the significantly different nozzle widening, the $UI$-characteristics of all nozzle materials are very much alike. All nozzles showed a distinctly falling characteristic and a distinctly rising characteristic. PMMA and quartz have the highest arc voltage in the falling region. In the rising region, however, quartz has a significantly lower arc voltage. In addition, the transition point $I_T$ of quartz is $200 - 250\, \text{A}$ higher than the one of the other materials.

The thermal inertia $\tau(g)$ of Macor in the falling region is $4\, \mu\text{s}$ higher than the one of the other materials. The $g$ value with maximum $\tau$ for quartz is 1 S higher than the one of the other materials.
Figure 9.6: Effect of nozzle material: a) pressure chamber pressure, b) bottle pressure, c) arc voltage, d) arc current
9.1 Black-box characteristic

Figure 9.7.: Effect of nozzle material ($p_B = 1.7$ bar, $d_T = 6$ mm): a) stationary arc characteristics $U(I)$, b) thermal inertia $\tau(g)$
9.2. Arc cross-section variation

The arbitrary current source (cf. section 6.1) and the model circuit breaker (cf. section 6.3) have also been used for the following experiments. An optical $A_V$, electrical $A_E$ and fluid dynamical cross-section $A_P$ have been determined for an arc stabilized by forced convection. The cross-sections are defined in section 4.2.4, the methods for their determination have been provided in section 6.6. In the experiment, a very large nozzle was chosen, so that less than 25% of the nozzle was filled by the arc. Thus, the arc can be assumed as convection stabilized without constricting effects from the surrounding nozzle.

9.2.1. Example results

An example measurement with blow pressure $p_H = 2.5 - 3.5$ bar in the period of interest is shown in figure 9.8. The current is kept constant at $i = 450$ A with a ripple of 50 A for a time of 60 ms (cf. figure 9.8a)). At $t = 60$ ms, the remaining energy stored in the test circuit is no longer sufficient to maintain the current and the arc extinguishes. In the section of decreasing current, the arc voltage $u$ increases. It shows the typical falling characteristic for blown arcs [NNH+01,BKM+70] This can be observed from the rising voltage at decreasing current.

In the interval $20 - 60$ ms, a slow decrease of chamber pressure $p_B$ is observed (cf. figure 9.8b)), the bottle pressure $p_B$ is 0.25 bar higher than $p_H$. The decreasing pressure in $p_H$ creates a moderately decreasing arc voltage in this interval. The evaluated arc diameters are compared in figure 9.8c). In former interval of decreasing pressure, an increasing diameter was observed with all three measurement methods.

In contrast to the reference measurement without arc, a ripple in $p_H$ is observable, which is proportional to the current ripple. This ripple is likely to be caused by the dynamic cross-section change with current but it lies below the guaranteed dynamical sensor accuracy.
Figure 9.8.: Arc diameter results of a convection stabilized arc: a) current and voltage, b) bottle and chamber pressure, c) arc diameter: visual $d_V$, fluid dynamical- $d_p$, electrical- $d_E$ and theoretical $d_T$. 
Figure 9.9: Arc cross-section variation with current for $p_H = 3$ bar. (visual $A_V$, fluid dynamical $A_P$, electrical $A_E$ and theoretical cross-section $A_T$).

The simple theoretical model predicts a diameter of $d_T = 3$ mm. This is in very good agreement with the electrically and optically determined diameters. The electrical diameter $d_E = 2.5 - 3$ mm is slightly smaller than $d_T$, the optical diameter $d_V = 3 - 4$ mm is slightly larger. Considerably stronger fluctuations in $d_V$ are observable compared to the variations in $d_T$ during the current ripple. The fluid dynamic arc cross-section $d_P = 7 - 8$ mm is significantly higher than $d_T$.

**Variation of current:** Five different experiments with blow pressure $p_H = 3$ bar were performed for current amplitudes in the range $i = 200 - 750$ A. The extracted arc cross-sections are compared in figure 9.9: A linear increasing cross-section $A_T$ with increasing current is predicted. The electrical cross-section $A_E$ is in excellent agree-
9.2 Arc cross-section variation

Figure 9.10.: Arc cross-section variation with chamber pressure $p_H$ for $I = 400$ A (visual $A_V$, fluid dynamical $A_P$, electrical $A_E$ and theoretical cross-section $A_T$).

ment with $A_T$. The increase of the optical arc cross-section $A_V$ can be trusted for the two low current experiments only. At higher currents, the increase becomes over proportional and the standard deviation widens because of the observed turbulence. Also the fluid dynamical cross-section increases proportionally with the current, despite it is five times larger than all other cross-section.

**Variation of pressure:** Five identical measurements at 400 A were performed with filling pressures $p_B = 5 - 14$ bar resulting in pressure chamber pressures of $p_H = 3 - 9$ bar. In figure 9.10, the average arc cross-sections $A_V$, $A_P$, $A_E$ and $A_T$ are plotted versus $p_H$. Theoretically, a decreasing cross-section $A_T = p^{-1/2}$ is expected with increasing pressure. This is accurately reflected by the electrical
and visual cross-section. A similar trend is also observable in the fluid dynamic cross-section.

**Visual observations:** In addition to the cross-section determination, several further observations have been made from the video:

- An extreme increase of brightness with increasing $i$ and $p_B$ was observed.

- A significant increase of arc movement with increasing blow pressure was observed.

- A cross-section that decreases downstream (eg. figure 9.11c)). However, the shape might be falsified by an optical distortion of the converging nozzle section. The theoretical prediction for a cylindrical nozzle would be an increasing cross-section $A_T \propto z^{1/2}$ downstream.

### 9.2.2. Discussion and conclusions

Relative variations with pressure and current are reflected correctly from electrical, optical and pressure measurements. However, the absolute cross-sections are ambiguous and describe slightly different physical properties. Of these, only the electrical cross-section represents the current carrying cross-section of the arc and was in agreement with the predictions of Lowke [LL75]. The light emitting column at the nozzle throat is slightly larger than the theoretically expected arc column, because radiation does not decrease as strongly with temperature as conductance does [CGR11]. The fluid dynamic cross-section turned out to be a five times higher, compared to the other cross-sections. It reflects that the flow reduction by the arc is caused by a reduction of effective cross-section. It can be concluded that the flow reduction is not simply caused by the arc column acting as an obstacle that does not contribute to the arc flow. But additional effects, such as turbulence, lead to a much larger reduction of mass flow in the nozzle.
Figure 9.11.: Pictures of arc shape variation: a)-d) Increase of arc diameter and brightness with current $i$, e)-f) Decrease of arc diameter and increase of turbulence with chamber pressure $p_H$. 
The dynamic resolution clearly favors electrical and optical over pressure measurements for arc cross-section determination. Optical observations are in particular beneficial for an interpretation of local phenomena in time and space. A drawback is that settings of different experiments cannot be kept constant easily because of strongly varying brightness.
10. Discussion

In this chapter, the results of the previous chapters are discussed. In the experiments, the theoretically expected falling, flat and rising stationary $U$I-characteristics were confirmed. All experiments have in common that the arc voltage decreases with increasing current at low $I$, followed by a constant voltage section and a rising region were the voltage rises with current $I$. The flat and rising regions were not measured in all experiments, because in some cases the transition current $I_T$ (current amplitude with minimum voltage) was above the maximum current amplitudes which are possible in the present setup.

Amongst all experiments, no case was found where the thermal inertia could be influenced independently from the stationary characteristic. The correlation of $\tau$ with $U(I)$ is reasonable, considering its definition as the ratio $\tau = E/P$ between stored energy and stationary cooling power. Typically, a rising $\tau(g)$ corresponds to a section of falling $U(I)$ characteristic and a falling $\tau(g)$ to a rising $U(I)$ characteristic. Another choice of arc parameters (e.g. $P(g)$ and $E(g)$) may allow better separation of physical effects.

10.1. Blow pressure

The blow pressure of the forced convection has shown to have the highest effect on the stationary power loss $P(g)$ of all parameters investigated. 8 bar blow pressure creates a strongly falling $U(I)$ characteristic and a high arc voltage up to currents exceeding 2 kA even in a narrow nozzle with 6 mm throat diameter (cf. figure 9.3a)). For 1.5 bar blow pressure in the same nozzle a rising characteristic
for currents above 500 A was measured.

An increasing cooling power $P(g)$ proportional to the blow pressure $p_H$ was observed (cf. figure 9.2a)). The proportional dependency is in agreement with measurements on an air blast breaker with blow pressures in the range of $10 - 50$ bar and currents up to $40$ kA [BKM$^+$70]. This is also in agreement with experiments on three MRTB breakers which differ in puffer cylinder diameter [NNH$^+$01]. The experiments showed that the breaker with higher puffer cylinder diameter, and therefore most probably also higher blow pressure, is characterized by higher $P(g)$ values.

In nozzles with large diameter, the thermal inertia $\tau(g)$ is quite independent of the blow pressure. In the experiments shown in figure 9.2b) $\tau$ did increase only by $1 - 2\mu s$ for a blow pressure increased by a factor 15. In narrow nozzles, however, a much stronger effect of $p_H$ on $\tau$ was observed. But the effect was only observed at high currents. The experiments shown in figure 9.3b) with a 6 mm diameter nozzle throat and 1.5 bar blow pressure, a thermal inertia $\tau = 4 - 6\mu s$ was measured at high currents. In the same arrangement but at increased blow pressure of 7.7 bar, a $\tau = 10 - 12\mu s$ was measured.

The observation that more nozzle widening from test to test (due to ablation) resulted in narrow nozzles gives an indication why the pressure dependency of $\tau$ appeared only at high currents with small nozzles throat diameters. In narrow nozzles, the low $\tau$ values at low blow pressure are most probably caused by additional ablative losses that set in at high current amplitudes. In high pressure experiments, these losses would set in at much higher current amplitudes, so that no reduction of $\tau$ was observed up to the measured current amplitude for their case. Up to the measured current amplitude, arcs in wide nozzles are dominated by convective losses, which do increase $\tau$ only minimally with blow pressure.

The increased blow pressure influences different processes in the arc chamber: Firstly, as shown in section 9.2.1, an increase of $p_H$ reduces the arc cross-section $A \propto I \cdot p^{-1/2}$. This effect was observed
for the optical, the fluid dynamical and the electrical cross-section (cf. figure 9.10) and is in agreement with the theoretical predictions of Lowke [LL75]. A higher current is required for the arc to reach a specified cross-section. As a consequence, the current amplitude is increased, at which wall-constriction becomes dominant and strong ablation sets in. This could explain, why the falling characteristic can be maintained up to higher currents, if higher blow pressures are applied.

The observations in section 9.2.1 showed also an increase of arc brightness at increasing blow pressure. This is an indication that also the arc temperature increases with increasing blow pressure. The observations showed a reduced cross-section at increased pressure. Assuming that a thermodynamic equilibrium was present before a blow pressure increase, the cross-section reduction at increased pressure has the following consequence: the current cannot be carried anymore, so that additional ohmic heating is created. This causes an increased plasma temperature and subsequently increases conductivity, which partly compensates for the additional losses. A new thermal equilibrium is found at smaller cross-section but higher plasma temperature. It is probable that at the new thermal equilibrium the plasma pressure will be increased and the plasma density reduced due to the increased plasma temperature.

The increased power loss at increased blow pressure (cf. figure 9.2a)) is dominated by an increased convective enthalpy flow. In general, the enthalpy flow scales with $\rho hv_s$ [LL75] and is increasing if a higher temperature is reached due to the higher blow pressure. However, to predict an absolute amount of increase, the change in axial cross-section profile must also be considered, as it is a dominant parameter for convective losses.

A reduced nozzle widening from test to test was observed for experiments with a higher blow pressure. According to the Lambert-Beer law, the radiation absorption in the cold gas vapor zone increases exponentially with the cold gas density and with the plasma-wall distance. As a consequence, at higher cold gas pressure, a
smaller fraction of radiation reaches the nozzle wall and can create ablation. It is therefore expected that ablation becomes dominant at higher current amplitudes.

Taking the effect on \( P \) and \( \tau \) into account only for wide nozzles, an increase of blow pressure increases the *desired losses* and thereby improves the passive resonant current creation.

### 10.2. Nozzle geometry

**Effect of nozzle length on arc characteristic:** The former section highlighted that mainly strongly blown arcs are interesting for passive resonance. In such arcs, three effects were observed when increasing the nozzle throat length \( L_T \): a) A larger \( \tau \) was measured for long nozzles (cf. figure 9.4f). b) A higher arc voltage was measured for long nozzles (cf. figure 9.4e)) and c) The *rising* characteristic started already from a lower current amplitude in long nozzles (cf. figure 9.4c). Effects a) and c) are undesirable for passive resonance, effect b) is desirable. To decide whether long nozzles are desirable, the optimization objective must previously be specified. Passive resonance at large currents can be better achieved with short nozzles. In addition, the lower \( \tau \) permits choosing smaller capacitor. However, at low currents, c) does show no effect, so that the larger arc voltage might outweigh the negative effects of an increased \( \tau \).

**Effect of nozzle diameter on arc characteristic:** A nozzle cross-section increase for a strongly blown arc showed only minor effect on \( \tau \) and on \( U(I) \) (cf. figure 9.3). At very low currents, the arc voltage is higher in the wide nozzles. However, the larger stray bands in this case indicate that this is the consequence of an increased fluctuation level. The low pressure experiments are more conclusive to decide whether a cross-section increase is desirable because they showed also the *rising* branch of the arc characteristics. At low blow pressure (cf. figure 9.3), a) a larger \( \tau \) was observed for wide nozzles, b) a smaller arc voltage was observed for wide nozzles and c) a higher transition current \( I_T \) to a *rising* characteristic was observed for wide
nozzles. For passive resonance, a) and b) are undesirable, but c) is desirable. Observation c) is a clear indication that at high currents, wide nozzles are superior for passive resonance. At small currents, the benefits from a reduced $\tau$ might overweigh and permit choosing a smaller capacitor.

**How ablation can create a rising characteristic:** The nozzle throat diameter $d_T$ and length $L_T$ showed significant effect on the transition current $I_T$, from which a rising characteristic was observed (cf. figure 9.3a), 9.4a) and 9.4c)). Narrow nozzles showed rising characteristics already from low current amplitudes. Long nozzles showed in particular very steeply rising slopes of the rising characteristics. However, the current density from which strong ablation sets in, is affected by the throat diameter and by its length.

A rising characteristic was always observed at high currents but it starts from lower currents in narrow nozzles. The rising branch is, therefore, caused most likely by the constricting effects of either the nozzle wall itself or the pressure build-up at significant ablation. An increased forced blow pressure did have as a consequence an increase in arc voltage. Such an increase of arc voltage is expected to occur also for an increase in ablative pressure. A forced blow pressure is not current-dependent as it is applied externally. It increases the arc voltage similarly for all current amplitudes. But the ablative pressure is a function of the current. Therefore, the rising ablation pressure with increasing current creates also an increasing voltage with increasing current.

**How a wall constriction can create a rising characteristic:** Lowke [LL75] explained a constant arc voltage at increasing current for convection stabilized arcs with a linearly increasing cross-section. A logical consequence of this is that an arc, which cannot freely increase its cross-section, may no longer have a constant voltage. Instead, a rising voltage for increasing current results. Certainly, a wall constricts the arc above a certain current level and prevents it from increasing its cross-section. Therefore, a constriction without ablation is expected also to cause a rising voltage.
How the throat length can increase the steepness of the rising characteristic: With long nozzle experiments the transition to a rising characteristic was not observed at much lower current $I_T$ but it showed a larger slope in the rising characteristics compared to that of short nozzles (cf. figure 9.4(a) and c)). The reason for this observation may be correlated with the ablative pressure build-up in the throat at increasing current. The pressure profile in the nozzle is formed by mass flow balance between the supplied material ablating from the surface and the mass flow that can exit the nozzle through one or both axial openings. A higher pressure would result if the surface is increased relative to the openings size. Several authors predicted a proportionally increasing stagnation point pressure with increasing throat length [Mul93, Nie87, Cas39].

Effect of throat length on nozzle widening: In the same number of experiments, a smaller throat widening occurred for long throat lengths. This is in agreement with the above explanation of increased stagnation point pressure at long nozzle throats. It is probable that similar to increased blow pressure, the arc reduces its cross-section also at increased ablation pressure. The resulting increased wall-arc distance would decrease the amount of radiation reaching the wall and thereby reduce the amount of ablation. However, it must be considered that the pressure profile significantly changes between forced blowing and ablative blowing. At forced convection, a pressure gradually decreasing down streams results. Ablative pressure profiles without externally applied pressure show a symmetric pressure profile with a stagnation point in the nozzle center and a decreasing pressure towards both exits. If in narrow nozzles ablation and forced convection are present simultaneously, a much more complicated pressure profile results with the stagnation point no more in the nozzle center.

A much larger nozzle throat widening from test to test due to ablation was observed for narrow nozzles. At small $d_T$, the arc approaches the wall already at small current amplitudes as the measurement results in section 9.2.1 revealed. An abrupt increase of ab-
Ablation has been shown at current densities exceeding $j = 50 \text{ A/mm}^2$ [STCA06]. As the current density is larger in narrow nozzles, ablation is expected to be more intensive therein for a given current amplitude.

10.3. Blow gas parameters

For strongly blown arcs no major difference in $\tau$ was observed at changing the blow gas but at low currents $\text{CO}_2$ showed the highest arc voltage (cf. figure 9.5). Of the tested gases, $\text{CO}_2$ is therefore best suited for passive resonance. Air showed a slightly higher $\tau$ and He showed a significantly lower arc voltage at high current amplitudes. The faster pressure decrease for He, observed in the experiments, is due to its lower density and higher sonic speed. A fast decreasing pressure is undesirable for passive resonance, as in DC circuit breakers the high blow pressure must be maintained during the whole duration of the passive resonant current creation. Experiments with higher currents should clarify if an effect on the transition current $I_T$ exists. Most probably the thermal dielectric and chemical performance are more important decision criteria to choose the gas type, because at current zero, the breaker must still be able to interrupt.

Considering the significantly different convective and conductive parameters of the gases air, $\text{CO}_2$ and He, the effect of the gas type on $U(I)$ and $\tau(g)$ is remarkably small. At high currents the voltage for all gases is almost identical (cf. figure 9.5a)) even though $\rho C v_s$ of He is two times higher than in $\text{CO}_2$ and air. According to equation (4.4), the He arc would burn with a smaller cross-section than that found with $\text{CO}_2$ and air. More specific experiments and theoretical investigations would be required to clarify how the gas properties affect arc parameters.
10.4. Nozzle material

The experiments with different nozzle materials should clarify the role of ablation in the arc chamber and whether the wall constriction or the resulting ablation causes the \textit{rising} characteristics at high currents. Quartz, expected to be a nearly non ablating nozzle material, was compared to the moderately ablating PTFE, Macor and the strongly ablating PMMA.

Firstly, one has to verify how strongly the materials are ablating. This can be concluded from two observations. The measurements of nozzle widening from test to test clearly identified PMMA as the most strongly ablating material and Quartz glass as a weakly to non ablating material. A second indication for the amount of ablation is the pressure build-up in the nozzle and the bottle during a high current phase (cf. figure 9.6). A pressure chamber pressure increase can be explained by an increased blocking of the nozzle throat. An increase of the bottle pressure, however, indicates that either more gas is present in the bottle or that the temperature of this gas has increased. In the experiments, all materials, including Quartz, created an increase of chamber pressure. Out of them, the strongly ablating materials created a higher pressure rise. This is in agreement with experiments of AC circuit breakers, which use ablation for the self blast effect [SNSD06]. However, all materials created also a measurable increase of bottle pressure. This is surprising, considering the significant differences in ablation concluded on the basis of the nozzle widening. If the pressure rise would only be a result of the increased amount of gas by ablation, nearly no bottle pressure rise would result during the Quartz nozzle experiment. This indicates that part of the pressure build-up in the nozzle results probably from gas heating.

Quartz as non or weakly ablating material has the highest transition current (cf. figure 9.7a)) and is thus well suited for passive resonance. Despite the very low ablation, experiments with Quartz nozzles did also show a \textit{rising} characteristic for high currents with
a similar slope as in the experiments with the strongly ablating materials, which is surprising. This is a clear indication that a rising characteristic is not simply a consequence of ablation. Probably it is caused predominantly by the wall constriction and only intensified or shifted to lower current values by additional constriction caused by the ablative pressure build-up.

Interestingly, arcs burning in Macor nozzles have the highest thermal inertia of all investigated nozzle materials (cf. figure 9.7b). As it is neither the most strongly ablating nor the most weakly ablating material, this must be the effect of other parameters than the amount of ablation. Possible explanations are in the radiation absorption performance of Macor vapor or in its thermal performance. Further experiments are required to clarify this point.

The observation that small $\tau$ values occurred predominantly at high currents and that PMMA as the most strongly ablating material has the smallest thermal inertia $\tau$ (cf. figure 9.7) leads to the speculation that the reduction of the thermal inertia at high current amplitude is caused by ablation. This would in principle be desirable for passive resonance. However, a small $\tau$ is of no use if the stationary $U(I)$ characteristic is rising. Experiments with low $\tau$ mostly showed also a rising characteristic already from low current amplitudes onwards.

### 10.5. Desirable and undesirable loss mechanisms

Because falling characteristics occur predominantly at convection stabilized low current arcs, as our measurements indicate, a measure to boost passive resonance would be the increase of the axial blow pressure. At the same time, effects of ablation and constricting walls should be minimized. Hence, passive resonance can benefit from the following parameter adaption in the DC arc chamber: high blow pressure, wide nozzle throat diameters, short nozzle throat length and weakly ablating materials. It is likely that the transition current
value to a *rising* characteristic is connected to the current amplitude where substantial ablation occurs. With experiments where back heating occurred, distinctly *rising* arc characteristics were always observed (cf. figure 9.1b), 9.4a) and 9.4c)). Therefore, ablation or even a flow reversal as in self-blast HVAC arc chambers has to be avoided in a DC arc chamber.

### 10.6. Potential for breaker improvement by adapting the arc chamber

An improvement of the arc chamber would enable improved performance of passive resonant breakers and new applications for breakers of this type. The breaker could be used as an HVDC circuit breaker, or it could be used as an HVDC load switch in a meshed multi-terminal HVDC network. The desired application leads to different optimization goals. A small capacitor is aimed at for load switches and MRTBs, a high interruption current at short interruption time is wanted for circuit breakers in meshed HVDC overhead line systems. An extremely short interruption time at high interruption current is aimed for in meshed HVDC cable networks.

#### 10.6.1. Resonance capacitor reduction for lower cost MRTBs

Load switches must only interrupt the specified nominal currents, and they are very uncritical in interruption time. The use of a smaller resonance capacitor could drastically reduce the costs and the footprint of such breakers.

Experiments on an existing HVDC MRTB designed for 500 kV, showed that with a resonant circuit of $C = 28 \mu F$ and $L = 130 \mu H$, the breaker can interrupt 3.5 kA by passive resonance [NNH+01]. The breaker is characterized by the black box parameters: $P = 1 W \cdot a_{pg}(g/(1 S))^{b_{pg}}$ and $\tau(g) = c \cdot (g/(1 S))^{d}$ with $a_{pg} = 6.92 \cdot 10^6$, $b_{pg} = 0.33$, $c = 30 \mu s$ and $d = 0.5$. 
In chapter 9, the black-box parameters of different arc chambers have been systematically extracted to find a configuration with minimum thermal inertia $\tau$, maximum falling slope of the stationary arc characteristic and high stationary arc voltage. Of the arcs investigated, those with high blow pressure and wide nozzle diameter showed arc characteristics, well suited for passive resonance. A blown arc with 15.5 bar in a 25 mm diameter nozzle is characterized by the coefficients $a_{pg} = 1.03 \cdot 10^6$, $b_{pg} = 0.5$, $c = 8.5 \mu s$ and $d = 0.4$. The selected arc chamber is non optimal because only one chamber parameter has been changed at a time to show its effect. Further improvement is expected by changing several parameters simultaneously. Arc chambers have been characterized for currents $< 2 \text{kA}$ only. For comparison of the selected arc chamber with the existing MRTB, the results have been extrapolated to 3.5 kA. At 500 kV, typically four arc chambers are used in series to achieve sufficient dielectric strength [BMRL85]. It is assumed that the power loss of several arc chambers in series can be superimposed and that the different arcs do not interact transiently with each other in an undesired way. If so, the stationary arc characteristics measured in section 9.1.3 can be multiplied by a factor of 4, which results in an increased parameter $a_{pg} = 4.15 \cdot 10^6$. All other coefficients remain unaffected.

In figure 10.1 arc characteristics are shown for the MRTB, for the selected arc chamber in a single chamber arrangement and for four chambers in series. The MRTB cooling power is approximately 50\% higher than the selected measurement curve. Unfortunately, no MRTB arc chamber parameters have been published, so it is unclear in which parameters the MRTB chamber differs from the selected arc chamber. Possible differences could be a higher blow pressure, a double flow arrangement, the use of SF$_6$ as interruption medium and a serial connection of several arc chambers. From the perspective of only the stationary arc characteristic the MRTB is therefore superior to the selected arc chamber for passive resonance. However, a four times smaller thermal inertia was measured in the
Figure 10.1.: Arc characteristics comparison of an existing MRTB [NNH+01] with measurement results from a selected arc chamber.
selected arc chamber of this thesis, which is a major advantage for passive resonance.

Simulations of the passive resonant current zero creation have been performed for the existing MRTB and for the selected arc chamber. It was found that for the MRTB the capacitor is approximately three times over dimensioned (cf. figure 10.2a). The breaker would still be able to interrupt the same current with an $8 \mu F$ capacitor. This would increase the time for current zero creation from 4 ms to 16 ms. A further reduction of the capacitor is only possible if the inductor is significantly increased to avoid additional damping by an increased resonance frequency.

The four times lower thermal inertia of the selected arc chamber (cf. figure 10.1b)) permits significantly higher resonance frequencies (cf. figure 10.2b)), without such strong increase of damping as in the MRTB. The capacitor can be reduced to $3 \mu F$ without the need to increase the inductor. As a consequence, the time for current zero creation is increased from 6 ms to 12 ms. Arcing times in this order of magnitude are considered as uncritical for load switches. A further reduction of capacitance is possible, but with similar consequences as the MRTB showed with a $8 \mu F$ capacitor.

The comparison revealed that an improvement of the arc characteristic allows using ten times smaller capacitor compared to that with the existing MRTB. The consequences on the size are illustrated in figure 10.3. The breaker size is reduced by 60%. Additional reduction is expected at further increase of blow pressure and combined optimization of all chamber parameters. In this comparison, the surge arrester bank dissipating the energy from the system after the switching arc interrupted is not considered.
Figure 10.2.: Reduction of commutation capacitor. Comparison of passive resonant current interruption for a) an existing MRTB [NNH+01] with b) the selected arc chamber (four chambers in series)
Figure 10.3.: 60% reduction of breaker size (without surge arresters) by improvement of the arc characteristic. Both breakers can interrupt 3.5 kA: a) existing HVDC MRTB breaker, b) possible HVDC load switch.
11. Summary and conclusion

Fault protection plays a key role in the development of future large scale meshed multi-terminal HVDC networks. However, DC current interruption is more challenging than AC interruption, because the current zero crossing, essential for arc extinction, is inherently absent with DC. To date no mechanical HVDC circuit breaker with fully satisfactory performance exists.

In this thesis, the influence of chamber and nozzle design parameters on the arc characteristics have been investigated. Focus was put on arcs for use in passive resonant HVDC circuit breakers and HVDC load switches. An improved switching arc characteristic permits a more efficient creation of a current zero crossing and eventually an overall optimized HVDC circuit breaker.

In figure 11.1, the state of the art in passive resonant HVDC circuit breaker research is graphically illustrated and classified as sufficient (red) or insufficient (blue) to be used in this project. The main work packages in this thesis are summarized (green). Logical future work steps (yellow) are formulated, which can be built upon this research. It is state of the art to use an existing AC arc chamber and to optimize an LC circuit for the given conditions. The main motivation of this work was to show that improved passive resonant current interruption can be achieved by adapting the arc chamber. Using a black-box description of a passive resonance breaker arc, a stability criterion and a theoretical optimum of arc characteristic have been formulated. The focus of this work is a systematic parameter study that has been performed to link arc chamber modifications to their effect on the black-box arc characteristics. With an arbitrary current source and an improved parameter determination method, a large number of different arc chamber configurations have been
characterized by their black-box parameters. Traditional methods
to determine arc parameters have been shown not to be sufficiently
accurate for such a parameter study. Therefore, it was necessary to
develop an improved parameter determination method, which uses
step currents and spike currents from the arbitrary current source,
instead of sinusoidal ones. Further research is required to find the
optimal combination of arc geometry for specific breaker require-
ments. But, is has been shown that in principle higher interruption
currents, smaller interruption times and smaller resonance capac-
itors are achievable by adaption in the arc chamber. This would
allow applying the principle of passive resonance for HVDC circuit
breakers and low-cost HVDC load switches.

11.1. Optimal arc characteristic for passive
resonance

A necessary though not sufficient condition for the resonance cir-
cuit instability and the growth of a passive resonance current, is
a falling stationary arc characteristic (decreasing voltage with in-
creasing current). A strongly falling characteristic and a large arc
voltage are desirable. A rising characteristic prevents passive res-
onance. Transiently, the passive resonance is suppressed by large
current gradients in combination with a large thermal arc inertia $\tau$.
Thus, a small thermal inertia and a low resonance frequency are de-
sirable. The resonance frequency is dominated by the commutation
path parameters and can be reduced by choosing a larger capacitor
or a larger inductance. However, both measures have their disad-
vantages as well: A large inductance increases the time to create a
current zero crossing, because more energy must be accumulated to
create the reverse current in the arc chamber. A large capacitor is
expensive and requires much space. Therefore, the highest potential
for improvement of passive resonance current zero creation is the
optimization of the arc characteristics by modifications of the arc
chamber and nozzle design.
### Figure 11.1.1.
Project overview: Identification of the state of the art gap and specification of project work packages.

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<th>Improved Arc Characterization</th>
<th>Breaker Performance Increase</th>
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<td><strong>State of the art</strong></td>
<td>Experimental search for optimal LC with a given arc chamber</td>
<td>Sinusoidal currents and simultaneous P and τ determination</td>
<td>passive resonant HVDC MRTB (AC breaker plus resonance- and energy absorber path)</td>
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<td><strong>Limitations</strong></td>
<td>Large number of experiments required</td>
<td>Inaccurate τ determination</td>
<td>Requires large and expensive capacitor</td>
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<td></td>
<td>Small performance increase achievable because AC arc chamber is non optimal for passive resonance</td>
<td>Coupled I and dI/dt</td>
<td>- time for current zero creation is too long</td>
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<td>Sensitive to arc fluctuations</td>
<td>Is limited in interruptible current</td>
<td></td>
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<tr>
<td><strong>Novel Approach</strong></td>
<td>Systematic arc characterization plus passive resonant simulation</td>
<td>Separate determination of dynamic and stationary arc characteristic</td>
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<td>Black-box simulations</td>
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State of the art (sufficient)  
State of the art (insufficient)  
Solution (this work)  
Solution (outlook)
11.2. Black-box parameter determination

The systematic characterization of different arc chambers requires an accurate and efficient method for arc characterization. Classical parameter determination methods used sinusoidal current waveforms for this purpose, which have coupled current $i$ and current slopes $di/dt$. Such current waveforms are not optimally suited for parameter determination, as transient and stationary effects simultaneously influence the arc voltage. A more accurate parameter determination was achieved with staircase-like and spike currents. In addition, staircase-like currents are well suited for validation of black-box modeling equations. They would in principle also allow a completely model-independent arc characterization. However, for this a large number of steps and slopes of different current gradient must be created to fill a table of $u$ and $du/dt$ for different combinations of $i$ and $di/dt$.

Furthermore, classical methods are not sufficiently robust with respect to stochastic arc fluctuations that are always present in the arc. As classical methods determine the arc parameters from single instants in time, an instantaneous fluctuation, which is not described correctly by the twice modified Mayr equation, leads to inaccurate parameter values. One of the methods investigated here does not suffer from this problem, because it calculates the parameters by iterative fitting. However, it is not considered as an alternative, because it requires to pre-specify a functional shape of the black-box model parameters. An improved method for arc parameter determination has been developed that overcomes the former limitations, because it extracts the stationary and the transient arc behavior independently from each other. This method makes use of more complex current waveforms.
11.3. Arbitrary current source

Complex current waveforms have been made possible by a novel arbitrary pulsed current source, developed for this project. It uses the superimposed current of three buck-converter-like modules. Nearly arbitrary current waveforms can be created by cooperative interaction of several modules. Currents up to $3 \text{kA}$ with selectable slopes of $0 - 150 \text{kA/ms}$ are achievable. The source is well suited for characterization of arcs with voltage $< 2 \text{kV}$ in a wide conductance range. By applying a small pre-current, an arc can be maintained during contact separation, so that no ignition wires are required.

11.4. Arc chamber configurations with a falling characteristic

In a model circuit breaker, convection stabilized arcs in a cylindrical nozzle have been investigated with a large number of different arc chamber configurations. Falling, flat and rising stationary UI-characteristics have been observed in a single experimental setup for various current levels. The arc behavior was interpreted using established arc models that include conductive, convective and radiative cooling: a) At low current density, voltage decreases with current because of the effect of radial conduction losses [$\text{TL75}$]. Hereby, an increasing arc temperature with current increases ionization and by this conductivity. b) At moderate current density, a constant current-independent arc voltage results because of axial convection losses [$\text{LL75}$]. This is caused by the linearly increasing cross-section with current but a no longer increasing conductivity, as ionization has already saturated. c) At high current density, the constricting wall and the ablated mass from its surface cause an overpressure in the nozzle and reduce the increase of cross-section with current [$\text{Nie87}$]. By that, a voltage rising with current results.

In a large number of similar experiments, the effect of blow gas pressure, nozzle diameter, throat length and blow gas on the black-
box parameters $U(I)$ and $\tau(g)$ was experimentally determined for arcs under forced convection and ablation controlled arcs of $< 2\,\text{kA}$. The derived relationships between physical arc chamber properties and arc characteristics allows indirect optimization of the transient arc-network interaction of new HVDC circuit breaker arc chambers, using black-box simulations. For the creation of artificial current zero crossings, passive resonance HVDC circuit breakers require a strongly falling arc characteristic up to high current levels and lowest possible thermal inertia.

The following arc chamber properties would support this optimization: A high blow pressure increases the falling slope of the stationary $UI$-arc characteristic and increases the arc voltage. It reduces the arc-cross-section such that wall constriction and nozzle ablation set in at higher current amplitudes. This causes the falling characteristic to remain up to higher current amplitudes. Nozzle ablation and wall constriction are considered to be undesirable for passive resonance, because they always corresponded to a rising characteristic in our experiments. In the experiments the thermal inertia increased only by $1 - 2\,\mu\text{s}$ when increasing the blow pressure from 1 bar to 15 bar. Thereby, only few additional damping is added to the resonance circuit.

The consequence of an increased nozzle diameter is that ablation and constriction effects set in at higher current amplitude. A wide nozzle will tolerate a falling characteristic to be maintained up to higher current amplitudes. However, in small nozzles at currents below the transition to a rising characteristic, an increased arc voltage and decreased thermal inertia was observed, which is in principle desirable for passive resonance. Furthermore, at large cross-section it is challenging to maintain sufficient blow pressure for long time. Therefore, the nozzle throat cross-section must be chosen with respect to the expected peak current.

A long nozzle throat increases the arc voltage, which is beneficial for passive resonance. However, it also shifts the transition point to a rising characteristic to lower current amplitudes, and it amplifies
the ablative pressure build-up in the nozzle. As a consequence, the arc voltage rises more strongly with the current in the in the rising characteristic. Besides that, the increase of throat length from 10 mm to 75 mm increased the thermal inertia by 3 μs.

In a weakly-ablating material such as quartz glass, a falling characteristic can be maintained up to higher current amplitudes. However, a rising characteristic is created by wall constriction effects at high currents even without strong ablation present.

11.5. Resonant breakers in a future HVDC network

HVDC load switches and MRTBs are limited in interruption current and are rather uncritical in interruption time. By simulations using the extracted arc parameters from section 9.1.3, the following could be shown for such breakers: Through adaption in the arc chamber it is possible to reduce the required capacitor by a factor of ten. The consequences for the size are illustrated in summary in figure 10.3. The size of the breaker (without surge arresters) is reduced by 60%. Additional reduction is expected at further increase of blow pressure and further optimization of the chamber parameters.

The improvement of the arc characteristic can also be used to increase to interruptible fault current or to reduce the time $T$ required for creating a current zero crossing. Both objectives are in the focus of HVDC circuit breakers. It has been shown in section 5.3 that reduced time for current zero crossing is achieved for an increased stationary arc voltage, an increased falling slope of the stationary arc characteristic and a decreased thermal inertia. In particular an increase of blow pressure can drastically reduce $T$.

The required fault current interruption capability in future meshed VSC-HVDC networks must probably exceed 10 kA, but the investigations in this thesis were limited to 2 kA. Further investigations are required to determine whether the geometrical and physical relationships found are scalable to current levels exceeding 2 kA. How-
ever, there have been measurements on an air blast breaker with 50 bar blow pressure that showed a *falling* characteristic up to 35 kA [BKM+70]. These measurements indicate that such a scaling is possible.
12. Outlook

In section 12.1, further experiments are proposed, to clarify questions which are important for passive resonant breakers, but have not been investigated within the scope of this work. Some part of the experiments would be possible with the available experimental tools but others would require extensions of the measurement setup. The required modifications of the model circuit breaker and of the arbitrary current source are discussed in sections 12.2 and 12.3.

12.1. Further experiments

12.1.1. Further phenomenological characterization

Future research should clarify the effect of pressure in a double flow arrangement and an encapsulated arrangement. These parameters have not been investigated in the scope of this work but are expected to have significant effect. The breaker should be extended to a double flow arrangement. Experiments in the literature [BMRL85, NNH+01] indicate that with double flow arrangements much higher stationary arc cooling can be achieved in comparison to single flow arrangements. Furthermore, an encapsulated breaker should be used to study the effect of increased background pressure. In principle, the encapsulation would also permit experiments with SF$_6$. However, the influence of the type of blow gas investigated so far was very small. When considering different gases, their dielectric performance must be kept in mind, as the arc chamber must still be able to interrupt and withstand transient over-voltages once a current zero is created. The modifications in the model circuit breaker required for this are discussed in section 12.2.1.
The investigations performed were limited to $2 - 3 \text{kA}$. This is far below the prospective fault current amplitudes HVDC circuit breakers will need to interrupt. Also the blow pressure was limited to 15.5 bar. A *falling* stationary arc characteristic up to 40 kA with 50 bar blow pressure can be found in literature [BKM+70], however, no experimentally determined $\tau$ curves were found for high current high pressure arcs. The variation of $\tau(g)$ with blow pressures has been experimentally verified up to 15.5 bar only. (In the range of 1 - 15 bar, $\tau(g)$ did not increase further with blow pressure but saturated.) But it is unsure, how well the experiment results can be extrapolated to several 10 kA for the application range of HVDC circuit breakers. Therefore, further characterization at high current and high blow pressure are desirable.

12.1.2. Arc characterization and understanding cooling mechanisms

The investigation in this thesis focused mainly on a phenomenological description. It has been experimentally shown, how the arc characteristic varies with the arc chamber parameters. The observations could be explained to a certain extent by simple mathematical models. But the experiments were not well suited to explain in detail physical processes that cause the observed phenomena. Further research should focus on a deeper understanding of the physical processes.

On a still higher level of abstraction, the axial arc cross-section profile and the radial temperature profile should be measured for various arc chamber and nozzle parameters. It became clear in the discussion that the arc cross-section plays an important role for the current amplitude $I_T$ at which a *rising $UI$-characteristic starts. It was, for example, observed that ablation in long nozzle throats causes less widening than in short nozzles, in identical experiments. Determination of arc cross-sections, nozzle widening and ablated mass in cylindrical nozzles of different materials (in particular quartz glass) and of different length would be required to verify the explanations
given here in the discussion part.

Measurements of the radial temperature profile would give further insight into radial conduction and radiative energy transport. This would be of interest especially in the low current range, where conductivity strongly varies with current. Measurements of axial temperature and cross-section profiles may help to understand the axial enthalpy flow. A CFD simulation of the cold gas and plasma gas flow would bring more insight into the laminar and turbulent mixing processes in the nozzle.

On a level of physical understanding, the measurement of specific energy transfer mechanisms would be of interest. A measurement of radiation emitted from the plasma column or a measurement of radiation absorption in the cold gas and on the nozzle surface would give further insight on how to influence ablation in the arc chamber. The ablation experiments from Seeger [STCA06] were very conclusive, but were restricted to atmospheric pressure. The experiments in this work have shown that the set in current of ablation is also affected by blow pressure. Experimental measurements of accumulated mass ablation would therefore also be desirable at higher blow pressures.

In principle, the arc thermal inertia is the ratio of the energy stored in the arc to the cooling power removed by different mechanisms. The use of gases with small enthalpy below 20000 K may decrease the thermal inertia of the arc and reduce the damping of passive resonance. It is, however, expected that this has also effects on the different cooling mechanisms.

12.1.3. Passive resonant current zero creation

A passive resonant current interruption has only been simulated but not experimentally measured in the scope of this thesis. For verification of the simulations performed, and for a deeper understanding of the effect of arc fluctuations on passive resonance, experiments with a resonant circuit will be required (cf. section 12.2.2).

Some authors observed a beat frequency in the passive resonance
at current increase [NNH+01]. Such a beat frequency cannot be
described with the mathematical modeling used here in the simula-
tions. It could, however, be a consequence of variations in the arc
characteristic during the passive resonant oscillation. Such slow vari-
ations could result from interactions of the pressure chamber with a
partly blocked nozzle.

12.1.4. Arc extinction

The arc in the arc chamber has not only to create a current zero
crossing but the arc must also be extinguished at current zero with-
out re-ignition. The current gradients in passive resonance can sig-
nificantly exceed the present gradients occurring in AC breakers dur-
ing 50 Hz interruption. Therefore, further experiments with a passive
resonant breaker should include arc extinction and apply a transient
recovery voltage after current interruption. It should be investigated,
up to which current gradient the arc can still be extinguished and
how the chamber and nozzle parameters influence the interruption
performance. This may result in a maximum allowable frequency
in passive resonance. It could also be possible that design parame-
ters that are favorable for current zero creation are unfavorable for
current interruption. However, results from such investigations with
AC circuit breakers could possibly be utilized.

12.2. Model circuit breaker extension

12.2.1. Performance increase

The maximum blow pressure used in the experiments is 20 bar be-
cause at higher blow pressures too high arc voltages resulted, so
that it was no longer possible to create the desired current wave-
form. However, all pressure equipment was designed to be used up
to 50 bar. Only the pressure sensor mounted in the heating chamber
would have to be replaced.
The wall bushing was constructed for 10 kV and 20 kA and the model circuit breaker could also be used at significantly higher current amplitudes. Furthermore, if hot plasma is blown out from the nozzle, the bushing between high voltage electrode and movable rod of the pressure cylinder for contact movement is close to the flashover limit for voltages exceeding 3 kV. At an increased voltage, it is advised to replace this bushing. Alternatively, the bushing could be totally removed and the pressure cylinder operated at high voltage potential.

### 12.2.2. Adaptations for passive resonance

For passive resonance experiments, an LC path can be installed in parallel to the breaker. If a small capacitor is used, it has to be considered that at arc extinction, the remaining current stored in the current source module coils may charge the capacitors to voltage levels that exceed the source voltage. The subsequent reverse current into the source capacitor, via the antiparallel diodes of the IGBTs must remain uncritical for the source.

### 12.2.3. Possible extensions

The model circuit breaker has been constructed without encapsulation and with a single flow arrangement. However, it is designed in such a way that a double flow arrangement and a pressure chamber could be added easily at a later stage. For a double flow arrangement, the lower cover of the pressure chamber would have to be replaced and a new nozzle would be required. An encapsulation at single flow arrangement could easily be mounted on top of the pressure chamber. For a double flow arrangement with encapsulations, either two separate encapsulation could be mounted at each side of the pressure chamber, or the whole breaker including the storage bottles could be placed inside a large encapsulation.
12.3. Arbitrary current source extension

12.3.1. Performance increase

An increase of arc current $> 3 \text{kA}$ and an increase of blow pressure $> 20 \text{bar}$ both exceed the present limits of the arbitrary current source. Higher currents could be achieved by adding additional modules to the source or by combining it with an LC resonant circuit. The first solution requires also a major adaptation in the IGBT controller, unless the additional modules can be made to switch synchronously with one of the existing modules. Additional modules in combination with the controller adaptation would bring the advantage of a virtual switching frequency increase and smaller ripple by interleaved operation. Adaptation in the controller would not be required in the alternative solution using an LC current source in parallel to the arbitrary current source. The available triggers in the controller could be used to control a making switch for the LC current. Thereby, complex current waveforms with amplitude up to $3 \text{kA}$ could be superimposed on a high sinusoidal current. This would allow stationary and transient arc characterization also at high current amplitudes.

At high blow pressure, the arc voltage will exceed the source voltage, so that the desired current waveforms can no longer be maintained. An increase of source voltage would require new capacitors and additional IGBTs in series to the installed ones. However, as the IGBT switching losses are a major disadvantage in the present source, a redesign with switching at current zero, would probably be superior to a simple voltage increase. A large scale current source up to $10 \text{kV}$ and up to $20 - 30 \text{kA}$ is under development [WCBF12].

12.3.2. Possible extensions

A future extension of the flexible current source with real time control could permit its use for power hardware in the loop measurements. By this, elements such as the parallel energy absorber or the
passive LC components in passive resonance HVDC circuit breakers could be simulated and do not have to be introduced as hardware components.

Synthetic tests are, in principle, also possible, as high voltage sources are available in the ETH high voltage laboratory. However, a reliable circuit breaker would be required to disconnect the source at the instant of current injection to avoid damage to the source components.

12.3.3. Alternative application fields

The flexible current source is also well suited for different high current test applications. It has already been successfully used for characterization of SF$_6$ disconnectors. In particular it would also be suitable for tests with different DC components, which require a specific DC ripple superimposed on a constant or slow changing current. In principle the modules could also be operated independently from each other. In this configuration, it might be possible to emulate a down-scaled HVDC network with three infeeding terminals.
A. Specification of the arbitrary current source

A.1. Requirements

Precise arc characterization in its stationary and dynamic behavior is the main application of the source designed. As shown in section 4, an arc plasma is a very dynamic load. It changes its resistance by orders of magnitude during a single experiment. A free burning arc reaches low resistance $< 0.1 \Omega$, a strongly blown arc has resistance values in the order of $1 – 10 \Omega$. After extinction, the resistance increases to very high values. The dynamic arc behavior also varies significantly with the current slope applied. At low current slopes, the arc follows its highly nonlinear stationary characteristic, at moderate slopes its resistance changes with time delays in the order of $1 – 30 \mu s$, at high current slopes the arc increases its voltage proportional to the current.

Traditionally, sinusoidal current waveforms have been used for arc characterization. The disadvantage is that current gradient and current amplitude are linked to each other. The chosen approach for arc characterization was to measure the transient characteristic and the stationary characteristic independently from each other. In a single experiment, each conductance value must be created as steady state by applying different current amplitudes. From each steady state, a transient reaction to a fast changing current must be excited, from which the transient parameter is determined. Stationary characterization requires constant current intervals. Transient characterization can only be performed during current slopes or at a constant current subsequent to a slope. Therefore, the source must
be able to create constant current periods and steep slopes close to each other and at various current amplitudes. A first order hold with selectable rate of rise up to approaching infinity, selectable step size and constant value intervals in between with freely selectable duration is considered to be optimal.

In VSC HVDC systems, short circuit current amplitudes exceeding 20 kA with rates of rise up to 5 kA/ms are expected at a voltage level of up to 500 kV [HJ11, BF13a]. In passive resonance, current slopes may exceed 200 kA/ms [NNH+01, BMRL85]. It was clear from the beginning that even for short measurement duration (eg. < 10 ms) the required power and component costs for testing at full voltage and full current would be beyond all practical limits. Instead, it was decided to build a small scale source as proof of concept with downsized current and voltage level, but full dynamic performance.

The minimum measurement duration required adds up from different phases of the experiment: A waveform of interest with 10 ms duration is sufficient for arc characterization. This allows the creation of up to 20 successive different arc states during one experiment, including excitations of transients and a re-stabilization in each state at a constant voltage. Instead of an ignition wire, a low current is be maintained sufficiently long to create the required experiment conditions. 80 ms are required for pneumatic contact separation and positioning. For blown arcs, the pressure stabilization requires 5 – 10 ms, but can be performed simultaneously with the contact opening. Thus, the source must create a current waveform with > 90 ms duration including a burst of > 10 ms high current amplitude.

The investigation focuses on the burning phase of an arc and not on its extinction. The physical behavior of an arc in the high current phase is independent from its electric potential, as long as no extinctions and no thermal or dielectric reignition occur. Two requirements must be considered for the choice of the applied source voltage: the ability to maintain the arc at low currents and the ability to create the desired current waveform independent of the dynamic arc voltage
A.2. Component specification

Mechanically, the source was set up in a separate source room. A wall bushing was installed to conduct the current into a high voltage laboratory, where a model circuit breaker is placed. The source-control, breaker triggering and data recording is located in a measurement cabin, placed inside the high voltage laboratory. A component overview of the whole experiment including the arbitrary current source and the model circuit breaker is provided in figure A.1, a single source module including a component list is provided in figure A.2.

A.2.1. Energy storage

In each module, an ESTA dry self-healing DC capacitor from Vishay of size \( C_n = 2 \text{ mF} \) was used to store the total energy required for creation of the current waveform prior to the experiment. The capacitor is short circuit safe and can be charged safely up to \( V_{\text{max}} = 3.2 \text{ kV} \). At
Figure A.1.: Schematic overview of the location and connection of the major experiment components
Figure A.2.: Zoom view and component list of a single current source module: front view (left top), back view (right top), side view (left bottom), top view (right bottom)
the nominal voltage of 3 kV, an energy of 27 kJ is stored in the three modules. IGBT and diode switching losses scale approximately linearly with the commutation inductance. Therefore, it was necessary to keep the stray inductances of the loop, formed by the capacitor, the IGBT, the fuse and the diode, below 150 nH. The capacitor is equipped with three parallel bushings for each terminal, to keep its self inductance $L_{CS} < 40$ nH.

An FuG HCK capacitor charger with 80 mA charging current is used to charge all three capacitors simultaneously via a $R_D = 250 \Omega$ charging resistor. The charger is connected optically to the experiment computer and the charging process is fully automated. A MATLAB routine continuously reads the instantaneous voltage and current values from the charger and disconnects the charger once the pre-set voltage is reached.

Three automated grounding rods $S_{Gn}$ ensure short circuiting of the capacitors in the off-state (cf. figure A.3). In an emergency case, a direct short circuiting of the capacitors via the grounding rods is possible but not advisable. The resulting short circuit current of 20 kA would damage the grounding rods and would lead to an undesired temporary rise of the local ground potential, even though all
equipment was carefully grounded by individual and low resistance cables to a common laboratory ground. At normal experiment shut down, an automated common discharging rod $S_D$ short circuits all capacitors via the resistor $R_D$. The grounding rods close with a delay of 4 seconds, relative to the closing of the discharge rod. The delay ensures that the capacitor voltage has decreases sufficiently to prevent large currents at grounding. At emergency shut-off, the delay is reduced to 1 second. It is assumed that a person entering the room will need more than 1 second to reach equipment parts on high voltage potential. With a 1 second delay, short-circuit currents also occur, but they do not lead to destruction of the grounding rods. In addition to the automatic grounding, a person entering the test area is instructed to ground the capacitors manually by a grounding rod provided.

### A.2.2. Variable inductors

A variable inductor for each module has been designed for precise selection of current slopes at a given capacitor voltage. Nine taps are arranged at winding positions $N = 0, 2, 4, 6, 10, 16, 30, 50$ and 64, so that 24 different inductance values in the range of $6 - 1345 \mu H$ are selectable. The inductance values have been measured and they have also been calculated by a COMSOL simulation. Simulation and measurement are in good agreement (cf. figure A.4). With an accuracy of $2 \mu H$ the inductance values are described by the following fifth order polynomial.

$$L(N) = k_5 N^5 + k_4 N^4 + k_3 N^3 + k_2 N^2 + k_1 N + k_0$$  \hspace{1cm} (A.1)

with $k_5 = -3.05 \cdot 10^{-13}$ H, $k_4 = 8.92 \cdot 10^{-11}$ H, $k_3 = -1.04 \cdot 10^{-8}$ H, $k_2 = 6.26 \cdot 10^{-7}$ H, $k_1 = 5.49 \cdot 10^{-6}$ H and $k_0 = -1.01 \cdot 10^{-5}$ H. In addition to that, three external 3 mH inductors are available, which can be selectively placed in series to a module inductor or as mutual inductance of two or three modules.
A Specification of the arbitrary current source

Figure A.4.: Selectable inductance values vs. winding numbers (from [S5])
A.2.3. IGBT and diodes

An ABB HiPak 5SNA1200G450300 IGBT is installed in each module (S\textsubscript{n} in figure 6.1). Antiparallel diodes are integrated in the HiPak modules to conduct negative currents and to protect the IGBT from negative voltages. Each module is also equipped with an ABB HiPak 5SNA1200G450300 freewheeling diode (D\textsubscript{n} in figure 6.1), to which the load current commutates at opened IGBT. The diode is mounted on the same rack as the IGBT, so that the circuit inductance of IGBT and diode remains below 50 nH. All solid state components can be safely operated up to 3 kV and 1 kA.

The IGBT gate drivers are electrically insulated, and an optical fiber is used for the on/off switching control. This allows operating the IGBT on 3 kV potential. The chosen driver unit is specified for up to 3 kHz switching frequency but can be operated during short bursts at 10 – 20 kHz. However, at frequencies exceeding 3 kHz, the thermal conduction of the chip is too small to dissipate the switching losses. A maximum temperature rise of 100 K of the IGBT and diode limits the maximum duration of the burst. No forced cooling is used. The time constant of thermal conduction is of the same order as the typical experiment duration, therefore a forced cooling would have no effect. The 4 minutes interval between two experiments (required to recharge the capacitors) is sufficiently large for the substrate temperature to decrease to near ambient conditions. Under normal experiment condition, no current flows through the antiparallel diode in the IGBT HiPack. Its thermal performance is therefore non critical.

A.2.4. Fuses

At overcurrent or thermal overstress, the IGBT could be destroyed thermally and the possibility of explosion of the housing cannot be excluded. An URB303250 Ferraz Shawmut fuse with melting point of $34 \cdot 10^3$ A\textsuperscript{2}s interrupts the current in case of an IGBT short circuit fault and protects the other source components from excess current.
The fuse is not sufficiently fast to prevent an overstress of the IGBT modules themselves, but it interrupts below the thermal explosion limit of the IGBTs. The fuse self-inductance $L_F$ is $< 30 \, \text{nH}$ and the fuse resistance $R_F = 0.07 \, \text{m}\Omega$ are negligible in operation. In addition, there is further protection by a transparent housing of nonbreakable PET enclosing the IGBTs.

**A.3. Performance limit estimation**

In experiments with high switching frequency and high current, the IGBTs are operated close to their thermal limits. To ensure that a desired current waveform does not lead to exceeding this limit, a simulation of the IGBT and diode losses was implemented in MATLAB and is executed each time before uploading a configuration file to the controller. The energy balance equation for the substrate temperature $T_n$ is numerically integrated for the switching cycle $n$ at a frequency $f$

$$T_n = T_{n-1} + \frac{\left( P_{\text{tot}} - \frac{(T_{n-1} - T_L)}{R_T} \right)}{f \cdot C_T} \quad (A.2)$$

with $T_0 = 25^\circ \text{C}$. Thermal heat conduction from the substrate over the junction, the case and the cooling plate into free air is considered by a combined cooling term with thermal conductivity $R_T$ and thermal capacity $C_T$. The switching and conduction device losses are considered in a heating term $P_{\text{tot}}$. The thermal IGBT parameters are $R_{\text{TIGBT}} = 9.5 \cdot 10^{-3} \, \text{K/W}$ and $C_{\text{TIGBT}} = 1.98 \, \text{J/K}$, the ones of the diode are $R_{\text{TD}} = 17.9 \cdot 10^{-3} \, \text{K/W}$ and $C_{\text{TD}} = 1.68 \, \text{J/K}$.

It has to be emphasized that the losses are a function of temperature, voltage, commutation inductance, current and switching frequency. The following numeric values of the IGBT and the diode were taken from the data sheet of the installed equipment. Current dependent parameters are known at a discrete temperature of $25^\circ \text{C}$ and $125^\circ \text{C}$ with a circuit stray inductance of $150 \, \text{nH}$ and a voltage.
Figure A.5.: Maximum experiment duration versus module current and switching frequency. Experiment durations below the plotted lines are within the thermal limit of IGBT and the diode. The calculations were made for a single module.
of 2800 V only. It is assumed that the losses scale linearly with the voltage. To account for the uncertainties, the allowed temperature increase is limited to 80°C, which is 20°C below the thermal limit of IGBT and diode.

In the IGBT, three types of losses occur. These are on-state conduction losses $P_{\text{IGBT}_{\text{cond}}}$, turn-on losses $P_{\text{IGBT}_{\text{on}}}$ and turn-off losses $P_{\text{IGBT}_{\text{off}}}$. Conduction losses are evenly distributed in time, during which the corresponding device is conducting. Switching losses arise as instantaneous amounts of thermal energy and are converted to mean power by multiplication with the switching frequency $f$:

$$P_{\text{IGBT}_{\text{on}}} \simeq (c_1 \cdot (I_{\text{on}}) + c_2) \left( \frac{u_C}{2800 \text{ V}} \right) f$$  \hspace{1cm} (A.3)

$$P_{\text{IGBT}_{\text{off}}} \simeq (c_3 \cdot I_{\text{off}}^2 + c_4 \cdot I_{\text{off}} + c_5) \left( \frac{u_C}{2800 \text{ V}} \right) f$$  \hspace{1cm} (A.4)

$$P_{\text{IGBT}_{\text{cond}}} \simeq d(V_{\text{IGBT}} \cdot I_N + R_{\text{IGBT}} \cdot I_N^2)$$  \hspace{1cm} (A.5)

with $c_1 = 4.4 \cdot 10^{-3} \text{ J/A}$, $c_2 = 0.61 \text{ J}$, $c_3 = 1.2 \cdot 10^{-6} \text{ J/A}^2$, $c_4 = 1.7 \cdot 10^{-3} \text{ J/A}$, $c_5 = 0.47 \text{ J}$, IGBT forward voltage $V_{\text{IGBT}} = 1.8 \text{ V}$ and on-state resistance $R_{\text{IGB}} = 1.5 \text{ mΩ}$. $u_c$ is the capacitor voltage, $d \simeq u_{\text{load}}/u_C$ is the duty cycle with arc voltage $u_{\text{lad}}$ and capacitor voltage $u_C$. The module current $I_N \approx I_{SN}$ averaged over a switching period is assumed to be equal to the set current $I_{SN}$. The IGBT turn-on current $I_{\text{on}} \approx I_N(1-k)$ and the IGBT turn-off current $I_{\text{off}} = I_N(1+k)$ deviate from this value due to a relative current ripple $k$ ($k<1$).

In the diode, only conduction losses $P_{\text{dcond}}$ and turn-off losses $P_{\text{doff}}$ must be considered, turn-on losses are negligible:

$$P_{\text{doff}} \simeq \left( c_6 \left( \frac{I_{\text{on}}}{2} \right)^2 + c_7 \left( \frac{I_{\text{on}}}{2} \right) + c_8 \right) \left( \frac{u_C}{2800 \text{ V}} \right) f$$  \hspace{1cm} (A.6)

$$P_{\text{dcond}} \simeq (1-d)(V_d I_N + R_d I_N^2)$$  \hspace{1cm} (A.7)

with $c_6 = -1.01 \cdot 10^{-6} \text{ J/A}^2$, $c_7 = 2.74 \cdot 10^{-3} \text{ J/A}$, $c_8 = 0.286 \text{ J}$, diode
forward voltage $V_d = 2.25$ V and on-state resistance $R_d = 1.7$ mΩ.

The thermal limit has been evaluated for a $C_N = 2$ mF capacitor charged to $U_c = 3$ kV, an $R_{arc} = 50$ mΩ load and $k = \pm 10\%$ current ripple and is displayed in figure A.5.

### A.4. Control and automation

The experiment is fully automated including experiment preparation, execution and data recording. The desired current waveform is specified in a .txt configuration file that is uploaded via a serial connection to an IGBT controller. The IGBT controller simultaneously controls the three module currents based on the specified parameters in the configuration file and the current measurement in each module. It also triggers the model circuit breaker and a Digital Oscilloscope for data recording. A basic scheme of the entire experiment control is shown in figure A.6. A typical .txt- control file is provided in figure A.7 and its parameters are explained in figure A.8.

### A.4.1. Monitoring and control hardware

A 5 mΩ HILO ISM5P/5 shunt is installed in each module to monitor the module currents $I_n$. Its maximum thermal rating of 80000 A²s allows a maximum current of 1 kA for an experiment duration of 80 ms, which is well above the achievable limits of the arbitrary current source. The bandwidth of 50 MHz and the rise time of 7 ns are sufficient for the current control.

A $TT - HVP15HV$ 1 : 1000 voltage probe with 50 MHz bandwidth is installed to measure the capacitor voltage.

The three voltage and three current signals are optically transmitted by individual optical links. The links were designed to be used with the IGBT controller, but they can also be operated as stand alone devices. In the sending $ATT$ device, a signal is sampled with 4.5 MHz and 16 bit and optically transmitted. The receiving $ATR$ device samples the signal at a frequency of 16 MHz. The specified
Figure A.6.: Current source control: principle scheme
Example .txt controller file

```
01.02.2011,Michael Walter,Test 1
IClip,TMess,Imax,UMess,Sonde,ph1,ph2,ph3,L
2000,00250,05000,1500,2000,000,120,240,2000
IGBTmax,IGBTton,IGBTtoff,a,b,c,d,e,DEmax
1200,03,05,0044,0061,0012,0017,0047,1000
kPLo,kPLo,kPLo,FLo,kPHi,kPHi,kPHi,FHi
0023,0002,0000,0200,0023,0002,0000,1000
T1 ,T2 ,T3
000500,000700,010000
ULmax,UCmax,UCmin,UCd
5000,4250,1200,0300

t ,I1 ,I2 ,I3 ,F ,T4,5,6,FHi
000000,0580,0680,0880,0500,0,0,0,0
100000,0300,0500,0600,0500,1,0,0,1
101000,0580,0680,0880,0500,0,0,1,1
104000,0580,0380,0280,0500,0,1,1,1
108000,0000,0000,0000,0500,0,1,0,1
112000,0000,0000,0000,0500,0,0,0,1
```

**Figure A.7.** Example .txt header file and corresponding set current waveforms (adapted from [WG])
### .txt file: Header parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Erstellungsdatum</td>
<td>01.02.2011</td>
<td></td>
</tr>
<tr>
<td>Name des Erstellers</td>
<td>Michael Walter</td>
<td></td>
</tr>
<tr>
<td>Experimentname</td>
<td>Test 1</td>
<td></td>
</tr>
<tr>
<td>Stromlimitierung (Clipping)</td>
<td>0250</td>
<td>0-10000</td>
</tr>
<tr>
<td>Periodendauer Aufzeichnung [us]</td>
<td>0500</td>
<td>10-10000</td>
</tr>
<tr>
<td>Max. Strom [A] (Abschaltung)</td>
<td>1500</td>
<td>50-10000</td>
</tr>
<tr>
<td>Spannungsmesssonde (100% 10V) [V]</td>
<td>5000</td>
<td>100-10000</td>
</tr>
<tr>
<td>Strommesssonde (100% 10V) [A]</td>
<td>1200</td>
<td>20-10000</td>
</tr>
<tr>
<td>Versatzwinkel φ1 [°]</td>
<td>120</td>
<td>360-5000</td>
</tr>
<tr>
<td>Versatzwinkel φ2 [°]</td>
<td>240</td>
<td>360-5000</td>
</tr>
<tr>
<td>L Induktivität [μH]</td>
<td>2000</td>
<td>10-40000</td>
</tr>
<tr>
<td>IGBT max. Stromspitze [A]</td>
<td>0,0001</td>
<td>0-9999</td>
</tr>
<tr>
<td>IGBT Einschaltverzögerung [us]</td>
<td>044</td>
<td>0-9999</td>
</tr>
<tr>
<td>IGBT Ausschaltverzögerung [us]</td>
<td>01</td>
<td>0-9999</td>
</tr>
<tr>
<td>IGBT a [0.0001]</td>
<td>012</td>
<td>0-9999</td>
</tr>
<tr>
<td>IGBT b [0.0001]</td>
<td>017</td>
<td>0-9999</td>
</tr>
<tr>
<td>IGBT c [0.0001]</td>
<td>047</td>
<td>0-9999</td>
</tr>
<tr>
<td>IGBT d [0.0001]</td>
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<td>0-9999</td>
</tr>
<tr>
<td>IGBT e</td>
<td>0023</td>
<td>0-10000</td>
</tr>
<tr>
<td>IGBT max. Erwärmung [°]</td>
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<td>0-10000</td>
</tr>
<tr>
<td>D Emax (max. Erwärmung) [°]</td>
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<td>0-9999</td>
</tr>
<tr>
<td>KpLo [0.1%]</td>
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<td>0-9999</td>
</tr>
<tr>
<td>KiLo [0.1%]</td>
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<td>0-10000</td>
</tr>
<tr>
<td>KdLo [0.1%]</td>
<td>0002</td>
<td>0-10000</td>
</tr>
<tr>
<td>Regler Periodendauer PWM Lo [us]</td>
<td>1000</td>
<td>int</td>
</tr>
<tr>
<td>KpHi [0.1%]</td>
<td>0002</td>
<td>0-10000</td>
</tr>
<tr>
<td>KiHi [0.1%]</td>
<td>0002</td>
<td>0-10000</td>
</tr>
<tr>
<td>KdHi [0.1%]</td>
<td>0002</td>
<td>0-10000</td>
</tr>
<tr>
<td>Regler Periodendauer PWM Hi [us]</td>
<td>1000</td>
<td>int</td>
</tr>
<tr>
<td>Triggersignal 1 [us]</td>
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<td>0-100000</td>
</tr>
<tr>
<td>Triggersignal 2 [us]</td>
<td>000000</td>
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<tr>
<td>Triggersignal 3 [us]</td>
<td>000000</td>
<td>0-100000</td>
</tr>
<tr>
<td>Sicherheit: max. Lichtbogenspannung [V]</td>
<td>5000</td>
<td>100-10000</td>
</tr>
<tr>
<td>Sicherheit: min. Kondensatorspannung [V]</td>
<td>4200</td>
<td>100-10000</td>
</tr>
<tr>
<td>Sicherheit: max. C-Differenzspannung [V]</td>
<td>300</td>
<td>100-10000</td>
</tr>
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</table>

### .txt file: Current waveform and trigger specification

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<tr>
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<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
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<td>Zeit [us]</td>
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<td>0-100000</td>
</tr>
<tr>
<td>Soll IGBT1 [A]</td>
<td>0580</td>
<td>50-5000</td>
</tr>
<tr>
<td>Soll IGBT2 [A]</td>
<td>0680</td>
<td>50-5000</td>
</tr>
<tr>
<td>Soll IGBT3 [A]</td>
<td>0880</td>
<td>50-5000</td>
</tr>
<tr>
<td>Periodendauer PWM [us]</td>
<td>500 (20kHz)</td>
<td>1-100</td>
</tr>
<tr>
<td>Triggersignal 4</td>
<td>1</td>
<td>0,1</td>
</tr>
<tr>
<td>Triggersignal 5</td>
<td>1</td>
<td>0,1</td>
</tr>
<tr>
<td>Triggersignal 6</td>
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<td>0,1</td>
</tr>
<tr>
<td>Periodendauer Hi aktiv</td>
<td>0</td>
<td>0,1</td>
</tr>
</tbody>
</table>

**Figure A.8.**: Explanation of .txt controller file parameters (adapted from [WG])
3 dB frequency of 0.55 MHz is sufficient for the IGBT control and protection measures. The specified signal delay is 0.8 µs. A 1:1 transformer of 3 kV insulation voltage is used to supply the ATT devices connected to the measurement shunt. In this way, it is possible to measure the current on high voltage potential.

The IGBT controller is a combination of a Texas Instruments (TMS) micro-controller, a Digital Signal Processor (DSP) and high speed comparators. The controller hardware and software has been developed by WEMEL GmbH. The three current and three voltage measurements are connected to the IGBT controller. The module current signal is used for current control and protection, the capacitor voltage signal is used for protection purpose only. An experiment is interrupted if a negative current is measured, if the capacitor voltage decreases below a specified limit or if the capacitor voltage exceeds a specified limit.

A National Instruments NIPCIe – 6351 IO card is used to record the three current and voltage signals. The card allows parallel sampling of measurement signals at a speed of 1 MS/s each. The signals are used to monitor the experiment and for optimization of the control parameters. For arc characterization, separate current and voltage measurements are used.

A.4.2. Current waveform control

A file is sent from the experiment computer to the DSP component of the IGBT controller. It contains the three module set-maximum-currents $I_{Sn}$ for discrete intervals, plus additional configuration parameters (cf. Figures A.7 and A.8). Three analog voltage waveforms, corresponding to the set current, are created by the DSP based on the discrete current values in the configuration file. The controller-specific parameters are forwarded to the micro-controller. Analog micro-controller inputs are the three module current signals $I_n$ from the $ATR$ and the three set currents $I_{Sn}$ from the DSP. A control algorithm calculates the three PWM outputs from the input signal difference. In its original configuration, a PID controller was
implemented. This controller did not achieve the desired control performance due to following reasons:

- The desired high current slopes > 50kA/ms could not be achieved without creating a massive overshoot. A PID controller was not suitable at high current slopes, because the typical control cycle time of 100 – 300 µs was too large, relative to the 20 µs rise time of the current from 0 – 100%. The controller could only be either optimized for speed or for minimum overshoot.

- The load varies too strongly during the experiment. During the contact separation a low current shall be maintained for 80 ms at an arc voltage of ~ 0.2 kV. In the subsequent 10 ms of interest, the blowing increases the arc voltage to ~ 2 kV. This causes a change of current gradient by a factor of 2.8 and significantly affects the control dynamics.

- A large integral parameter of the PID controller is required to compensate the large arc voltage variations. However, a falling arc characteristic in combination with the stochastic arc behavior can cause an instable controller at large integral or differential parameters.

- Repetitive experiments are not sufficiently identical because of the interaction of the controller dynamics with the stochastic arc behavior.

In the scope of this project, modifications in the controller hard- and software have been made to optimize the control behavior for application on switching arcs. The PID controller was replaced by a semi-hardware-semi-software upper band controller. Its functionality is as follows: All IGBTs are turned on period f synchronous with a selectable phase shift \( \varphi_n \). One high speed comparator per IGBT monitors a voltage signal, which is proportional to the instantaneous current. It turns off the IGBT, if this signal exceeds a second voltage signal, which is proportional to the momentous set-maximum-current. The set-maximum-current is created by the DSP
based on discrete set current values in the preloaded configuration file. The control scheme is shown in figure A.6, modifications for the upper band controller are indicated in red therein. The upper band controller has the following advantages:

- nearly negligible over-currents at maximum control bandwidth (exactly predictable small over-currents occur due to the current measurement and turn-off delay of 6 µs)
- possibility to increase the current from 0 – 100 % without a single turn-off
- very reproducible current waveforms, as interaction of arc stochastic and control parameters is no longer present.

A disadvantage of the upper band controller is that not a mean set current waveform is specified but instead a maximum-current-waveform is followed. A current ripple amplitude increase by $\Delta I$ due to a significant arc voltage increase has the following consequence: the maximum current in a control cycle remains constant but the minimum current decreases by $\Delta I$. By that, the mean current value of a control cycle decreases by the factor $\Delta I/2$. 
B. Specification of the model circuit breaker

The independent variation of blow pressure, contact opening-time and current amplitude is not possible in a puffer circuit breaker or a self-blast breaker. Therefore, it has been decided to build an air blast breaker but with external supply of pressurized gas from bottles. An overview of the model circuit breaker components is shown in figure B.2

B.1. Contact opening mechanism

A FESTO DNC pressure cylinder with pneumatic damping at both end positions is used for contact separation. The moving contact is typically accelerated in the first 20 ms, then velocity saturates. The contact is pneumatically damped at reaching the end position. The opening speed varies with applied pressure and with electrode weight. In the experiments, a pressure of 4.5 bar is used, at which a maximum speed of 3.4 m/s has been measured. Complete contact separation typically requires 80 ms. In addition, a typical delay of 30 ms exists between valve triggering and start of contact movement.

B.2. Blow apparatus

Pressurized gas (typically dry air) originates from a bank of gas bottles at 300 bar (cf. figures B.1 and B.2), from which the eight 1.5 liter bottles are filled with 1 – 50 bar prior to each experiment.
**Figure B.1.:** Model circuit breaker components: (right top) full view, (left) zoom of arc chamber, (center bottom) gas bottle bank, (center right bottom) wall bushing and current sensor, (right bottom) zoom of valves and pressurized bottles
Figure B.2.: Schematic drawing of the model circuit breaker (from [S2])
Sonic speed in the nozzle throat can only be achieved if the cross-section of the pipe system from the storage volume to the nozzle throat has everywhere larger cross-section than the nozzle throat itself. For a maximum throat diameter of 30 mm the piping system has to exceed a cross-section of 848 mm$^2$ at its narrowest section. To control the blowing, fast-operating valves are required, which are not commercially available for sufficiently large cross-sections. It was therefore necessary to use several valves in parallel. Eight VCH−400 valves with 120 mm$^2$ each and 2 ms opening time have been used.

To minimize the pressure loss from the storage volume to the nozzle throat, a radially distributed placing of eight valves and eight separate storage bottles was chosen. This has the advantage that for smaller throat diameters the storage volume size and by that the gradient of pressure decrease during the experiment can be varied by selective operation of only some of the valves and bottles.

A pressure chamber was manufactured from a high-tensile aluminium block to merge the flows from the eight valves. This reduces possible turbulences in the nozzle, caused by the gas flow splitting, and achieves a more stable gas pressure. The wall thickness is overdimensioned, so that it can bear three times higher shear stress than resulting at 50 bar. In this way, possible overpressure created by ablation in the nozzle will not exceed the mechanical limits. In addition, the circuit breaker is equipped with a housing of non breakable PVC.

The fluid-dynamic equivalent circuit of the model circuit breaker is shown in figure B.3. In total, the eight bottles represent a total storage volume $V_B = 12000 \text{ cm}^3$ connected via an effective cross-section $A_V = 9.2 \text{ cm}^2$ to the chamber. A piezo-resistive pressure sensor that measures $p_B$ is placed at the neck of one bottle, and a second one that measures $p_H$, is placed inside the pressure chamber at 10 cm distance from the nozzle throat.
**Figure B.3.**: Fluid dynamic equivalent circuit of model circuit breaker (from [S2])
C. Bibliography


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