Voltage Control and Load Sharing in DC Distribution Systems

Per Karlsson, Jörgen Svensson
Department of Industrial Electrical Engineering and Automation, Lund University
Box 118, SE-221 00
Lund, Sweden
Phone: +46 46 222 92 90, Fax: +46 46 14 21 14
e-mail: per.karlsson@iea.lth.se, jorgen.svensson@iea.lth.se
URL: http://www.iea.lth.se

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Abstract
This paper addresses voltage control and load sharing in DC distribution systems. At present, DC distribution systems are utilised in, for example, telecommunication systems. Future applications could also involve distributed power systems based on renewable energy sources. The dynamic and stationary properties of voltage droop control and its load sharing abilities are investigated. The DC bus voltage droop controller gain is selected so that the loading of each source converter is equal in per unit. Both experimental and simulation results verify the operation. Experimental results regarding fault detection in DC distribution systems are also presented.

Introduction
In this paper, DC bus voltage control and its stationary and dynamic behaviour are investigated. The target system in the study is a distributed power system based on renewable energy sources. Several different DC bus voltage control schemes exist [1], of which two seem commonly used: master-slave and droop control. The master-slave method strongly relies on fast communication between the source and load converters. One of the converters, referred to as the master, is responsible for controlling the DC bus voltage and distributing power references to the other source converters. In this paper voltage droop control is used. Droop control does not require any communication between the converters. Instead, the DC bus voltage is measured at each source converter and all the source converters contribute to balance the total power consumed by the loads and the losses of the DC power system. In common voltage droop control, the DC bus voltage decreases linearly as the DC side current, or in some cases power, of the converter increases, in order to give stable operation. This, of course, yields a stationary error in the voltage level.

The paper starts with an overview of the investigated system. Then the DC bus voltage controller used in the analysis is introduced. The controller specification yields a specification of the DC side filter, in this case purely capacitive. The derivation of the DC bus capacitor specification is also given. Last, simulation and measurement results of the dynamic properties for a two-converter system and load sharing for a three-converter DC ring bus, are shown. To investigate both the transient response and the stationary load sharing of the DC bus voltage controllers, the power reference for the power fed to the load is changed in steps.

DC bus voltage droop control
The investigated DC distribution network consists of several voltage source converters connected back-to-back to a common DC ring bus [2] (Fig. 1), where some are operated as sources and the other are feeding power to loads. The converters are connected to galvanically separated AC power sources or loads, i.e. without common reference or ground, to avoid zero sequence currents caused by the neutral voltage shift problems discussed in [3].
The structure of the DC bus voltage controller is shown in Fig. 2. A proportional (P) controller with gain $K$ is used for the droop scheme, adopted here. This controller structure is implemented in all converters intended for DC bus voltage control. The reason for this is to distribute the total load between as many converters as possible when droop control is used. The measured DC bus voltage is low-pass filtered (Fig. 2) to attenuate interaction between the negative sequence voltage of the AC side and DC bus voltage control and to enhance controller pole placement [4].

![Fig. 1: The investigated DC power system configuration.](image)

For a two-converter system (one source and one load) with the sending and receiving end DC bus capacitors lumped into one equivalent, i.e. $C_{dc} = C_s + C_r$, the source converter DC side voltage is

$$V_s(s) = \frac{(K/C_{dc})(s + \omega lp)}{s^2 + \omega lp s + \omega lp (K/C_{dc})} V_{dc,ref}(s) - \frac{(1/C_{dc})(s + \omega lp)}{s^2 + \omega lp s + \omega lp (K/C_{dc})} I_r(s)$$

where $I_r$ is the receiving end converter current. The second term of the expression above, i.e. the droop function, is equivalent to a virtual resistance $R_{droop}$. This resistance should be proportional to the rated converter power $P_n$ to achieve proportional load sharing in the case of multiple source converters. The relation between equivalent droop resistance, controller gain, desired closed loop damping $\zeta_n$ and DC bus voltage error for the source converter at rated load, i.e. $\delta_n V_{dc,ref}$, is given by

$$R_{droop} = \frac{1}{K} \frac{(2\zeta_n)^2}{\omega lp C_{dc}} = \frac{V_{dc,ref} \delta_n}{I'_n} = \frac{V_{dc,ref} \delta_n}{I_n/(1-\delta_n)} = \frac{V_{dc,ref}^2}{P_n} (1-\delta_n) \delta_n = R_n (1-\delta_n) \delta_n$$

according to Fig. 2. The DC bus capacitance of each converter, both source and load, is selected from
Here, the DC bus voltage reference equals \( V_{dc, ref} = 270 \text{ V} \). The break over frequency of the low-pass filter is \( \omega_{lp} = 2\pi 30 \text{ rad/s} \), the closed loop damping is \( \zeta_n = 0.5 \), and the voltage droop at rated load is equal to \( \delta_n = 0.025 \). Thus, each converter should have a DC side capacitance to rated power ratio of 1536 \( \mu \text{F/kW} \). Note that for a multi-converter system, all the source converters should have the same stationary characteristic in per unit to obtain proper load sharing. This is fulfilled if the DC side capacitor of each converter (both source and load) is selected according to equation (3) above.

The experimental set-ups are formed by three-phase converters connected to a 230 V, 50 Hz three-phase grid. As discussed previously, the converters are galvanically separated on the AC line sides and, therefore, each converter is connected to the common three-phase grid through transformers. The transformers are connected \( YN_y \) with a voltage ratio of 230/145. The AC side voltage is selected to give suitable \( V_{dc, ref}/E_{LL} \) ratio for proper operation, i.e. without over-modulation.

**Dynamic properties**

In this section the dynamic properties of voltage control are studied in simulations and measurements. The dynamic properties are studied for a system of two converters, connected back-to-back with a cable model in between (Fig. 3). The cable model consists of a resistor \( R_{dc} \) in series with an inductor \( L_{dc} \). Several different combinations of \( R_{dc} \) (1.0-6.0 \( \Omega \)) and \( L_{dc} \) (0-32 mH) are investigated. The converters are equipped with 1.1 mF DC bus capacitors, which implies that the load side converter has a rated power \( P_n = 716 \text{ W} \) according to (3).

![Fig. 3: Experimental set-up for investigation of dynamic properties.](image)

Fig. 4 shows the location of the poles of the transfer function \( V_s/V_{ref} \) for \( L_{dc} \) ranging from 0.3 to 32 mH and \( R_{dc} = 1 \text{ \( \Omega \)} \). Fig. 5 to Fig. 9 shows measured and simulated DC bus voltage and power. The simulations are performed in DYMOLA\textsuperscript{TM}. The power is defined positive flowing out of the DC system. The short circuit impedance of the outlet in the simulation model, is modelled as \( Z_{sc} = 0 \text{ \( \Omega \)} \).

![Fig. 4: Root locus for pole \( \rho_1 \) (left) and pole \( \rho_3 \) (right) at rated load (black) and no-load (grey). The two other poles are complex conjugate pairs of these poles and therefore not shown.](image)
Fig. 5: Measured (left) and simulated (right) sending end (black) and receiving end (grey) voltage (top) and power (bottom) for the case with $R_{dc} = 1.0$ $\Omega$ and $L_{dc} = 0$ mH ($L_{dc} = 1$ nH in the simulation model). Note that $v_r$ is not offset-adjusted.

Fig. 6: Measured (left) and simulated (right) sending end (black) and receiving end (grey) voltage (top) and power (bottom) for the case with $R_{dc} = 1.0$ $\Omega$ and $L_{dc} = 0.3$ mH.

Fig. 7: Measured (left) and simulated (right) sending end (black) and receiving end (grey) voltage (top) and power (bottom) for the case with $R_{dc} = 1.0$ $\Omega$ and $L_{dc} = 3.0$ mH.
From Fig. 6 to Fig. 8 it is apparent that the characteristics of the response change as $L_{dc}$ increases from 0.3 to 32 mH. For low equivalent cable inductance (Fig. 6), the system behaves like a second order system with poles corresponding to $\rho_3$ (Fig. 4) and its complex conjugate $\rho_4$. These poles approximately correspond to the desired according to the DC bus capacitor selection (3), i.e. they have damping $\zeta_n$ and natural frequency $\omega_n = (1/2\zeta_n)\cdot\omega_{lp}$ [4]. For high equivalent cable inductance (Fig. 8), the poles $\rho_1$ (Fig. 4) and its complex conjugate $\rho_2$, are dominant.

Due to the poor agreement between simulation and measurement for the case with $R_{dc} = 1.0$ Ω and $L_{dc} = 32$ mH, it is investigated further. Since the DC bus voltage controllers are implemented the same way in both simulations and experiments, it is concluded less likely that the discrepancy originates from the DC bus voltage controller. Therefore, the emphasis of the investigation is put on the AC side and its current controller. The main reason is that it seems like the estimated AC side power appears to have a higher overshoot in the measurements than in the simulations. This implies that the transient DC bus voltage is lower in the measurements than in the simulations. The reasons for the deviation between measurement and simulation results are investigated in the following.

The $q$-direction current reference is calculated from...
where $p_{ref}$ is the power reference and $e_{q,f}$ is the low-pass filtered grid voltage vector. The first order low-pass filter, with break-over frequency of 5 Hz, is used to attenuate disturbances since it is found that they affect the reference to a large extent. The estimated power, however, is not used for control purposes and disturbances are not considered as a problem. Therefore, it is calculated from $p_{est} = e_q i_q$. The problem is that the line impedance is not modeled in the simulation but always present in an experimental set-up. Consequently, $e_q$ is not constant in the measurements as in the simulations. When the output power from the load converter increases, $e_q$ also increases while $e_{q,f}$ remains unaltered initially. Thus, $i_{q,ref}$ is unaltered causing the overshoot in the estimated power. Moreover, the power converters are connected to the same outlet via the three-phase transformers. When the load converter responds to the step in its power reference it delivers power to the grid. This means that when the source converter starts to draw power from the same three-phase grid, the loading of the outlet is lower than expected. However, during the voltage transients the short circuit impedance of the outlet is important.

The experimental set-up is connected to an AC power outlet equipped with 10 A fuses. Consequently, it is likely that the short circuit impedance of the grid is rather high. Therefore, a short circuit impedance of $Z_{sc} = 0.25 + j0.25 \ \Omega$ is used for the following simulations. Two simulations are repeated with this line impedance applied to the model. In Fig. 10 the simulation results with $L_{dc} = 3.0 \ \text{mH}$ and $L_{dc} = 32 \ \text{mH}$ for $R_{dc} = 1.0 \ \Omega$ are shown. The measurement and simulation results for $L_{dc} = 3.0 \ \text{mH}$ show a good agreement already in Fig. 7. The reason for repeating the simulation with the altered model is to verify that it covers all cases and not just the ones with $L_{dc} = 32 \ \text{mH}$.

It is clearly seen that the result corresponds well with the measured result of Fig. 7. Due to the good agreement between measurement and simulation also for the altered model in this case, the other simulations that already show a high degree of agreement is not repeated. The simulation result for the case with $R_{dc} = 1.0 \ \Omega$ and $L_{dc} = 32 \ \text{mH}$ in Fig. 10 shows better agreement with the measured result in Fig. 8 compared to the simulation result of Fig. 8. Still, the ringing following the voltage overshoot in the simulation is not apparent in the measurement. This is most likely due to the fact that the short circuit impedance $Z_{sc}$ of the three-phase outlet is not accurately estimated and that the distributed impedance in the converter is not modeled but present in the experimental set-up.

![Simulated sending end (black) and receiving end (grey) voltage (top) and power (bottom) for the case with $R_{dc} = 1.0 \ \Omega$ and $L_{dc} = 3.0 \ \text{mH}$ (left) and $R_{dc} = 1.0 \ \Omega$ and $L_{dc} = 32 \ \text{mH}$ (right) when $Z_{sc} = 0.25 + j0.25 \ \Omega$.](image-url)
Load sharing

An experimental set-up based on three converters connected back-to-back to a common DC ring bus (Fig. 11) is employed to investigate load sharing. The system shown in Fig. 11 is also implemented in DYMOLA™ for verification. The DC distribution system shown in Fig. 11 is a reduced version of the system shown in Fig. 1, which is theoretically investigated in [4]. In the study here, the converters are equipped with DC bus capacitors of 1.6 mF (unit 1), 2.1 mF (unit 2) and 1.1 mF (unit 3). According to (3) this corresponds to a rated power for each converter of 1041 W (unit 1), 1367 W (unit 2) and 716 W (unit 3). In this investigation, units 1 and 3 act as sources, and unit 2 as a load. In the controller design it is assumed that the rated power of the sources and loads are equal in total, which implies that the sum of the DC bus capacitance of the sources should be equal to the one of the loads. However, the numbers given here show that this is not the case since the sources have a total DC bus capacitance of 2.7 mF, corresponding to a rated power of 1757 W, and the load has a DC bus capacitance of 2.1 mF, corresponding to 1367 W. However, the gain of the DC bus voltage controller is calculated according to (2) assuming that there is a load converter of equal rated power, i.e. $C_{dc} = C_s + C_r = 2C_{dc, converter}$. Thus, for damping equal to $\zeta = 0.5$

$$K = 2\omega_p C_{dc, converter}$$ (5)

Since the total rated power of the source converters is higher than the rated power of the load converter in this case, the gain will be somewhat higher than expected and the stationary voltage error lower. The system damping is also lower than expected due to the higher gain.

Each converter is connected to the DC ring bus through a cable node with two 10 µF grounding capacitors $C_{p1}$ and $C_{p2}$ (Fig. 12), with 220 kΩ bleeder resistors connected in parallel. The resistors $R_{p1}$ and $R_{p2}$ of 100 Ω each are connected in series with each grounding capacitor, to obtain a minimum time constant of 1 ms for the fault currents, as described in [6]. Current transducers are not mounted. Instead, current probes are used for measuring the differential fault currents, $i_{dc, diff1}$ and $i_{dc, diff2}$. Each cable segment is modeled by four 0.1 Ω resistors (Fig. 12), to introduce a fault on the middle.

![Fig. 11: Schematic of the network for investigation of load sharing.](image1)

![Fig. 12: Transducer arrangement for ground fault detection.](image2)
For a DC bus-to-ground fault, the initial fault current $i_{dc\text{diff}1}$ is equal to

$$
|i_{dc\text{diff}1}| = \frac{1}{n} \frac{V_{dc}}{R_{Fault}} \cdot n \cdot V_{ni}^2
$$

(6)

with equal sign for all cable nodes. The magnitude is also equal provided that the initial grounding capacitor voltages are given by $V_{CP1} = V_{CP2} = V_{dc}/2$ [4][6]. Here, $n$ is the number of cable nodes of the DC distribution system and $R_{Fault}$ the fault resistance between outer conductor and ground. The initial magnitude of the fault current $i_{dc\text{diff}2}$ for the cable nodes located closest to the fault is

$$
|i_{dc\text{diff}2}| = \frac{n-1}{n} \frac{V_{dc}}{R_{Fault}} \cdot n \cdot V_{ni}^2
$$

(7)

Note that $i_{dc\text{diff}2}$ is of opposite sign for the two cable nodes closest to the fault location.

The rest of this section is arranged as follows. First, load sharing is examined for a system where the DC bus voltage measurements of the sources are offset-adjusted so that they are loaded proportionally equal. Measurements are made for both an unbroken and broken DC ring bus. The result of a high impedance DC bus ground fault is also examined. Second, load sharing without offset-adjusted DC bus voltage measurements is investigated. Note that all the measurements in this section are averaged from ten separate measurements due to the high level of measurement noise. The power reference for the load converter (unit 2) is changed in steps in all the experiments on load sharing. The magnitude of the step corresponds to approximately 850 W.

Fig. 13 shows experimental and simulated results in the case the DC bus voltage measurement transducers of the source converters are offset-adjusted. Offset-adjustment is carried out by subtracting the DC bus voltage measurement of unit 3 by 3.7 V in the controller to give the same level as for unit 1 at no-load conditions. In Fig. 13, both sources (units 1 and 3) are loaded to 0.6 per unit (unit base), which verifies proportional load sharing.

The differential fault currents in the case of a 1.0 kΩ DC bus ground fault between unit 1 and 2 of the three-converter system, are investigated. Current probes with a transfer ratio of 100 mV/A are used for the differential current measurements. The measurement noise due to switching has a higher magnitude than the fault currents in this case. Therefore, the fault currents are measured for a case where the converters are not operated. Consequently, the DC bus voltage equals the peak AC line-to-line voltage, i.e. 205 V. The differential current measurements are shown in Fig. 14.
The peak fault current at the fault location should equal 103 mA, which means that the peak differential fault current $i_{\text{dc} \text{diff}1}$ equals 68 mA for units 1 and 2, according to (7). The peak differential fault current $i_{\text{dc} \text{diff}2}$ equals 34 mA, according to (6). The levels of the fault currents are, thus, in agreement with the theoretical results of [6]. Since $i_{\text{dc} \text{diff}1}$ is negative, the fault is located between the positive DC bus conductor and ground. The sign combinations of $i_{\text{dc} \text{diff}1}$ and $i_{\text{dc} \text{diff}2}$ for units 1 and 2 reveal that the fault is located between these units. For unit 3, $i_{\text{dc} \text{diff}2}$ is essentially unaffected by the fault since the common mode current flows into both cable segments for this cable node. Note that the switching noise problem discussed earlier in this section indicates that the transducers for the differential current measurement should have a higher transfer ratio, for example 1 V/A to accurately detect high impedance ground faults. They should also be saturable to avoid excessive voltages for low impedance faults.

Fig. 15 shows measurement and simulation results when the DC ring bus is broken between converter 1 and 2. The DC ring bus could be broken for example due to a previous fault causing a disconnection of the faulted cable segment. Due to the moderate cable resistance, load sharing is only affected to a minor extent.
A very critical issue shows up in the last experiment on load sharing where the performance is investigated for a case where the DC bus voltage measurement of unit 3 is not offset-adjusted. As seen in Fig. 16, unit 1 delivers power to the DC bus and unit 3 draws power from the DC bus when unit 2 operates at no-load.

![Image of voltage and power measurements]

Fig. 16: Measured unit 1 (black), unit 2 (grey) and unit 3 (light-grey) DC bus voltage (top) and power (bottom) in the case of non-offset-adjusted DC bus voltage measurements for the source converters.

Fig. 16 visualizes that one of the main problems of droop control is that the DC bus voltage measurement must be accurate to yield proper load sharing. On the other hand, it also indicates that DC bus voltage shift control is feasible. In the wind power application of [5] the droop characteristic is shifted up- or downward to share the load dependent on the available power of each source.

**Conclusions**

A DC distribution system intended for renewable energy systems is investigated in this paper. Simulation results regarding stationary and dynamic properties of voltage control and load sharing are verified in experiments. It is found that the controller scheme is robust to cable parameter variations and that load sharing acts proportional, i.e. equal in per unit. Also a method to detect and localize ground faults, presented in [4][6], is verified in measurements. Further information on the background and results of the work presented is found in [4].

**References**


